Investigation of flow over oscillating NACA 4421airfoils in tandem configuration at low Reynolds number

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**INVESTIGATION OF FLOW OVER OSCILLATING
NACA 4421 AIRFOILS IN TANDEM CONFIGURATION
AT LOW REYNOLDS NUMBER**

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SCHOOL OF MECHANICAL AND AEROSPACE ENGINEERING

A thesis submitted to the Nanyang Technological University in partial
fulfilment of the requirements for the degree of
Doctor in Philosophy

2019

Statement of Originality

I hereby certify that the work embodied in this thesis is the result of original research, is free of plagiarised materials, and has not been submitted for a higher degree to any other University or Institution.

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This thesis contains material from 1 paper published in the following peer-reviewed journal(s) where I was the first and/or corresponding author.

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- Professor Simon Yu provided the initial project topic and guidance in the experimental sets and parameters.
- Associate Professor Tang Hui provided his technical expertise in the standardization of the PIV vorticity contours.
- Both Professor Simon Yu and Associate Professor Tang Hui vetted the drafts of the manuscript and provided their scientific inputs in rationalizing the force and PIV measurement results.

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Abstract

The present study is on the experimental investigation of the flow over oscillating NACA4421 airfoils in various tandem configurations at low Reynolds number of 8.6×10^3 . Force and two dimensional particle image velocimetry (PIV) measurements were carried out in the NTU closed loop water tunnel with a freestream velocity, $U_\infty = 0.102\text{m/s}$. The effects of phase angle, ϕ , stagger distance, $S_d (= x/c$ where x is the horizontal axial spacing between the tandem airfoils and c is the chord) and gap, $G (= |y|/c$, where y is the vertical spacing between the tandem airfoils) were investigated for both oscillating and static tandem airfoils. In-phase oscillating tandem configuration has shown its potential to increase average combined lift coefficient, C_L up to 47.7% but resulted in a corresponding increase in average combined drag coefficient, C_D up to 54% at $\alpha > \pm 30^\circ$ in relation to the single oscillating case. It also attained its best combined C_L / C_D ratio of 2.33 which is 35% lower than the single oscillating airfoil at $\alpha = \pm 45^\circ$ with S_d of 2. This is attributed to the in-phase oscillating downstream airfoil's ability to generate 89% higher average C_L than the single oscillating airfoil as a result of the down-washed vortex interaction between the primary shed vortex before its first stroke reversal. However, this interaction increases the average C_D up to 5 times more than the single oscillating airfoil which decreases the combined C_L / C_D ratio for the in-phase oscillating tandem configuration. The out-of-phase tandem oscillating configuration produces the highest combined C_L / C_D ratios at larger $\alpha > \pm 30^\circ$ with the larger S_d of 2.5 due to the downstream airfoil's ability to generate large lift improvements of more than 100% and exhibiting comparable drag production in relation to the single oscillating airfoil through effectively capitalizing on its 'indirect' down-washed vortex interaction effects. Thus, making the out-of-phase oscillating tandem configuration at $S_d = 2.5$ the most ideal configuration in the present study, potentially viable for hovering flight.

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NOMENCLATURE

ac	Aerodynamic Center
AR	Aspect Ratio = S / c
A_w	Wing Area (m^2)
B	Vertical Spacing between Consecutive Shed Vortices (m)
c	Chord (m)
C_D	Drag Coefficient = $F_x / (A_w q_\infty)$
C_T	Thrust Coefficient = $- C_D$
C_h	C_l - Hysteresis = $\int C_l d\alpha$
C_L	Lift Coefficient = $F_y / (A_w q_\infty)$
C_M	1/4 chord Moment Coefficient = $M / (c A_w q_\infty)$
C_p	Pressure Coefficient = $1 - q/q_\infty$
C_w	Torsional Damping Factor = $\int C_m(\alpha) d\alpha$
d	Vortex Diameter
f	Oscillation or Flapping Frequency (Hz)
F_x	Drag Force (N)
F_y	Lift Force (N)
$g(a)$	Leading Edge Opening Angle for ‘Clap and Fling’ Mechanism
G	Gap = $ y / c$
h_a	Heaving Amplitude or Stroke Amplitude
κ	Reduced Frequency = $\pi f c / U_\infty$
K	Turbulent Kinetic Energy
K_{belt}	Stiffness Constant for Elastic Belt (N/m)
L	Horizontal Spacing between Consecutive Shed Vortices (m)
L_{space}	Tandem Airfoil or Wing Spacing (m)
L'	Sectional Lift Force per Span Width (N/m)
l_b	Laminar Separation Bubble Length
M	1/4-chord pitching moment (N/m)
p	Pressure (N/m^2)
q	Dynamic pressure = $1/2 \rho U^2$ (N/m^2)
Re	Reynolds Number = $c U_\infty / \nu$
S	Span Width (m)
Sd	Stagger Distance = x / c
St	Strouhal Number = $f h_a / U_\infty$

T_i	Free-Stream Turbulence Intensity
t	Time (s)
T	Time required to complete one Oscillation Cycle, Period (s)
T_z	Torque about Span-Wise Axis, Z
U	Flow Velocity (m/s)
U_c	Combined Uncertainty (%)
U_s	Systematic Error (%)
u_i	Velocity Vector in Cartesian Coordinates
$\vec{V}$	Velocity Vector
V_{JF}	Resultant 'Jet-Force' Vector Magnitude
V_θ	Vortex Swirl Velocity (m/s)
w	Mesh Velocity (m/s)
x	Horizontal Axial Spacing (m)
X_{belt}	Belt Elongation (m)
X_{LE}	Separation Point Distance from Leading Edge (m)
y	Vertical Spacing (m)

Greek Symbols

α	Pitch Angle or Amplitude (°)
α_{ds}	Dynamic-Stall Angle (°)
α_{fr}	Angle at the Onset of Flow Reversal (°)
α_{max}	Maximum Angle of Attack (°)
α_{min}	Minimum Angle of Attack (°)
α_{ss}	Static-Stall Angle (°)
β	Stroke-Plane Angle
δ	Decalage = $\alpha_{ua} - \alpha_{da}$ (°)
Δ	Differential
ε	Turbulent Dissipation
Γ	Circulation (m ² /s)
λ	Wavelength (m)
η	Propulsive Efficiency (%)
μ	Dynamic Viscosity (kg/(s.m))
ν	Kinematic Viscosity (m ² /s)
ω	Angular Velocity (deg/s)
$\vec{\omega}$	Vorticity Vector

ϕ	Phase Angle / Difference ($^{\circ}$)
$\phi(t)$	Positional Angle of a Flapping Wing
$\Psi(t)$	Feathering or Twist Angle of a Flapping Wing
ρ	Density (kg/m^3)
σ	Stress (N/m^2)
$\theta(t)$	Elevation Angle of a Flapping Wing
ζ	Vorticity (s^{-1})

Subscripts

0	Initial Condition
a	Axial Flow
cw	Clockwise
ccw	Counter-Clockwise
d	Downstroke
da	Downstream airfoil
ds	Dynamic-Stall
fr	Flow Reversal
∞	Free Stream condition
JF	‘Jet-Force’ Vector
lev	Leading-Edge Vortex
max	Maximum
min	Minimum
u	Upstroke
ua	Upstream airfoil
s	Single Airfoil
ss	Static-Stall
ref	Reference
vs	Vortex Shedding

Abbreviations

2-D	Two-Dimensional
3-D	Three-Dimensional
AoA	Angle of Attack

CFD	Computational Fluid Dynamics
CW	Clockwise
CCW	Counter-Clockwise
FR	Flow Reversal
JF	Jet-Force
LE	Leading Edge
LEV	Leading-Edge Vortex
LSB	Laminar Separation Bubble
NS	Navier-Stokes
PIV	Particle-Image Velocimetry
TBL	Turbulent Boundary Layer
TE	Trailing Edge
TEV	Trailing Edge Vortex
vs	Vortex Shedding

1. INTRODUCTION

Figure 1-1 : (a) MQ-9 Reaper UAV manufactured by General Atomic Aeronautical Systems [1]; (b) Bluefin-21 UUV from General Dynamics Mission Systems [2]

With the increasing demand for more agile and power efficient Unmanned Air Vehicles (UAVs) such as the MQ-9 Reaper (Predator B) Drone or Unmanned Underwater Vehicles (UUVs) such as the Bluefin-21 shown in Figure 1-1, it has motivated researchers and inventors to come out with novel propulsive and flight systems. In addition, this amassed demand also spurs on the development for lighter and stronger materials such as carbon-fiber reinforced composites used in Quadcopter drones we see today.

Figure 1-2 : A schematic of a typical UAV operating system [4]

With the current state-of-the-art research capabilities, advancement in nanotechnology and robust development of 'Artificial Intelligent' systems, the possibilities for these vehicles are endless. “Unmanned” vehicles are essentially defined as a vehicle that can operate without a pilot on board with various degree of autonomy; either remotely controlled by a pilot or fully or intermittently autonomously via on-board computers, can be expandable and recoverable and can carry non-lethal or lethal payload [3].

A simplistic UAV operation system consists of two platforms; (1) the Vehicle Platform and (2) the Unmanned Aircraft System Platform as shown in Figure 1-2 [4]. The Vehicle Platform consists of sensors and actuators that are powered by an onboard power supply and controlled by an onboard computer. These sensors and actuators are vital components to control the vehicle movement and provide feedback of the physical state of the vehicle. The Unmanned Aircraft System Platform will ensure a two-way communication mode with the operator and vehicle with a defined degree of autonomy. With the advancement in communication technology, this platform is designed to allow the dual exchange of videos, images and data between operator and vehicle. For example, a ground control operator can transmit commands to the vehicle via a computer module to the onboard computer and can also remotely download surveillance videos recorded by the vehicle.

In 1992, United States Department of Defense’s research arm, Defense Advanced Research Projects Agency (DARPA) held a technology workshop titled “Future Technology-Driven Revolutions in Military Operations” where one of the topics that were discussed revolved on the concept of small “micro-drones” utilization [5, 6]. Henceforth, the Micro-Aerial Vehicle or MAVs concept was born. In the mid-1990s, the RAND Corporation published a well-cited paper on sensor-carrying insects. This led to DARPA collaborating with MIT Lincoln Laboratories and US Naval Research Laboratory (NRL) to carry out validation studies on the conceptual feasibility of Micro-Air Vehicles (MAVs). DARPA spearheading efforts in MAV research were well reciprocated globally by numerous government agencies [7] and have since motivated researchers to invent smaller and more efficient MAVs. These MAVs will be cheaper to manufacture, require lesser operating power and logistical support, and utilize less sophisticated operating system in relation to larger UAVs such as military-specific drones.

Table 1-1 : Micro-Air Vehicles (MAVs) Requirements [7]

Specification	Requirement
Size	<15 cm
Weight	100 g
Payload	20 g
Range	1-10 Km
Endurance	60 min
Altitude	< 150 m
Speed	15 m/s

Figure 1-3 : (a) Fixed Wing MAV; 'The Black Widow' developed by AeroVironment [8], (b) Micro-Quadcopter; Estes 4606 Proto X developed by Estes [9] and (c) Flapping MAV; 'DelFly' developed by TU Delft (Right) [10]

According to MAV's requirements specified by DARPA shown in Table 1-1 [7], MAVs are defined as an air vehicle that measures no larger than 15cm in length with a flying altitude of lesser than 150m [5-7]. These MAVs are known to be used in many military or civil applications such as reconnaissance and surveillance, search and rescue as well as target detection specifically for indoor terrains such as tunnels and urban environment [7]. Currently, there are three primary types of MAVs; (1) the fixed wing MAV such as the Black Widow developed by Aerovironment [8]; (2) the rotary wing MAV such as the Estes 4606 Proto X developed by Estes [9] and (3) the flapping MAV such as the 'DelFly' developed by TU Delft [10] as shown in Figures 1-3 (a), (b) and (c) respectively.

One of the challenges with the development and design of MAVs would be scalability. Ideally, MAVs can be scaled down from its larger aircraft counterparts to its miniature size that includes its aircraft vehicle and propulsion system. However, as documented by Petricca et al. [11], miniaturization rate have practically plateaued after 2002 due to physical and technological challenges. One huge impediment is to understand the complex aerodynamics related to these MAVs. As the name suggest, being less than 15cm in total length, these MAVs need to operate in a similar environment as small natural fliers, where the flow condition is laminar and falls between 10^4 to 10^5 Reynolds Number [11].

Figure 1-4 : $(C_L/C_D)_{\max}$ versus Reynolds Number, Re for Smooth Airfoils [11]

In the present study on flow over oscillating airfoils, the Reynolds Number, $Re = \frac{\rho U_{\infty} c}{\mu}$ is defined as the ratio between inertial forces and viscous forces for a fluid flow where ρ denotes the fluid density (kg/m^3), U_{∞} is the freestream flow velocity (m/s), c is the characteristic airfoil chord (m) and μ is the dynamic viscosity of the fluid ($\text{kg/m}\cdot\text{s}$). Reynolds number is also an important dimensionless parameter commonly used to predict or characterize the flow transition between laminar and turbulent flow condition. In these low Reynolds number flow conditions that these small natural fliers operate in, as per the definition, the fluid flow is dominated by viscous effects where it presents several aerodynamic problems that are associated with laminar flows such as laminar flow separation and laminar-to-turbulent transition. Moreover, as shown in Figure 1-4, for a fixed wing MAV flying at Reynolds number below 10^5 , the lift-to-drag ratio (C_L/C_D) was found to drastically decrease which is attributed to laminar flow separation due to the dominant viscous effects that is inherent in the laminar flow regime [11].

In order to overcome these aerodynamic problems that fixed wing MAVs encounter, researchers began looking at natural fliers such as small birds and insects which flies at approximately the same laminar flow regime (Reynolds number $< 10^5$) as the MAVs for inspiration. Due to the slower flight speed, these small natural fliers are more affected by their flight environment such as wind gust. Hence, they are known to utilize their flapping wings to effectively overcome these environmental factors, exhibit great maneuverability, showcasing their capability of flying at low speeds and achieving high aerodynamic efficiency [12, 13].

One of our key motivations for this study is to emulate the flight prowess of a dragonfly. It is a remarkable creature that utilizes its tandem wings to great effect; accomplishing highly difficult maneuvers, flying upside down and producing lift force up to 15 to 20 times their body weight during flight [14, 15]. Thomas et al. [16] and Alexander [17] have studied the force generation and aerodynamic performance of the dragonflies' flight. Thomas et al. [16] documented that dragonflies are able to produce high lift force primarily due to the generation and shedding of Leading-Edge Vortices (LEVs) by controlling the angle of attack. Alexander [17] studied the effects of phase angles, ϕ between the tandem wings in relation to the various mode of the dragonflies' flight such as take-off, hovering and cruising. Lee [18] then experimentally studied pitching NACA0012 tandem airfoils at various stagger distances, S_d and phase angles, ϕ . S_d is defined as the horizontal spacing between the two tandem airfoils and is typically represented as $S_d = \frac{x}{c}$ where c is the characteristic airfoil chord and x is the horizontal spacing between the two tandem airfoils. Phase angle or difference, ϕ is defined as the proportion of the stroke period in which the downstream airfoil leads the upstream airfoil. Lee [18] concluded that the airfoil spacing and the phase difference between the tandem airfoils played a vital role in determining the nature of the flow interaction that will have a direct impact in the aerodynamic performance of these tandem airfoils. The experimental work by Lee [18] will be elaborated in the next chapter of this study. Despite these insightful and novel literatures, one important physical parameter of the tandem airfoil configuration has not been thoroughly explored is the effect of gap distances, G , between the tandem airfoils. G is defined as the vertical spacing between the tandem airfoils and can be represented as $G = \frac{|y|}{c}$ where y is the vertical spacing between the airfoils.

Figure 1-5 : Typical Insect Flapping Kinematic Motions showcasing its Heaving, Surging and Pitching Axes

Figure 1-6 : Various Possible Combination of Flapping Wing Kinematic Motions; (a) Heaving Motion, (b) Oscillating or Pitching Motion, (c) Surging Motion and (d) Coupled Pitching and Heaving Motion with Phase Angle, $\phi = 90^\circ$

In the present study, firstly, the flow dynamics and aerodynamic performance of a typical flapping wing will be investigated to provide the basis of comparison to tandem wing configuration and a better understanding on these small natural fliers. Typically, a flying insect utilizes a combination of rotational motion (pitching), vertical motion (heaving) and

horizontal motion (surging) during its flight as shown in Figures 1-5 and 1-6 [19-21]. These kinematic motions together with governing laws of physics will provide the necessary aerodynamic forces such as lift and thrust to keep these fliers afloat. The various permutations of these kinematic motions utilized by these small natural fliers for different flight modes such as hovering and forward flight will be elaborated in the Chapter 2 of the present study. However, the present study aims to provide an in-depth fundamental investigation on the oscillating motion as depicted in Figure 1-6 (b) using tandem airfoils, its effects on wing-wing and wing-wake vortex interactions and also these vortex interaction effects on lift and drag or thrust performance.

Based on works by Ellington [19], Dickinson and Gotz [20] and Dickinson et al. [21], the lift and thrust properties of flapping wings depended profoundly on several unsteady flow mechanisms and the interactions of vortices produced during the oscillations. The study of a single flapping wing will enable us to look at these unsteady flow mechanisms and the effects of the vortex interactions in order to apply these concepts to the study of tandem oscillating airfoils. The present study aims to develop novel flow dynamic concepts and also to impart new scientific knowledge on tandem oscillating airfoils that can be applied to new MAVs designs.

1.1 FLIGHT IN NATURE

Figure 1-7 :Small Natural Fliers in the Low Reynolds Number Flow Regime such as (a) Broad-Tail Humming Bird [22], (b) Migrant Hawker Dragonfly [23] and (c) White-Eyed Mutant Fruit Fly or *Drosophila Melanogaster* [24]

Numerous biologist and naturalists such as Taylor et al. [25] and Ho et al. [26] have documented the kinematics of the flapping motion utilized by nature and correlations between flapping frequency, weight, wing-span and power requirements of many birds and insects such as those in Figure 1-7. Taylor et al. [25] analyzed the flapping amplitudes and frequencies of 42 species of birds, insects and bats in cruise flight and discovered that these

natural fliers all operate within a narrow range of Strouhal number of between 0.2 and 0.4. Strouhal number, $St = \frac{f h_a}{U_\infty}$ is defined as a dimensionless parameter used to effectively characterize the flapping flight performance where f is the flapping frequency, h_a is the heaving amplitude and U_∞ is the free stream velocity. Ho et al. [26] reviewed on the desired characteristics of MAVs through correlating the physical attributes of natural fliers and man-made fliers such as weight and wing span to its aerodynamic performance such as cruising speeds and wing loadings.

Figure 1-8 : 'Great Diagram of Flight' [27]

Researchers have analyzed large number of species of birds, bats and flying insects and formulated correlations between their physical characteristics and aerodynamic performance [25-27]. Correlations charts such as the 'Great Diagram of Flight' produced by Tennekes [27] shown in Figure 1-8 demonstrates the relationship between weights of the various fliers from as small as a housefly to a Boeing 747 commercial jet. Even with a vastly diverse sample population, the chart shows a clear linear regression line that most of these fliers fall on. This is one of the many notable correlation studies exhibiting the concept of regression analysis. One benefit of using such regression analysis is in the application of geometric scaling that helps researchers and engineers to determine the wing spans, weight and wing loading and size of the MAVs in relation to the flow regime that it falls under. Hence, with a given size, flapping frequency, wing span or weight of the MAVs and the aerodynamic performance of the MAVs can be predicted.

Figure 1-9 : Plot of Wingtip Speed and Flight Speed versus Body Mass [26]

Ho et al. [26] acknowledged that the kinematics of flapping wings can be categorized into two regimes; the quasi-steady and the fully unsteady regimes as shown in Figure 1-9. The distinction between these two regimes is based on empirical correlations that predict this segregation to be at approximately 0.15m in wingspan. The quasi-steady regime is applicable to mainly large birds such as the seagulls where their flight is dominated by soaring and gliding as they rely on flapping for selected flight operations such as take-off, landing and stabilization. In their powered flight, their wings will be rigid and spread out which acts as a glider using the thermal currents to soar and glide gracefully.

On the other hand, the fully unsteady regime is applicable to all small birds and insects that flap at low Re number flows of between 10^4 to 10^5 . Typically these small birds and insects will continuously flap in their powered flight and their flapping motion involves complex motions such as oscillating or pitching, heaving or plunging and coupled pitching and heaving motion. They are also known for their high maneuverability, power, propulsive efficiency and low speeds which are the preferred characteristics of a MAV. Through the correlation chart shown in Figure 1-9, it shows that MAVs would need to operate under the unsteady-state flow regime where the flow conditions are not constant over time and cannot be approximated by quasi-steady state assumptions [26]. Hence, in the present study, one of the assumptions is to focus on the aerodynamic performance of the oscillating wing solely under this fully unsteady regime.

1.1.1 Single Flapping Wings in Forward Flight

Past studies by Knoller [28], Betz [29], Maxworthy [30] and Ellington et al. [31] and Dickinson and Gotz [20] demonstrated that unsteady flow mechanisms were responsible for the generating aerodynamic forces for flapping wings. Small natural fliers utilized the coupled heaving and pitching motion to great effect by altering their flapping parameters such as reduced frequency (κ), angle of attack (α), flapping amplitudes, phase angles (ϕ) and timing of stroke reversals. The reduced frequency, $\kappa = \frac{\pi f c}{U_\infty}$, is used as a dimensionless parameter to define the unsteadiness of the flow condition where f is the flapping frequency, c is the characteristic airfoil chord and U_∞ is the freestream flow velocity. The manipulation of these flapping parameters will determine the unsteady flow mechanisms used for its intended flight applications such as forward flight or hovering flight.

Weis-Fogh and Jensen [32] were credited as one of the pioneers in the studies on insect flight where experimental studies on the desert locust (*Schistocerca Gregaria*) were carried out. Nachtigall [33, 34] then investigated on the flight of the fly (*Phormia Regina*) through performing experiments in the wind tunnel using novel force and control balance system. In these past studies, there were limitations in measuring forces produced by these insects due to its small nature. Hence, detailed photographs were captured using high speed cameras to visualize the insects' flapping flight which provided the first glimpse of the dynamics behind the flapping wing flight. Maxworthy [30] published an excellent review of the dynamics of insect flight where he documented the inner workings unsteady flow mechanisms such as the 'clap and fling'. In recent times, Lua et al. [35] employed numerical and computational methods to study insect flight.

Figure 1-10 : (a) Geometric/Discretized Model of Fruit Fly with Affixed Body Frame $(x, y, z)^B$ and Global Reference Frame (x, y, z) ; (b) The Wing Frame $(x, y, z)^W$ [35]

The insect models are typically represented in three main frames as shown in Figure 1-10 (a) where the body frame is denoted by its $(x, y \text{ and } z)^B$ axes and the global reference frame is denoted by its $(x, y \text{ and } z)$ axes which corresponds to the lateral, horizontal and vertical planes respectively. Figure 1-10 (b) shows the wing frame which is denoted by its $(x, y \text{ and } z)^W$ axes and exhibits the flapping wing kinematics which are characterized in three distinct motion angles; stroke/sweep angle $\phi(t)$, elevation angle $\theta(t)$ and wing-twist/pitch angle $\psi(t)$ about the wing axis. These distinct motion angles can be correlated to the flapping wing kinematic motions presented in Figure 1-6 earlier where the stroke/sweep angle, $\phi(t)$ represents pure surging motion, the elevation angle, $\theta(t)$ represents pure heaving motion and the wing-twist angle, $\psi(t)$ represents the oscillating motion about the wing axis.

Figure 1-11 : Motion of the Wing Tip of a Fly at 1/6400s Intervals with the Insect Moving from Right to Left [33, 34]

Contrary to large birds, small birds and insects need to continuously flap their wings to move them forward. The simplistic breakdown of the flapping forward flight consists of the downstroke or power stroke where most of the thrust force is produced and the upstroke or the recovery stroke. As the wings flaps up and down in the z -axis and simultaneously moving forward, the movement of the wings near to the body does not move substantially as compared to the vertical movements at the wing tips. These natural fliers' wings are able to twist about the wing axis during flight, thus enabling them to change their angle of attack especially so at the wing tips. During the down stroke, the angle of attack of the wing is increased and is decreased in the upstroke. Nachtigall [33, 34] has produced a schematic of

the wing tip motion of the fly (*Phormia Regina*) in its forward flight captured at 1/6400s intervals in Figure 1-11. It shows the increase in angle of attack during its down-stroke depicted in lines 1 to 21 with the small triangle denoting the wing's leading edge. With the wing tips at a positive angle of attack during its down-stroke, the resultant force vector acting on the outer surface of the wing at its pressure center will be pointing forward thus resolving into a positive lift force and thrust force; deflecting the air downwards and rearwards. This helps propel the flyer forward without any loss in altitude. Figure 1-11 also shows the wing rotation that took place prior to the wing's upstroke as depicted in lines 22 to 30. This timing of this wing rotation or pitching motion is critical to the effect of rotational circulation mechanism that will be elaborated in the later sections of this study. As the wing progresses into their upstroke from Lines 28 to 50, the wings rotate to a point where the wingtip points straight in the line of travel so as to cut through the air with the least resistance. Another point is that even when the insect is flying backwards, the wing rotation allows it to generate positive lift and thrust forces at a quasi-steady basis as depicted in Line 43 [33, 34].

1.1.2 Single Flapping Wings in Hovering Flight

Figure 1-12 : The ‘Figure of Eight’ Hovering Stroke [36]

Hovering flight is defined as when the travelling velocity or forward velocity approached zero and that reduced frequency, κ approaches infinity [30]. In hovering flight, the average upward lift force produced is equal to the body weight of the insect as the insect hovers at a fixed point in space where they do so by flapping their wings rapidly. These insects or hummingbirds also utilized their wing rotations and extensions to stabilize themselves sideways during its hovering flight. Wu et al. [37] have recently published a computational

study on free hovering insect flight under low Reynolds number flows where they investigated the stability characteristics of hovering insect flight and showed the poor static stability of insects based on their heuristic analysis. However, quasi-steady periodic stability was achieved through active adaption of the insect's wing kinematics [37].

Photographs of insect in hovering flight have been published by Weis-Fogh [32], Ellington [38-40], Dickinson and Gotz [20], Dickinson et al. [21], Maxworthy [30] and a similar kinematic pattern emerged which is the 'Figure of Eight' stroke. As shown in Figure 1-12, this 'Figure of Eight' stroke of a hovering fly consists of four stages; two translational phases (upstroke and downstroke) where the wings sweep through the medium with a high angle of attack and two rotational phases (pronation and supination) when the wings rapidly rotation and reverse direction [21, 36]. Pronation occurs during the transition to upstroke and supination occurs during the transition to downstroke [21, 36].

Figure 1-13 : Force Vector Diagram depicting the Downstroke in the Hovering Stroke by Dickinson [21, 36]

Dickinson studied the kinematics and flow dynamics of insect flight through designing a robotic fly apparatus that was designed to match the Reynolds number of a typical *Drosophila* (small fruit fly) where $Re \approx 136$ and employing Plexiglas wings measuring 0.25m in span width which are modeled after a *Drosophila* wing [21]. Dickinson also documented the various unsteady flow mechanisms that were responsible for generating lift during the downstroke of the hovering flight as illustrated in Figure 1-13 [21, 36]. In (1), the first mechanism is the 'delayed stall' effect where the leading-edge vortex (LEV) that forms over the top suction wing surface reduces the pressure over the wing and increases lift by

increasing the effective angle of attack. As the wing rotates or pitches upwards and increases its angle of attack in (2), it creates a vortex that has a ‘backspin’ effect which induces a positive lift or ‘rotational lift’. This unsteady flow mechanism is known as ‘rotational circulation’ [36]. As the wing approaches the end of its downstroke in (3), the wing rapidly rotates as it undergoes pronation which induces a huge thrust vector propelling the insect forwards without much lift.

Figure 1-14 : Force Vector Diagram depicting the Upstroke in the Hovering Stroke by Dickinson [21, 36]

As the wing progresses through its upstroke in (4) as shown in Figure 1-14, it flaps through its own wake at a high angle of attack capturing some of the vortical energy in the wake which evidently increases the lift produced in (5). This vortex-wing interaction is better known as ‘wake capture’ [21, 36]. For most parts of the upstroke, the wing produces resultant force vectors as indicated by the red arrow that are primarily dominated by drag forces. Thus, looking at the resultant force vectors throughout the hovering stroke cycle; even though the rotational motion of the wing during pronation produces thrust, this is quickly cancelled out by the drag forces generated by upstroke of the wing. Before the stroke reversal, the wing rotates rapidly again undergoing supination after which the wing transits from upstroke to downstroke and the cycle repeats.

Past studies by Weis-Fogh and Jensen [32], Ellington [38-40], Dickinson and Gotz [20] and Dickinson et al. [21] have attributed several key unsteady flow mechanisms responsible in producing lift forces. Some of these unsteady flow mechanisms such as ‘delayed stall’,

‘rotational circulation’ and ‘wake capture’ that were employed in the hovering ‘Figure of Eight’ stroke were briefly presented earlier. In reality, quantifying and characterizing any complete flight mode be it; forward or hovering flight can be really challenging. Experimentally, it has always been challenging to simulate or model an actual hovering insect and analyze the various interactions of wing motions and forces produced due to the small size and rapid wing beat frequencies of these insects. However, with the advancement in computational power, insect hovering flight can be realistically simulated in both two-dimensional and three-dimensional forms. Researchers like Wu et al. [37] were able to numerically validate these unsteady flow mechanisms during hovering flight and documented the significant impact of wing kinematics and fluid dynamics to the pitching posture of the insect. In the normal hovering flight where the mean stroke plane was maintained to be nearly horizontal while employing the ‘Figure-of-Eight’ wing motion, the insect actively pitched its stroke-plane angle by tilting its body forward or backwards. This active stroke-plane or outer-wing kinematic control mode was primarily used by small insects like fruit flies to ensure that the mean resultant force produced by the wings was predominantly vertical. This control process was personified as a ‘pendulum-like’ response to stabilize hovering insect flights [37]. Recent computational works by Sun and Tang [41] also emphasized the importance of flow mechanisms such as rapid wing acceleration and formulated unsteady aerodynamic correlations to the various wing kinematics and validated unsteady flow mechanisms. However, inherently, these computational simulations are based on numerous assumptions and exemptions.

1.2 OBJECTIVES

In the present study, flapping wings aerodynamics under low Reynolds number flows that encompassed studies of small natural fliers such as the fruit fly, *Drosophila* and dragonflies will be introduced. Fundamental experimental and analytical studies on insect flights by Ellington [38-40] and Dickinson et al. [20, 21] that create the foundation on unsteady flow dynamics such as ‘clap and fling’, ‘delayed stall effect’, ‘wake capture’ and ‘rotational circulation’ will be presented. Despite not specifically investigating on insects’ forward or hovering flight modes, it is still crucial to study these complex correlations between the key unsteady flow mechanisms and the aerodynamic performance of flapping wings in order to facilitate the rationalization of the present study’s experimental findings.

Figure 1-15 : Thrust Coefficient, C_T versus Reduced Frequency, κ for Single NACA0012 Oscillating Airfoil by Koochesfahani [42]

In 1989, Koochesfahani [42] had experimentally demonstrated that a single NACA0012 airfoil undergoing oscillating motion with small pitch amplitudes, $\alpha = \pm 2^\circ$ and $\pm 4^\circ$ at a low Reynolds number of 12,000 is not ideal for thrust production and produced thrust only at high reduced frequencies, $\kappa > 2$ as shown in Figure 1-15. Therefore, the present study intends to debate that with the incorporation of a hind wing, the aerodynamic performance of the oscillating airfoils in tandem can provide benefits in terms of lift and thrust production through the optimization of its flapping kinematic parameters such as pitch amplitudes, α , stagger distance, S_d , gap distance, G and phase angles, ϕ . In addition, Koochesfahani [42] focused only on small pitch amplitudes and through literature work by Lee [18], it is important to not only focus on pre-stall conditions as the critical flow events and the corresponding changes to the unsteady aerodynamic performance in the post stall conditions are considerably different.

In 1992, Scharpf and Mueller [43] were two of the pioneers to carry out experimental studies on tandem airfoils where two identical Wortmann FX63-137 airfoils were used in tandem configurations at a low Reynolds number of 8.5×10^4 . The tandem airfoils were held stationary as the difference in the angle of attack between the upstream and downstream airfoils known as the ‘decalage’, δ , were altered. The aerodynamic forces induced with changing of ‘decalage’, δ were measured using a three-component force balance capable to measuring the lift, drag and $1/4$ pitching moment forces. Apart from these force measurements, surface pressure measurements were carried out using pressure taps. Flow visualizations across the tandem airfoils were performed using kerosene smoke which is outdated to supplement the quantitative experimental data. The major difference between Scharpf and

Mueller's experimental study [43] with the present study is that their experimental results were exclusively on static cases and are governed by 'quasi-steady state' conditions. The vortex interactions between the fore and hind wings were also not investigated. Thus, the present study utilizes 2-D PIV measurements coupled with 6-axis force transducers to obtain more quantitative results in order to provide a more accurate and in-depth analysis of vortex interaction effects on aerodynamic performance for both upstream and downstream airfoils.

Figure 1-16 : Tandem NACA 4421 Airfoil Configuration [43]

However, their study has provided the definition of the physical parameters for tandem wing configuration as shown in Figure 1-16. The tandem wing configuration is represented by two wings or airfoils placed closely to one another which consists of the upstream or fore wing and the downstream or hind wing. In the present study, these tandem airfoils will pitch or oscillate about their aerodynamics centers which are $\frac{1}{4}c$ from their respective leading edges. The corresponding pitch angle or amplitude is denoted as α_{ua} and α_{da} for the upstream and downstream airfoils respectively. Stagger distance, S_d is defined as the dimensionless parameter that denotes the horizontal spacing between the tandem wings' aerodynamic centers and is expressed as $S_d = \frac{x}{c}$, where x is the horizontal distance between two wings and c is the chord. The gap distance, G is defined as the dimensionless parameter that denotes the vertical distance between the two wings and is expressed as $G = \frac{y}{c}$, where y is the vertical distance between the two wings' aerodynamic centers as shown in Figure 1-16.

Lee [18] carried out one of the key experimental studies on tandem airfoils undergoing sinusoidal oscillations where he investigated the aerodynamic effects at various S_d , ϕ and α at $G = 0$ under a constant free stream velocity, U_∞ . Force and PIV measurements documented by Lee [18] will be presented in the subsequent chapters and will provide the necessary validation for the present study. Lee [18] incorporated his knowledge on the critical flow

events such as flow reversal, turbulent breakdown, LEV formation, stall and flow reattachment in understanding the downstream airfoil's aerodynamic performance such as C_L , C_M and C_D in the tandem configuration. He concluded that tandem oscillating airfoils at $\phi = 180^\circ$ aerodynamically outperformed the in-phase oscillating airfoil at $\phi = 0^\circ$. The out-of-phase oscillating airfoils' better aerodynamic performance than the in-phase oscillating airfoils was attributed to the downstream airfoil's ability to retain its leading-edge suction peak, delay the onset of stall and in reducing the C_L hysteresis that increases aerodynamic damping. The effects of the three wing spacings, at $L_{\text{space}} = 0.3c$, $0.6c$ and $1.6c$ were also documented where it was concluded that the wing spacings, L_{space} and the phase differences both influenced the nature of the flow interaction such as the strength of the detrimental interaction effects on the downstream airfoil. Moreover, the downstream airfoil at $\phi = 0^\circ$ exhibited improvements in C_L and C_M with the increase in wing spacing as the detrimental effects of the flow interaction diminished along with the wake deficit. In contrast, for $\phi = 180^\circ$, C_L and C_M generally improve when L_{space} decreases.

However, Lee [18] study on tandem oscillating airfoils is limited to only small pitch amplitudes, $\alpha \leq 13^\circ$ and with a low $\kappa = 0.05$. Hence, the effects of down-washed vortex interactions on the downstream airfoil's performance at post stall conditions were never investigated. Lee [18] study did offer some insights on the effects of aerodynamic performance on the downstream airfoil due to the flow interaction with the varying wing spacings and phase differences but did not provide an in-depth rationalization relating to the probable vortex interactions that led to the changes in aerodynamic force production. As the flow interaction effects on the upstream airfoils were deemed to be less significant than on its downstream counterparts, Lee [18] focused only on the downstream airfoil's performance in his study.

One of the most important research gaps would be that most of the recent studies on tandem oscillating wings emphasized on coupled pitching and heaving kinematic motion both computationally and experimentally such as works by Broering and Lian [44, 45] and Lian et al. [46]. This means that there is a lack of recent literature works on tandem oscillating airfoils which is the main research objective of the present study. Despite the differences in airfoil kinematic motion, publications on tandem airfoils by Broering and Lian [44, 45] and Lian et al. [46] will be key references for the present study.

With the lack of recent literature works focusing on pure pitching or oscillating airfoils, the present study aims to provide reliable experimental data and new physics insights on oscillating tandem configurations in various kinematic parameters such as pitch amplitudes, stagger distances, phase angles and gap distances. In this report, pure pitching airfoils can also be termed as oscillating airfoils. As one of the primary objectives is to debate on whether pure pitching airfoils can be a viable propulsive mode, the present study aims to validate Koochesfahani's claims on pure pitching airfoil and also to prove that with the incorporation of tandem airfoil configuration, pure pitching tandem airfoils can be optimized and viable for certain flight modes such as hovering. Sharpf and Mueller [43] and Lee [18] have provided preliminary insights to the benefits that tandem configuration can offer but did not investigate the unsteady aerodynamics and vortex interaction effects at post stall conditions. The present study aims to address that gap by investigating oscillations at both pre and post stall conditions to study the impact and potential benefits of the full spectrum of flow mechanisms and vortex interactions on these tandem airfoils' aerodynamic performance. In addition, Lee [18] emphasized solely on the aerodynamic performance of the downstream airfoil thus neglecting its upstream counterpart. Broering and Lian [44, 45] and Lian et al. [46] did however document the effects on the upstream airfoil with the variation in S_d and ϕ but it is important to take account that the tandem flapping airfoils were in coupled heaving and pitching motion which differs from the present study. Hence, to effectively bridge this gap, the present study intends to examine the aerodynamic performance of both upstream and downstream airfoils in static and oscillating mode by varying the kinematic parameters such as α , S_d , ϕ and G . This will also address the fundamental question on whether incorporating a downstream airfoil can have an effect on the aerodynamic performance of the upstream airfoil and the vortex interaction modes in play. In order to further validate the accuracy of the present study's experimental results, CFD simulations were also carried out on single static airfoil case using the K- ϵ turbulence model.

The present study aims to investigate on the unsteady flow over two oscillating NACA4421 airfoils in tandem configuration under low Reynolds numbers flow of 8.6×10^3 in a closed loop water tunnel through employing two main experiments; (1) force measurements through the use of 6-axis force transducers and (2) Particle-Image-Velocimetry (PIV) measurements using a 2-D Litron Laser system. Real-time instantaneous aerodynamic forces produced by these oscillating airfoils are measured at a sampling frequency of 50Hz. The 2-D PIV measurement system provides the visualization of the vortical flow structures across the

airfoils by measuring the displacements of polymer particles in water and subsequently calculating the flow vorticities from the computed velocity vectors. Both force and PIV measurements setups and processing methods are presented in Chapter 4. The tandem airfoils are oscillated at various α ranging $\pm 10^\circ$ to $\pm 60^\circ$ at constant rotational speed, ω , of about 100deg/s which translates to a range of κ . Apart from varying α , the present study will investigate on the effects of varying S_d and G between the tandem airfoils at two phases angles of $\phi = 0^\circ$ (in-phase) and $\phi = 180^\circ$ (out-of-phase).

1.3 SCOPE AND METHODOLOGY

The present study will first review past experimental and computational works on single flapping wing and tandem flapping wing aerodynamics. Reviews on the effects of stagger distances and phase angles on the aerodynamic performance of tandem flapping wings will then be documented. Subsequently, the force and PIV experimental methods, setups and processing methods will be presented. The emphasis of the present study will cover the experimental results and discussions on the following cases below in sequence to investigate the effects of various tandem configurations on the aerodynamic performance of static and oscillating NACA4421 airfoils.

1. Single Static Airfoil
2. In-Phase Tandem Static Airfoils at various S_d
3. Out-of-Phase Tandem Static Airfoil at various S_d
4. Single Oscillating Airfoil
5. In-Phase Tandem Oscillating Airfoils at various S_d and G
6. Out-of-Phase Tandem Oscillating Airfoils at various S_d

1.4 PUBLICATIONS BASED ON CURRENT WORK

1.4.1 Journal Paper Publication

1. Seet, A., Yu, C.M. and Chua, L.P., “Effects of Stagger Distances on In-Phase Oscillating Airfoils in Tandem Configuration at Low Reynolds Number Flow,” *Journal of Aircraft*, 2019. **(In Preparation for Submission)**

2. Seet, A., Yu, C.M. and Chua, L.P., “Experimental Study on Flow over Oscillating Airfoils at Various Stagger Distances and Phase Angles at Low Reynolds Number,” Experiments in Fluids, 2019. **(In Preparation for Submission)**

1.4.2 Conference Paper Publication

1. Seet, A., Tang, H. and Yu, C.M., “Experimental Study of Flow over Two Oscillatory Pitching Airfoils in Tandem Configuration,” 31st AIAA Applied Aerodynamics Conference, No. 2817, 2013 [47]. **(Published and Presented)**

2. LITERATURE REVIEW

2.1 SINGLE FLAPPING WING AERODYNAMICS

Figure 2-1 : Knoller-Betz Effect showing the Thrust (T) and Lift (L) Components of the Normal Force Vector (N) during Heaving Motion in (a) Downstroke; (b) Upstroke [48]

One of the first scientific theories surrounding flapping wing was focused on heaving or plunging airfoil. Knoller [28] and Betz [29] were credited to be the first to observe that a heaving airfoil in a moving free stream produced a net thrust. The up and down heaving motion induced an effective velocity component, V_{eff} , that lead to an effective angle of attack, α_{eff} that is coupled with a normal vector, N as shown in Figure 2-1 [48]. This normal vector, N , can be resolved into both thrust, T , and lift forces, L . This phenomenon is better known as the 'Knoller-Betz' effect. Since then, in 1922, Katzmayer [49] validated the 'Knoller-Betz' effect by carrying out an experimental study by subjecting the stationary airfoil to a sinusoidally oscillating freestream velocity. Katzmayer consistently validated the Knoller-Betz

effect and concluded that an airfoil in an oscillating wind stream can generate a thrust force. In Katzmayr's study, the Knoller-Betz model did not take account of the effects of vortex generation and interactions on the aerodynamic performance of the airfoil.

In 1934, motivated by the research on 'flutter', Theodorsen [50] formulated an analytical method for the approximation of the unsteady lift and moment on oscillating airfoils under a harmonic motion. This analytical method was based on the assumption that the flow is incompressible and inviscid. The force generation of these oscillating airfoils were assumed to be produced by very small amplitudes of oscillation, thus the flow over the airfoil remained attached as the 'Kutta condition' was applied to the trailing edge. The trailing wake was modeled as a continuous vortex street with undulating strength and was not affected by the wake velocity. Despite these assumptions, Theodorsen's analytical model was proven to be useful and was used in the years after to investigate the effects of flutter primarily on the study of turboprop aerodynamics and also in flapping wing flight [50]. Following the emergence of Theodorsen's inviscid incompressible analytical model, in the 1940s, there were a lot of focus on studying the aerodynamics of coupled heaving and pitching airfoils due to the need for flutter and wind gust analysis. As a result, a lot of research works then gravitated on determining lift forces without much work put into investigating the potential of thrust generation by employing flapping wings.

On the positive note, in 1935, Von Kármán and Burgers [51] founded one of the first theories on drag or thrust generation based on the orientation and location of the wake vortices. In their experimental study on an airfoil in incompressible flow, they observed a wake consisting of two rows of counter-rotating vortices that was responsible for generating thrust forces. Von Kármán and Burgers [51] crucial findings on the wake vortices will be presented in the following chapter.

Garrick [52] published his theoretical work by applying Theodorsen's inviscid flat plate theory and linearized potential flow analysis to ascertain the thrust forces produced by heaving and oscillating airfoils. He concluded that the heaving airfoil was able to produce thrust forces across the entire range of flapping frequencies whereas the oscillating airfoil was only able to produce thrust above a certain critical flapping frequency, f_c , and was more influenced by its pivot location [48, 52]. These observations were similar to the ones made by Koochesfahani [42] who concluded for a NACA0012 oscillating airfoil oscillating at small amplitudes, thrust forces were only produced at reduced frequency, $\kappa > 2$.

As one of the main motivations for this current study is to emulate the aerodynamic prowess of flying insects in particular, the dragonfly, it is important that this chapter covers the pioneering work and quintessential breakthroughs in this realm. In 1956, the pioneering work by Weis-Fogh and Jensen [32] modeled insect flight as a 'quasi-steady state' model which assumed that steady state forces are generated at each instantaneous position throughout a full stroke cycle. This model assumed that the forces generated depend solely on airfoil geometry, flow velocity and angle of attack; ignoring time-dependent characteristics of force generation. The limitations of this 'quasi steady state' model were well documented such as in the works published by Ellington [38-40] where he stated that the predicted C_L in the 'quasi steady state' model are higher than the actual forces measured due to the fact that unsteady time-dependent flow mechanisms during the flapping cycle were not taken into account.

Figure 2-2 :Angular Movements of the Long Axis of the Wing Relative to the Body in *Encarsia Formosa* with an Indication of the ‘Clap’, ‘Fling’ and ‘Flip’ Phases [53]

However, Weis-Fogh [53] was to become one of the founding fathers of the insect flight aerodynamics when he published an persuasive paper in 1973 introducing the scientific world to the very first unsteady flow mechanism, the ‘Clap-and-Fling’ when he investigated an exceptionally high average $C_l \approx 2$ to 3 produced by a very small insect, the *Encarsia Formosa* during its hovering flight as shown in Figure 2-2. Researchers such as Bennett [54] and Maxworthy [55] spurred on to perform more experiments to evaluate and validate this ‘Clap and Fling’ mechanism or also known as ‘Weis-Fogh’ mechanism. Inspired by the intriguing work done by these researchers, Dickinson and Gotz [20] and Dickinson et al. [21] published numerous ground-breaking studies to dissect the kinematics of insect flight to the various phases; translational phase (heaving) and rotational phase (pitching and surging). Through

carrying out experimental studies on insect flight, Dickinson documented three distinct yet interactive unsteady flow mechanisms; (1) delayed stall, (2) rotational circulation and (3) wake capture or wing-wake interaction. For the purpose of providing an analytical reference for the present study, these fundamental unsteady flow mechanisms will be reviewed in the Chapter 3.

Numerous experimental and computational studies on the single flapping wing that encompassed the coupled heaving and pitching motion have yielded conflicting conclusions regarding the effects of these unsteady flow mechanisms on aerodynamic performance. Lua et al. [35] investigated on the effect of ‘wing-wake interaction’ on lift generation of a heaving airfoil and found that there were two possible outcomes of wing-wake interaction. First outcome occurred when the wing meets a pair of counter-rotating wake vortices on the reverse stroke where the vortices impinged directly on the windward side which resulted in the acceleration of the incoming flow to the wing that increased lift production. Second outcome occurred when the wing encountered one vortex on the reverse stroke; the close proximity together with the vortex suction effect caused a decrease in net force which in turn reduced lift production. Conclusively, the study showed that lift production due to wing-wake interaction depends on the flapping motion and the timing of the stroke reversals. On the contrary, the computational study by Sun and Tang [41] on a fruit fly executing a coupled heaving and pitching motion showed that the wing-wake interaction was primarily detrimental to lift production due to the downwash of the wake vortices.

Dickinson and Gotz [20] also carried out a two-dimensional experimental study on a heaving airfoil at high angle of attack and documented on the ‘delayed stall’ effect that contributed to an 80% increase in lift production over the first two chords of travel as compared to the lift produced over 5 chords of travel later. However, it was argued that this increase in lift was due to the ‘detached vortex-lift’ mechanism seen in fixed wing steady-state thin airfoils such as the delta wing configuration. One aspect of that argument stated that due to the large LEV generated, the flow over the LEV was accelerated thus resulting an increase in the resultant lift force vector. Dickinson et al. [21] also documented that an insect does not solely rely on the lift generated through translating motion and the effects of ‘delayed stall’ but in order to generate excess lift force, other unsteady flow mechanisms such as ‘rotational circulation’ and ‘wake capture’ played a significant role in insect flight as well. In addition, the term ‘two-dimensional’ in the present study indicates that the airfoil has an infinite length which

means that the forces produced over the airfoil are not affected by wing tip effects and the effects of span-wise vortices will not be investigated.

In the numerical research area, linearized potential flow methods were one of the first numerical methods adopted by researchers such as Theodorsen [50] and Garrick [52]. However, this linearized potential flow method had its limitations as it could only approximate forces generated at very small amplitudes of oscillation and over a thin plate. To overcome these limitations, Hess and Smith [56], Giesing [57] and Teng [58] were recognized for developing the 'panel methods' for both steady and unsteady flow conditions. With these panel methods, it eliminated the restrictions on amplitudes and airfoil geometry while maintaining the potential flow conditions by positioning sources and vortices on the airfoil surface instead of along the chord line.

Figure 2-3: Measured and Predicted Thrust Coefficients, C_T versus κ [42]

Koochesfahani [42] then carried out his study on the thrust performance of an oscillating airfoil and compared his measurements represented by the black dots with the linear potential flow theory and panel-code methods as shown in Figure 2-3. Both of these linear potential flow theory and panel-code methods generally produced higher thrust coefficient values in relation to Koochesfahani's experimental results. In both of the linear potential flow theory and panel-code methods, the flow was assumed to be inviscid which meant that the separation point has to be pre-determined at its trailing edge due to the application of the 'Kutta condition'. As the flow remained attached over the surface of the airfoil, the effects of LEV generation and dynamic stall on the aerodynamic performance of these oscillating airfoils were not accounted for and may explain the higher C_T production in relation to

Koochesfahani's experimental results as shown in Figure 2-3. In order to overcome these limitations of the linear potential flow theory and panel methods, Navier-Stokes (NS) solvers were developed and with these solvers, the inviscid flow condition can be eliminated, thus enabling a large permutation of flow conditions to be simulated such as turbulent flows to study viscous effects. One major advantage of using these Navier-Stokes solvers is that the flow separation point on the airfoil surface is predicted rather than being pre-determined under the 'Kutta condition' which meant flow events such as effects of dynamic stall on aerodynamic forces can be investigated.

Figure 2-4 : (a) Lift Coefficient and (b) Drag Coefficient of a Single NACA0012 Airfoil in Pure Heaving Motion at $h_a = 0.1c$ and $\kappa = 1.5$ [59]

Figure 2-5 : (a) Flow field Computed by Navier-Stokes and (b) Discrete Wake Vortices computed by UPOT; at $h_a = 0.1c$ and $\kappa = 1.5$ [59]

Tuncer and Platzer [59] were one of the pioneers that employed Navier-Stokes computations to simulate unsteady and viscous flow over a single heaving NACA0012 airfoil. They also simulated flow over a heaving upstream airfoil and a stationary downstream airfoil in tandem. In their study, they compared and validated the force coefficients produced by the single flapping airfoil case with the unsteady potential flow solution method (UPOT) as shown in Figures 2-4 (a) and (b). They [59] also concluded that the vortex wake shed by the single heaving airfoil as shown in Figure 2-5 (a) agreed with the UPOT's results in Figure 2-5 (b) as it exhibited a 'thrust-producing' vortex street or the Reverse Von Kármán vortex street. These Von Kármán vortex street characteristics will be elaborated and discussed in the subsequent chapter.

2.1.1 Oscillating Airfoils Review

Figure 2-6 : (a) Location of the Hot-Film Sensors around NACA0012 Airfoil Profile; (b) and (c) Multi-element Hot Film Sensor (MHFS) arrays [60]

One of the most compelling experimental studies on oscillating airfoils was published by Lee and Gerontakos [60] where it characterized the aerodynamics of oscillating airfoils in a systematic and comprehensive manner which is relevant to the present study on oscillating tandem airfoils. Their experimental research focused on the unsteady boundary layer characteristics of the flow over an oscillating NACA0012 airfoil at pre-stall, during stall and post-stall conditions [60]. They utilized an array of hot-film sensors that were closely arranged on the surface of the airfoil which aided the investigation of various critical boundary layer points and stall events in non-intrusive manner as shown in Figure 2-6. Surface pressure measurements, hot-film wake measurements and smoke visualizations were employed to supplement the hot-film measurements. In the following sub-sections, Lee and Gerontakos work [60] on the oscillating airfoil at various stall conditions will be reviewed.

2.1.1.1 Static Single Airfoil Case [67]

Figure 2-7: Boundary-Layer Events of a Static NACA0012 Airfoil at $Re_c = 1.35 \times 10^5$. (a) Critical Flow Points: $\circ$, Laminar Separation; $\square$, Peak Transition, Δ , TE Flow Separation, (b) C_L - α Curve, (c) Bubble Length l_b , $\circ$, C_p ; Δ , MHFS, (d) C_p Plot [60]

The experimental results shown in Figure 2-7 from Lee and Gerontakos [60] on a single static NACA0012 airfoil case presents the various unsteady boundary layer and stall events at small pitch angles, $0^\circ < \alpha < +15^\circ$. The critical boundary-layer flow points such as the transition, laminar and trailing-edge separation points were also presented in Figure 2-7.

For $\alpha < 10^\circ$, Figure 2-7 (a) shows that the critical flow events such as the laminar separation, transition and the trailing-edge separation were found to propagate linearly upstream. Similarly, in Figure 2-7 (b), the lift coefficient was seen to increase linearly with the increase in angle of attack, α . However, at $\alpha > 10^\circ$, the critical flow points and lift coefficient exhibited a nonlinear trend. Figure 2-7 (c) shows the movement of laminar separation bubble towards the leading edge and also the shortening of its length as α increases. Based on the pressure distribution plot in Figure 2-7 (d), the bubble ceased to exist at $\alpha > 13^\circ$,

characterized by a flat surface pressure distribution which indicates a complete boundary layer separation. Hence, concluding that for a static NACA0012 airfoil at $Re_c = 1.35 \times 10^5$, the sharp leading edge static stall occurs at $\alpha_{ss} = 13^\circ$ and was caused by the bursting of the laminar bubble.

2.1.1.2 Oscillating Single Airfoil [60]

Oscillations within the static stall angle, α_{ss}

Figure 2-8 : Boundary Layer-Events; (a) Transition and (b) Relaminarization for $\alpha(t) = 7.5^\circ \sin \omega t$; $\circ$, $\kappa=0.025$; $\square$, 0.05; Δ , 0.1 and (c) Dynamic-Load Loops for $\kappa=0.05$; ‘...’, Static Values and $\diamond$, Static Airfoil [60]

For this attached-flow oscillating case, the NACA0012 airfoil was oscillated within its static stall angle, α_{ss} at $\kappa = 0.05$ with $\alpha(t) = 7.5^\circ \sin \omega t$. When compared to the static case, the transition points were delayed and exhibited gradual decreasing trend with the increase in α as shown in Figure 2-8 (a). Similarly, the relaminarization points were also delayed and displayed a more linear decreasing trend with the increase in α as shown in Figure 2-8 (b). The flow remained attached throughout the oscillation cycles and the bubble length was insensitive to the change in α . However, there were slight increments in the lift-curve slope,

lift coefficient, C_L and the pitching moment coefficient, C_M whereas the drag coefficient, C_D was found to be similar to the static case as presented in Figure 2-8 (c) [60].

Oscillations at post static stall angle, α_{ss}

Figure 2-9 : (a) C_L , (b) C_D and (c) C_M Dynamic Load-Loops at $\kappa = 0.1$ for $\alpha(t) = 10^\circ + 15^\circ \sin(\omega t)$; Critical Flow Points; (1) Onset of Flow Reversal; (2) End of Upward Spread of Flow Reversal; (3) Turbulent Breakdown; (4) Lift Stall; (4) – (5) Full Stall; (6) Onset of Secondary Vortex; (7) Onset of Flow Reattachment; (8) End of Flow Reattachment [60]

For the NACA0012 airfoil oscillating beyond its static stall angle, $\alpha_{ss} = 13^\circ$, the unsteady boundary layer flow events were more complicated than the previous two cases presented due to the presence of flow reversal, turbulent breakdown and the initiation and convection of LEV. Figure 2-9 shows the dynamic load-loops of the oscillating airfoil at $\kappa = 0.1$ with $\alpha(t) = 10^\circ + 15^\circ \sin(\omega t)$ where the critical flow points are labeled to illustrate the changes in flow events during the oscillation with $\alpha_{max} = 25^\circ$.

Figure 2-10 : Selected Sequences of Boundary-Layer Events Both prior to, during, and after stall at $\kappa = 0.1$ for $\alpha(t) = 10^\circ + 15^\circ \sin(\omega t)$; (a)-(g) correlates to the Critical Flow Points and (h)- (n) are the corresponding Conceptual Sketches [60]

Figure 2-10 shows the selected sequence of boundary layer events which correlates to the critical flow points in Figure 2-9 using snapshots of the smoke visualizations. The sequence of the boundary layer events during the oscillation of the NACA0012 airfoil exhibiting the critical flow points prior to, during and after stall are presented below.

1. For critical flow points (1) to (2) : Onset and Upward spread of flow reversal,

Prior to static stall, at $\alpha < 13^\circ$, the boundary layer remained attached with the exception of the trailing edge. The transition to turbulent flow and its return to laminar state can be identified from the drastic rise and fall of the multi-element hot-film sensor array (MHFS) outputs in every cycle. As α increases $> \alpha_{ss} = 13^\circ$, the flow reversal was first seen at $\alpha_u = 12.9^\circ$ or at $s/c = 0.884$ as it subsequently propagates upstream to $s/c = 0.26$ at $\alpha_u = 21.6^\circ$. This flow reversal is characterized by a thin layer at the bottom of the thickened turbulent boundary layer as shown in Figures 2-10 (a) and (h).

2. For critical flow points (2) to (3) : Turbulent Breakdown and LEV Formation,

In Figures 2-10 (b) and (i), the flow remained attached up till $s/c \approx 0.14$ at $\alpha_u = 21.8^\circ$ where the turbulent boundary layer underwent a sudden breakdown which fluctuated rapidly both upstream and downstream. As the airfoil continues to pitch upwards, this turbulent breakdown induced the formation, growth and convection of the LEV at $\alpha_u = 21.9^\circ$ with an initial length of $0.14c$ illustrated in Figures 2-10 (c) and (j). The presence of LEV is vital in significantly increasing the resulting C_L , C_D and C_M values as indicated as Point (2) in Figures 2-9 (a), (b) and (c) respectively.

3. For critical flow points (3) to (4) : LEV Growth and Rearward Convection,

As the airfoil pitches upwards from α_u to 24.4° , the LEV grew in size and is convected rapidly rearwards as exhibited in Figures 2-10 (c) and (j).

4. For critical flow point (4) : Dynamic or Lift Stall,

As the airfoil nears the end of its upstroke at $\alpha_u = 24.7^\circ$, it experienced dynamic stall that is characterized by the dramatic decrease in lift soon after reaching its maximum lift coefficient, $C_{Lmax} = 2.44$ as shown in Figure 2-9 (a). Here, the LEV that has grown to $0.9c$ in length is observed to undergo a catastrophic detachment as shown in Figures 2-10 (d) and (k).

5. For critical flow points (4) to (5), Full Stall,

After the LEV spilled over the trailing edge, the airfoil experienced a full stall momentarily where the flow completely separates and the lift coefficient dramatically decreases from its $C_{Lmax} = 2.44$ to $C_L \approx 1.30$ as shown in Figure 2-9 (a). Figures 2-10 (e) and (l) effectively illustrate the post dynamic stall flow condition of a separated flow for $\alpha_d < 14.1^\circ$.

6. For critical flow points (5) to (6), Onset of Secondary Vortices,

During the airfoil downstroke at $\alpha_d = 21.8^\circ$, there is a slight increase in lift coefficient denoted in Point (6) in Figure 2-9 (a) which is attributed to the onset and convection of weaker secondary vortices. Based on the MHFS outputs, the loci for the boundary layer events can be plotted and analyzed. Through these loci plots, it was documented that the LEV and secondary vortices propagated at 45% and 30% of the U_∞ .

7. For critical flow points (6) to (8), Flow Reattachment,

After these secondary vortices are shed and spilled over the trailing edge, the separated flow starts to reattach to the airfoil suction surface at $\alpha_d = 14.1^\circ$. The rearwards reattachment of the flow is shown in Figures 2-10 (f), (g), (m) and (n) as the force coefficients returned to its pre-stalled values.

One major difference between this deep stall oscillating case and the static case was that for the oscillating case, the leading edge dynamic stall was not caused by the bubble bursting but began with an abrupt turbulent breakdown of the flow near the LE. Subsequently, the turbulent separation point moved upstream and downstream rapidly with the increase in α until it reached the bubble reattachment point where it induced the bursting of the laminar bubble. Lee and Gerontakos [60] also concluded that there was a considerable hysteresis in the phases and pitch angles between the unsteady flow separation and reattachment points. They highlighted that for oscillating airfoils, small changes in the κ will significantly delay the onset of various boundary layer events resulting in considerable variations in force coefficient peak values [60].

Oscillations through α_{ss} (Light Stall Case)

In this light stall case, the NACA0012 airfoil was oscillated through a maximum angle of attack, α_{max} of 15° which is 2° above the α_{ss} of 13° . The flow events were considerably different from that of a deep-stall case as the lift stall was mainly caused by the rapid trailing-edge dynamic stall. As the airfoil pitched upwards, the flow reversal propagated to $s/c = 0.4$ and was ensued by a turbulent breakdown near the top of the oscillation. This breakdown resulted in the initiation of a vortex-like circulation with a length of $0.3c$ which did not have time to grow and was quickly dissipated by the pitch-down stroke [60].

Conclusively, this experimental study by Lee and Gerontakos [60] on the oscillating airfoil at three stall conditions provided a good understanding and reference on the unsteady critical flow events across the suction surface of the airfoil and its corresponding effects on aerodynamic forces and pressure distributions. Despite the fact that numerous studies have concluded that the oscillating airfoils under sinusoidal motion are not ideal for thrust generation, the present study intend to prove otherwise by exploring the effects and benefits of oscillating motion in various tandem configurations. Moreover, these past studies have not thoroughly study the vortex shedding characteristics of oscillating airfoils and the effects of down-washed vortex interactions on the aerodynamic performance experimentally.

2.1.2 Computational Studies on Single Flapping Wing Aerodynamics

Several notable computational studies on single flapping wing aerodynamics will be reviewed which focus on the importance of computational methods and its progression through the years. In the previous section, experimental studies on oscillating airfoils such as by Lee and Gerontakos [60] were reviewed where they utilized and developed novel experimental techniques such as the use of multi-element hot film sensor arrays (MFHS) embedded on the airfoil surface. These experimental methods and techniques provide the means to investigate flow patterns, critical flow points and also to acquire data to study the various flow mechanisms. However, these experimental works are time-consuming and very costly as it typically involves (1) formulation of Design of Experiment (DoE), (2) sourcing for equipments and consumables, (3) calibrating of equipments, (4) developing experimental capability and competency and (5) laborious post-processing of experimental data. Given these drawbacks with experimental studies, many research institutions and universities are focusing their efforts and resources on computational studies as it may be less costly in the long term and with a mature and robust platform, simulations can be carried out efficiently.

In the recent years, the progression and development of computational methods have been increasing exponentially. From Theodorsen's inviscid and incompressible model [50] through to Garrick's linear theory [52] and the development of the Panels methods [56-58], numerical theories have evolved to robust computational methods such as the Navier-Stokes solvers capable to simulating viscous flow effects [59]. One of the key gaps that computational methods are so effective at bridging would be simulating three-dimensional viscous unsteady flow problems.

In the context of the present study, 3-D unsteady aerodynamics modeled the flapping wing with a 'finite' wing span which accounts for the forces generated at the wing tips that create a flow velocity that propagates along the wing span. Ramamurti's group [61-66] was recognised as the pioneers in 3-D computational studies on unsteady aerodynamics on insect flight. Ramamurti and Sandberg [64-66] carried out these 3-D computational simulations using an incompressible flow solver based on Navier-Stokes equations in Arbitrary Lagrangian-Eulerian (ALE) formulation. This flow solver have been successfully evaluated and validated in 2-D and 3-D studies on laminar and turbulent flow conditions [61-63].

$$\frac{dU}{dt} + V_a \cdot \nabla U + \nabla p = \nabla \cdot \sigma \quad (\text{Eq. 2.1})$$

$$\frac{dU}{dt} = \frac{\partial U}{\partial t} + w \cdot \nabla U \quad (\text{Eq. 2.2})$$

$$\nabla \cdot U = 0 \quad (\text{Eq. 2.3})$$

The governing Navier-Stokes equations in ALE formulation are written as shown in Equations 2.1, 2.2 and 2.3 where, p denotes pressure, and $V_a = U - w$ is the advective velocity where U is the flow velocity and w is the mesh velocity. Pressure, p and stress, σ terms have been normalized by the constant density, ρ , and are discretized in time, t , using an implicit time-stepping procedure. This flow solver was effectively designed to be able to simulate flows with moving bodies and to capture the unsteadiness of a flow field [64-66].

Ramamurti and Sandberg [64, 65] carried out 3-D simulations at various phase angles between the rotational and translation motions; namely at 3 wing rotation conditions; (1) symmetrical; (2) advanced and (3) delayed where the rotational motion is (1) in-phase; (2) precedes and (3) delayed with respect to the stroke reversal respectively. The force time histories from the simulations were then compared to Dickinson et al. [21] experimental results for validation with the aim to understand the role of rotational motion and LEV during flight and its interactions with the axial flow.

Figure 2-11 : (a) Lift and (b) Drag Forces for (i) Symmetrical; (ii) Advanced and (iii) Delayed Wing Rotation Phase Conditions [21, 64 and 65]

Figure 2-12 : Velocity (in cm s^{-1}) Vector Plots at $t = 12.24\text{s}$ showcasing the Flow Separation after Pronation at (a) TE and (b) Wing Tip [65]

For the symmetrical case as shown in Figure 2-11 (i) where the wing rotation occurred in-phase relative to the stroke reversal, the variation of the lift and drag forces during the translation phase were predicted correctly but the computed lift forces produced were higher than those of Dickinson et al. [21] experimental results. The non-symmetrical nature of Dickinson et al. [21] results during the downstroke and upstroke was attributed to the mechanical play in the experimental arrangement [65]. For points 0 to 3, during pronation where the wing rotated near the beginning of the downstroke, the lift decreased between point 0 and 1 and subsequently increased till point 2. This increase in lift was due to the ‘rotational circulation’ mechanism. Another explanation for this increase in lift between points 1 and 2 would be the effect of ‘wake capture’. As the wing moves along its downstroke, it moves through the LEV generated by the preceding upstroke which was located below the wing and rotating in the CCW direction. This wing-wake interaction produced a stagnation region at the pressure side of the wing which resulted in an increase in lift. The decrease in lift from points 2 to 3 was attributed to the flow being separated at the trailing edge and wing tip as shown in Figure 2-12 which led to a higher pressure on the upper wing surface, hence lift decreases. From points 3 to 4, the combined rotational effect and translation motion continued to produce constant lift with a plateau in the middle which was attributed to the shedding of the trailing edge vortex. Similar trends were also observed during the supination phase from points 4 to 5 and from points 5 to 7.

Figure 2-13 :Velocity (in cm s^{-1}) Vector Plots near the Leading Edge early in the Downstroke for the 'Advanced' Wing Rotation in relation to the Stroke Reversal [65]

In the advanced case where the wing rotation occurred prior to the stroke reversal, the peak in lift was attained prior to stroke reversal due to the benefits of rotational circulation as discussed earlier. This lift peak of 0.56N produced at $t = 10.76\text{s}$ was significantly higher than the lift peak of 0.47N produced for the symmetrical case as seen in Figure 2-11 (ii). As the rotational mechanism weakened, lift decreased significantly to -0.2N right before the upstroke. As the wing began its downstroke, the lift increased as the wing moved through the wake created by the preceding upstroke and creating a high pressure region at the pressure airfoil side thus the benefits of 'wake capture' were exhibited as displayed in Figure 2-13.

Figure 2-14 : Velocity (in cm s^{-1}) Vector Plots at the LE (a and b) and at the TE (c and d) early in the Downstroke for the 'Delayed' Wing Rotation Case [65]

In the ‘delayed’ case where the wing rotation only occurred after the stroke reversal, the ‘rotational circulation’ mechanism did not generate any positive lift before the start of the downstroke which was different from both the symmetrical and advanced cases as shown in Figure 2-11 (iii). The LEV from the upstroke was also absent after $t = 12.05\text{s}$ as presented in Figure 2-14 (a) and (b) which resulted in a high pressure region at the pressure side of the airfoil and lift was reduced [65]. Subsequently, the coupled rotational and translational motion of the airfoil produced positive lift but at $t=12.8\text{s}$, there was a plateau region as shown in Figures 2-11 (iii) (a) which was attributed to the presence of a trailing edge vortex on the suction airfoil surface depicted in Figure 2-14 (c) as the increase in pressure caused a momentary loss in lift. The increase in lift resumed after this trailing edge vortex was shed at the trailing edge as exhibited in Figure 2-14 (d).

Figure 2-15 : Velocity Contours at the Beginning of the Downstroke; (a) Advanced; (b) Symmetrical and (c) Delayed relative to the Stroke Reversal [65]

The velocities at the beginning of the downstroke for all 3 cases are displayed in Figure 2-15 and it is evident that the higher lift peaks correlated to the higher fluid velocity the wing is moving through as in the ‘advanced’ wing rotation case. Conclusively, Ramamurti and Sandberg [64, 65] computational results validated and agree very well with Dickinson et al. [21] experimental results.

Figure 2-16: Three-Dimensional Simulation of a Hovering Fruit Fly, *Drosophila* [66]

Following this validation study, Ramamurti and Sandberg [66] carried out 3-D unsteady computations specifically on the *Drosophila* under hovering and free flight. They simulated the generation of leading-edge and trailing-edge vortices as shown in Figure 2-16 where they observed a distinct loop-like vortex structure shed from the wing's leading edge and wing tips during stroke reversals such as at $t = 0.02493s$. After these stroke reversals at $t = 0.02527s$, this loop-like vortex structure pinches off into $\wedge$ -like structures and propagates downstream until $t = 0.02729s$. This significant observation has not been documented previously in the studies of flapping wings and demonstrated one of the many benefits of 3-D computational simulations [66].

k	$\overline{C}_T$ (2D)	$\overline{C}_T$	Efficiency(2D)	Efficiency
0.1	0.0158	0.0136	0.883	0.854
0.2	0.0466	0.0399	0.763	0.743
0.5	0.1844	0.1687	0.585	0.550

Table 2-1 : Mean Lift and Propulsive Efficiency with Varying κ for 2-D and 3-D Pure Plunging NACA0014 Airfoil at $h_a = 0.4c$ and $AR=8$ [67]

Figure 2-17: Propulsive Efficiency and Mean Lift Coefficient for 2-D NACA0014 Airfoil with varying (a) ϕ and (b) α [67]

San and Hu [67] developed a numerical method to solve Euler/Navier-Stokes equations to perform 2-D and 3-D computational simulations on a NACA0014 airfoil in pure heaving motion and in coupled heaving and pitching motion. They investigated the effects of varying κ , mean angle of attack and α with h_a fixed at $0.4c$. Through the 2-D pure heaving motion simulation results, it was concluded that the mean lift output and propulsive efficiency, η , were not affected by the mean angle of attack but primarily affected by κ . Then, 3-D pure heaving simulations at $h_a = 0.4c$ with varying κ and with an aspect ratio, $AR = 8$ were carried out. The results shown in Table 2-1 showcased a consistent reduction in mean lift and η across the range of κ tested when compared to the 2-D simulation results which was attributed to the influence of the unsteady tip vortex [67]. Subsequently, the NACA0014 airfoil was simulated in 2-D and 3-D under coupled heaving and pitching motion to study flapping flight at varying ϕ as shown in Figure 2-17 (a) and at varying α with $\phi = 90^\circ$ as shown in Figure 2-17 (b) with h_a is fixed at $1.0c$ and with κ set at 0.1 . Conclusively, in Figure 2-17 (a) for the coupled heaving and pitching motion, the max propulsive efficiency, η is achieved at $\phi = 90^\circ$ whereas the highest mean lift coefficient was attained at $\phi = 270^\circ$ which is where the lowest $\eta = 80\%$ was exhibited. On the other hand, Figure 2-17 (b) demonstrates that the increase of α will linearly increase η and decrease the mean lift output.

Figure 2-18 : Vorticity Contours, ω_x along the Flight Path of 3-D Coupled Motion [67]

For the 3-D simulation of the coupled motion, through employing Navier-Stokes calculations, the vorticity contours of the wake were visualized as shown in Figure 2-18 where the solid line represents positive vorticity values and dashed lines represents negative vorticity values. Through viewing from the rear of the wing, the longitudinal lines near the tip region represents a roll-up wake. Through the downstroke and upstroke of the wing, a Von Kármán vortex street can be seen which consists of CW and CCW vortices. These 3-D computational studies have proven instrumental in modeling the effects of wing-tip effects such as roll-up vortices. However, these 3-D unsteady aerodynamic effects can be very difficult to visualize

through experimental means. As the main focus is on the vortex interactions between the tandem airfoils, the 2-D approach would be more suited for the present study. In this 2-D approach, the airfoil are modelled with an 'infinite' span width with the force generated by these airfoils and the flow structures observed are idealized to be independent of any span-wise or wing tip effects. Past experimental works by Anderson [68], Freymuth [69] and computational studies by Jones and Platzer [70] have also provided the foundation and insights on 2-D unsteady aerodynamics.

2.2 TANDEM WINGS AERODYNAMICS

Through the work done by Von Kármán and Burgers [51] and Garrick [52] on oscillating airfoils, it is known that a substantial amount of energy produced through the flapping motion was lost in the form of vortex shedding in the trailing wake. Schmidt [71] proposed a novel idea that some of the vortical energy can be captured by introducing a stationary airfoil or wing in the trailing wake of an oscillating airfoil. Schmidt [71] then went on to produce his novel-designed wave propeller which he claimed to have achieved similar efficiencies as conventional propellers,, thus demonstrating an early indication of the benefits and applications of tandem airfoil configurations.

One of the inspirations to study tandem airfoils emanates from the aerodynamic prowess of the dragonfly which are unique natural fliers known for their impressive maneuverability and capability for hovering over long periods of time. One of the distinctive features of the dragonfly is the use of its pair of tandem wings which can operate independently and its ability to vary their wing kinematics such as by controlling phase angles, ϕ between its fore wings and hind wings as documented by Alexander [17]. Norberg [72], Saharon and Luttges [73] and Reavis and Luttges [15] have also provided comprehensive insights on dragonflies' aerodynamic performance capabilities and its wing kinematics. Norberg [72] has provided morphological wing data, including wing shape, size, mass distribution, and weight. Somp and Luttges [14] documented that the tethered dragonfly was able to produce extremely high lift forces up to twenty times its average body weight. Reavis and Luttges [15] also carried out force measurements and flow visualizations on six Asechna Palmetta Dragonflies in two flight modes; escape mode and hover mode and introduced the tandem wing configuration parameters such as the stagger distance, S_d and the gap distance, G which were defined earlier in Section 1.2.

2.2.1 Effect of Stagger Distance on Tandem Wing Aerodynamics

Figure 2-19 : Schematic of the computational grid and boundary conditions used in the study (not shown to scale) [44, 45]

Following Reavis and Luttges [15] inspiring work on dragonflies, Broering and Lian [44, 45] and Lian et al. [46] studied the benefits and effects of tandem wing configuration. Broering and Lian [44] published a computational study on the effects of wing spacing or stagger distances on tandem wing aerodynamics at a low Re flow of 5000. In Broering and Lian [44, 45] study, wing spacing is denoted as the horizontal distance between the upstream airfoil's TE and the downstream airfoil's LE which is then non-dimensionalized by its chord, c . The flow field simulation was produced by solving the incompressible Navier-Stokes equations through discretization via an overlapping grid using a second order central differencing scheme to approximate the velocity and pressure terms. The tandem wing motion is controlled by using an overlapping moving grid shown in Figure 2-19 [44, 45]. The tandem wings were oscillated in-phase ($\phi = 0^\circ$) in a coupled sinusoidal pitching and plunging motion and at four wing spacings of 0.1, 0.25, 0.5 and 1.0 which are equivalent to Sd of 1.1, 1.25, 1.5 and 2 respectively.

Figure 2-20: Time Dependent Force Coefficients for Tandem Wings at $\phi = 0^\circ$ (In-Phase) at Different Wing Spacings compared to a Single Wing; (a) Fore Wing Lift, (b) Fore Wing Thrust, (c) Hind Wing Lift and (d) Hind Wing Thrust [44, 45]

The conclusions on the effect of wing spacing on the force coefficients for the oscillating in-phase tandem wings from Broering and Lian [44, 45] are highlighted below.

1. The fore wing exhibited higher lift and thrust than the single wing at all spacings.
2. The hind wing effect on the fore wing increases with the decrease in wing spacing. However, as the wing spacing decreases to less than 0.25, an additional reduction in spacing does not produce any substantial effect on the fore wing performance.
3. The effect of the fore wing on hind wing varies with the wing spacing.
4. Decreasing the spacing will increase the peak in lift and thrust forces for both wings.
5. There was a slight phase shift in both lift and thrust plots for both wings.

Through analyzing the time-averaged force coefficients shown in Figure 2-20, by decreasing the wing spacing, it increases the lift of the fore wing while reducing the lift of the hind wing. By combining the forces generated by both wings and comparing to twice the values of a single wing, the $C_{L_{max}}$ was achieved at $Sd = 2.0$ (wing spacing = 1.0) and the lift performance

deteriorated with a further decrease in wing spacings. The maximum thrust was achieved at $Sd = 1.5$ (wing spacing = 0.5) and as the wing spacing decrease further, the thrust produced dramatically decreased. The effect of the wing spacing is negligible on the resultant forces generated by the tandem wings.

Figure 2-21 : Vorticity Contours of Tandem Wing Configuration Moving In-Phase, $\phi = 0^\circ$ at the start of the downstroke. Row 1) Single Wing, Row 2) 1.0 Wing Spacing, 3) 0.5 Wing Spacing, Row 4) 0.25 Wing Spacing, 5) 0.10 Wing Spacing [44, 45]

The vorticity units for these contours in Figures 2-21, 2-23, 2-24 and 2-25 by Broering and Lian [44, 45] were not published but relative comparisons can still be made as the colored scales used were consistent throughout. Firstly, at $Sd = 2$ or wing spacing of 1.0 in Figure 2-21 Row 2, when compared to the single wing, the hind wing displayed a larger and stronger LEV formation which corresponded well with the larger lift amplitude shown in Figure 2-20 (c). In addition, at $Sd = 2$, the fore wing was able to produce a defined vortex pairing which was shed and interacted with the hind wing at $t/T = 0.25$ and 0.75 that resulted in a sharp increase in lift forces illustrated in Figure 2-20 (d).

As the wing spacing decreases, the vortex growth and propagation around the hind wing accelerated due to the closer proximity between the wings which resulted in earlier LEV shedding and more elongated vortex structures as shown in Figure 2-21. This effect started to become more apparent for the 0.5 spacing case shown in Figure 2-21 Row 3. The rapid LEV shedding exhibited by the hind wing due to the interaction with the down-washed vortex also

resulted in a phase shift in lift production as shown in Figure 2-20 (c) which was attributed to the prolonged attachment of the elongated LEV over the suction side which resulted in prolonging the lift peak. When the wing spacing is decreased from 1.0 to 0.5, the largest thrust peak was produced and based on the vorticity plots in Figure 2-21 Row 3, this thrust peak was attributed to the larger opposite signed vortex pairing seen at the hind wing's wake when compared to the vortex pairing observed in Figure 2-21 Row 2 at wing spacing of 1.0. The fore wing thrust production also increased due to the closer interaction between the fore wing's shed LEV and the opposite signed LEV forming at the top surface of the hind wing seen in Figure 2-21 Row 3.

As the wing spacing is decreased further to 0.25, the thrust produced by the hind wing decreased during both downstroke and upstroke. This was due to (1) the lack of structured vortex pairings in the trailing wake during the upstroke (2) the vortex pairing situated further from the hind wing and (3) the smaller size or lower magnitude vortex pairings during the downstroke as shown in Figure 2-21 Row 4. The lift performance for the hind wing was similar to that at wing spacing of 0.5 where it exhibited an increased in vortex elongation. On the other hand, the decrease in wing spacing contributed to a positive effect on the fore wing, increasing lift due to the better defined LEV formation and also increasing thrust due to the closer proximity of the shed LEV with the opposite signed LEV forming around of the hind wing.

At its smallest wing spacing of 0.1, one significant observation is that the thrust for the hind wing decreases showing the same trend as the 0.25 case. In addition, the C_l for the hind wing decreases as a result of strong vortex interactions between the shed LEV and the hind wing's suction side which rapidly displaces the LEV formed by the hind wing and thus weakening the lift production as shown in Figure 2-21 Row 5 at $t = 0.25$. The effect on the fore wing at wing spacing of 0.1 is similar to that at wing spacing of 0.25.

The review of this paper has provided us with important insights on the effect of vortex interactions between the fore wing and hind wing on the resultant aerodynamic force generation at $\phi = 0^\circ$ which are summarized in the points below.

1. The presence of the fore wing played an important role in accelerating vortex growth for the hind wing, thus elongating the vortex structure surrounding the hind wing resulting

in an increase in lift. The elongation of the vortex structure can lead to a delay in flow separation thus prolonging the peak in lift.

2. The hind wing thrust production was considerably affected by the interaction of the down-washed vortices shed by the fore wing as it was observed that a more defined and larger opposite signed vortex pairings at the hind wing's trailing wake tend to correlate to higher lift values.
3. Higher intensity of down-washed vortex interactions associated with small wing spacings lower than 0.25 can be detrimental to the hind wing's lift production due to the rapid displacement of the LEV and to the hind wing's thrust production due to the unstructured opposite signed vortex pairings seen at the trailing wake.
4. The hind wing effect on the fore wing increases with the decrease in wing spacing that contributed to increase in lift due to a better defined LEV formation and also an increase in thrust due to the closer proximity of the shed LEV with the opposite signed LEV forming around of the hind wing.

2.2.2 Effect of Phase Angles on Tandem Wing Aerodynamics

Through studies on the flight of dragonflies, Alexander [17] found that they can operate their wings independently enabling them to adjust the phase angles, ϕ between the fore and hind wings. He also noted that dragonflies employed phase shifting regularly for different flight phases such as in-phase flapping motion during take-off and out-of-phase flapping motion for cruising flight [17]. Works by Wang and Russell [74] and Lan and Sun [75] both demonstrated that the in-phase flapping motion produced the maximum resultant force ideal for take-off and that the out-of-phase motion at approximately $\phi=180^\circ$ produced the highest efficiency. These past studies revealed that varying ϕ can have an influence on both lift production and power [74, 75 and 76]. In terms of its aerodynamic efficiency for these tandem flapping wings, Usherwood and Lehmann [76] carried out an experimental study to investigate the hypothesis proposed by Thomas et al. [16] that interacting tandem flapping wings can produce high lift with high efficiency through utilizing the hind wing to extract energy from the wake of the fore wing. They concluded that the highest aerodynamic

efficiency was attained at phase angle, $\phi = 90^\circ$ due to the hind wing's ability to extract out energy from the fore wing's wake by removing the swirl [76].

Following their study on the effect of S_d on the tandem wing aerodynamics, Broering and Lian [44, 45] demonstrated that the vortex interactions between the tandem wings played a vital role in the force generation such as the acceleration of the LEV formation over the hind wing due to the down-washed vortex shedding by the fore wing which increased lift. As such, the variation in ϕ between the tandem wings may also have an impact on the vortex interactions and the aerodynamic performance which inspired their study on the effects of phase angles on the performance of the tandem flapping wings [45, 46].

$$\alpha(t) = \alpha_0 \cos(2\pi ft + \Phi_\alpha + \Phi_h) + \alpha_{ave} \quad (\text{Eq. 2.4})$$

$$h(t) = h_0 \cos(2\pi ft + \Phi_h) \quad (\text{Eq. 2.5})$$

Table 2-2 : Values used for the different kinematic parameters [44-46]

Parameter	Single/Fore	Hind
α_0	20°	20°
F (Hz)	0.3	0.3
ϕ_α (deg)	90	90
α_{ave} (deg)	5	5
h_0	0.5c	0.5c
ϕ_h (deg)	0°	0°, 90°, 180°
St	0.3	0.3
k	0.942	0.942

Broering and Lian [45] and Lian et al. [46] utilized the following coupled sinusoidal pitching and plunging kinematics as shown in Equations 2.4 and 2.5 respectively where α_0 denotes the pitching amplitude, t is time, f is the flapping frequency, h_0 is the plunging amplitude, ϕ_α is the phase angle for pitching motion, α_{ave} is the average α and ϕ_h is the phase angle for plunging motion. Table 2-2 tabulates the values for the various kinematic parameters in their study. The tandem wings operated under a low Reynolds number flow of 5,000 and based on a flapping frequency, f , of 0.3Hz and the plunge amplitude, h_0 , which correspond to a Strouhal number, St , of 0.3 which falls within the Strouhal range of 0.2 to 0.4 for natural fliers [25]. These tandem wings were simulated to oscillate at three ϕ with the hind wing leading; at $\phi = 0^\circ, 90^\circ$ and 180° and at four different wing spacings; at 0.1, 0.25, 0.5 and 1.0.

Figure 2-22 : Instantaneous Lift and Thrust Coefficient of the Hind Wing over One Flapping Cycle compared to a Single Wing; (a) 0° Phase Lift, (b) 0° Phase Thrust, (c) 90° Phase Lift, (d) 90° Phase Thrust, (e) 180° Phase Lift and (f) 180° Phase Thrust [45, 46]

Based on the instantaneous force coefficients results shown in Figure 2-22 for the hind wing over the single wing at various ϕ and wing spacings, here are the key points:

1. At 0° phase angle, the lift and thrust produced were significantly higher than the single wing case for all the wing spacings tested as shown in Figures 2-22 (a) and (b). It was observed that the best lift and thrust performance were attained at wing spacing of 0.5.
2. At 90° phase angle, the lift and thrust produced were higher than the single wing case at small wing spacings but not at the larger wing spacing of 1.0 especially so during the downstroke as shown in Figures 2-22 (c) and (d).
3. At 180° phase angle as shown in Figures 2-22 (e) and (f), the hind wing produced higher thrust forces during the upstroke when compared to the single wing but during the downstroke, the thrust values were lower than that of the single wing case. At the largest spacing of 1.0, the hind wing produced the lowest lift and thrust forces throughout the flapping cycle as shown in Figures 2-22 (e) and (f).
4. The increase in wing spacing or stagger distance between the tandem airfoils resulted in a phase shift in the force results when compared to the single wing case. With the decrease in wing spacing, the lift and thrust forces will increase. The effect of wing spacing on the lift and thrust forces are more apparent for the 90° and 180° cases as compared to with the in-phase motion at $\phi = 0^\circ$.

Figure 2-23: Vorticity Contours around the Single Wing and Tandem Wings at $\phi = 90^\circ$ and at Different Wing Spacings; (a) Single, $t/T = 68\%$; (b) $0.25c$, $t/T = 68\%$; (c) $0.5c$, $t/T = 68\%$; (d) $1.0c$, $t/T = 68\%$; (e) Single, $t/T = 89\%$; (f) $0.25c$, $t/T = 89\%$; (g) $0.5c$, $t/T = 89\%$; (h) $1.0c$, $t/T = 89\%$ [45, 46]

Figure 2-24 : Vorticity Contours around the Single Wing and Tandem Wings at $\phi = 180^\circ$ and at Different Wing Spacings; (a) Single, $t/T = 81\%$; (b) $0.25c$, $t/T = 81\%$; (c) $0.5c$, $t/T = 81\%$; (d) $1.0c$, $t/T = 81\%$; (e) Single, $t/T = 2\%$; (f) $0.25c$, $t/T = 2\%$; (g) $0.5c$, $t/T = 2\%$; (h) $1.0c$, $t/T = 2\%$ [45, 46]

Figure 2-25 : Vorticity Contours around the Single Wing and Tandem Wings at $\phi = 0^\circ$ and at Different Wing Spacings; (a) Single, $t/T = 4\%$; (b) $0.25c$, $t/T = 4\%$; (c) $0.5c$, $t/T = 4\%$; (d) $1.0c$, $t/T = 4\%$; (e) Single, $t/T = 25\%$; (f) $0.25c$, $t/T = 25\%$; (g) $0.5c$, $t/T = 25\%$; (h) $1.0c$, $t/T = 25\%$ [45, 46]

Figures 2-23 to 2-25 each shows two instantaneous vorticity contour snapshots of the vortex structures during one flapping cycle. The arrows indicate the stroke direction with the counter-clockwise (CCW) vortices denoted in red and the clockwise (CW) vortices denoted in blue. Similar representations of the stroke and vortex rotational directions are employed in the present study. These flow visualization results revealed several key observations that are summarized below which may explain the behaviors shown in the force data.

1. At 90° phase angle, the down-washed vortex shed by the fore wing interacted with the hind wing at $t/T = 68\%$ during the upstroke shown as in the Figure 2-23 (Top Row). As the wing spacing increases, this down-washed vortex interaction with the hind wing was delayed as it took longer for the vortex to reach the hind wing thus resulted in losing vortical energy at larger wing spacings. This was evident in the force data in Figures 2-22 (c) and (d) where there was an apparent phase lag and reduction in the lift and thrust as the wing spacing increases for the hind wing.
2. At 90° phase angle and at $t/T = 89\%$ during the upstroke prior to stroke reversal shown in Figure 2-23 (bottom row), the effect of wing spacings can be clearly observed as it affected the timing of the interaction of the vortex shed by the fore wing. The timing of the down-washed vortex interaction had a significant impact on the LEV formation around the hind wing. The down-washed vortex shed by the fore wing had the same CCW rotation direction as the LEV formed over the hind wing and this ‘like-signed’ vortex interaction is constructive as it reinforces the LEV generation. Hence, as the wing spacing decreases, the vortex interaction was stronger as the down-washed vortex shed by the fore wing had lesser time to dissipate which resulted in a larger LEV formation over the hind wing and this corresponded well with the higher lift and thrust results in Figures 2-22 (c) and (d).
3. At 180° phase angle, the vorticity contours show similar trend as the 90° case with the exception that the down-washed vortex shed by the fore wing interacted with the hind wing later in the upstroke at $t/T = 81\%$ compared to $t/T = 68\%$ in the 90° case as shown in Figure 2-24 (top row). The increase in wing spacing resulted in a more delayed vortex interaction which corresponded to a phase lag and reduction in the lift and thrust forces which was similar to the 90° case shown in Figures 2-22 (e) and (f).
4. At 180° phase angle and at the largest wing spacing of 1.0, the vortex interaction was delayed to an extent that it led to the CW LEV formation on the suction surface of the hind wing after stroke reversal at $t/T = 2\%$ as shown in Figure 2-24 (bottom row). The CCW down-washed vortex that interacted with CW LEV dampened the LEV and accelerated its shedding, resulting in low lift and thrust as exhibited in Figures 2-22 (e) and (f). With an extensive delayed vortex interaction, the lift and thrust results at wing spacing of 1.0 displayed a different pattern from the other wing spacings cases.

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5. At 0° phase angle, at the small wing spacings of < 0.25 , the closer proximity of the tandem wings produced a jet which accelerated flow velocity and LEV formation by the hind wing as shown in Figure 2-25 (bottom row). Hence, this led to smaller and more elongated LEV formation by the hind wing which corresponded to lower lift and thrust values at small wing spacings < 0.25 as shown in Figures 2-22 (a) and (b). This differed from the 90° and 180° cases.

From these computational studies by Broering and Lian [44, 45] and Lian et al. [46], it can be concluded that by varying both wing spacings and phase angles, the timing of the vortex interactions can be altered that will affect the resultant aerodynamic force production. These vortex interactions can also affect the strength and structure of the LEV formation around the hind wing. In addition, they provided a preliminary insight into the effects of ‘like-signed’ and ‘opposite-signed’ vortex interactions on the performance of tandem wings [44-46].

2.2.3 Static Tandem Wings Aerodynamics Review

The experimental study on static tandem wings by Scharpf and Mueller [43] will be reviewed in this section to provide the foundation for the present study on static tandem wings. Scharpf and Mueller [43] investigated the flow across two identical tandem airfoils at $Re_c = 8.5 \times 10^4$. Most of the experimental and computational works on tandem airfoils were conducted at high Reynolds number flows at $Re_c > 10^6$ where transient events such as transitional laminar separation bubble cannot be investigated [43]. Their main objective is to determine any benefits in terms of optimizing the (C_L/C_D) ratio by using tandem airfoils instead of a single airfoil configuration.

2.2.3.1 Experimental Methods [43]

Flow visualizations and force measurements were conducted in the sub-sonic, open loop wind tunnel which was equipped with an atmospheric exhaust. It had a square inlet measuring 3.04m by 3.04m in cross section and a testing section with a cross section area measuring 0.61m by 0.61m which gave an area contraction ratio of 24:1. The turbulence intensity, T_i for the tunnel was $< 0.08\%$ and the free stream tunnel wind velocity was set at 9m/s. Two identical Wortmann FX63-137 airfoils measuring 6in. in chord, c and 16in. in span width, s , were used for the experiments. Both airfoils were mounted between a set of end plates at a gap length of 0.02in. so as to reduce the wingtip effects and span wise

variations in aerodynamic loadings. Hence, a 2-D flow can be approximated with the downstream airfoil only affected by the wake of the upstream airfoil. For the tandem configuration, $S_d = 1.5$ and $G = 0$ at $Re_c = 8.5 \times 10^4$ were selected.

The force measurements were taken using a 3-component force balance which measured lift, drag and $\frac{1}{4}$ chord pitching moment; C_L , C_D and C_M respectively. The vertically mounted airfoils were pitched from $\alpha = -15^\circ$ to 20° and back to its initial position to record any aerodynamic hysteresis effects. Maximum error for $C_{L_{max}} = 2.6\%$ and $C_{D_{min}} = 5.8\%$. For the surface pressure distributions, 40 and 15 pressure taps were mounted on the upper and lower surface of the airfoil respectively. Some of the taps were concentrated at the leading edge and mounted in a staggered fashion in order to avoid any upstream induced disturbance on the downstream tap. A maximum error of 4.17% was found at $C_{p_{max}}$. Kerosene smoke was used for the flow visualizations which complemented the quantitative data.

2.2.3.3 Flow Visualization Results [43]

Figure 2-26 : Hysteresis Region for Static Tandem Airfoils [43]

In this section, the effects of Re and α on the hysteresis region will be presented for the tandem airfoil configuration. To explain the hysteresis region illustrated in Figure 2-26, let's take two identical airfoils in tandem and subject both airfoils to the same pitch angle, $\alpha_{ua} = 5^\circ$ and $\alpha_{da} = 5^\circ$ in a free stream flow velocity. In the first scenario, the flow velocity starts accelerating from a low Reynolds number in Point (1) where the flow over the tandem airfoils is initially separated, thus this denotes the 'separated flow' region. As the flow starts accelerating, the flow over the tandem airfoils begins to reattach at a larger Reynolds number in Point (2) which denote the 'reattachment point' of an 'accelerating separated flow'.

The second scenario is when the flow over the tandem airfoils at $\alpha_{ua} = 5^\circ$ and $\alpha_{da} = 5^\circ$ starts at a larger Reynolds number (higher flow velocity) in Point (3) and the flow is attached at this initial condition, thus representing the ‘attached flow’ region. This ‘attached’ flow is then being decelerated up to Point (4) where the flow could no longer stay attached due to the adverse pressure gradients and begins to separate, hence this Point (4) denotes the ‘flow separation point’ of a ‘decelerating attached flow’. Hence, due to the different points when these flow ‘separation’ and ‘reattachment’ points occurred with the two initial flow conditions, there is a hysteresis region as shown in Figure 2-26. The hysteresis region for the tandem airfoils with an upstream airfoil pitched at $\alpha_{ua} = 5^\circ$ and 10° is shown in Figure 2-26.

Figure 2-27 : Tandem Airfoils at $Re_c = 8.5 \times 10^4$, showing the Separation Hysteresis Phenomenon for $\alpha_{ua} = \alpha_{da} = 10^\circ$; (a) Decelerating Attached-Flow Condition; (b) Accelerating Separated-Flow Condition [43]

With introduction of the second airfoil, the hysteresis region for these flows increased significantly. The hysteresis effect of these flows can be explained by the difference in energy and also the increase in stability inherent with the decelerating attached flow hence, the flow remained attached at an even lower Reynolds number than that of the attachment of the accelerating separated flow as demonstrated in Figure 2-27 at $\alpha_{ua} = \alpha_{da} = 10^\circ$. The hysteresis effect may be due to the “apparent shape” of the airfoil when the flow was separated. Critically, the increase in hysteresis region was mainly due to the variation in stream wise pressure difference between the leading and trailing edge of the upstream airfoil. Based on Figure 2-26, the increase of angle of attack from $\alpha_{ua} = 5^\circ$ to 10° resulted in the increase of the hysteresis region as well. This increment was due to the increase in adverse pressure gradients as α was increased, thus requiring more energy for flow attachment. This also contributed to an increase in stability for the attached flow resulting in a lower Reynolds number for separation to take place. Due to the importance of the nature of the starting flow condition, for the subsequent results presented here, the attached-flow condition was chosen.

2.2.3.4 Force Measurement Results [43]

Figure 2-28 : Lift Coefficient, C_L for (a) Downstream Airfoil and (b) Upstream Airfoil (right) at α_{ua} and $\alpha_{da} = 0^\circ$ and 10° respectively with $Sd = 1.5$ and $G = 0$ [43]

This section will present the various force coefficients such as C_L and C_D for one of the airfoils with respect to the second airfoil which was held at a fixed angle of attack of 0° and 10° . These force results were compared to the single airfoil case under the same conditions as displayed in Figure 2-28.

With the presence of the upstream airfoil, the C_L values were lower for the downstream airfoil across the range of αda in relation to the single airfoil as shown in Figure 2-28 (a). In comparison to the single airfoil case, stall occurred at $\alpha da = 16^\circ$, whereas for the tandem configuration, the flow across the downstream airfoil remained attached for all α tested. This delay in the onset of stall was attributed to the reduction in the effective angle of attack of the downstream airfoil due to the downwash produced by the upstream airfoil. Based on the flow visualizations, it was deduced that the downwash propagated below the downstream airfoil and created a low-pressure region that led to a loss in lift. In contrast, the upstream airfoil exhibited consistently higher C_L values in comparison to the single airfoil case due to several factors that will be elaborated in the pressure distribution analysis.

Figure 2-29 : Drag Coefficient, C_D for (a) Downstream Airfoil and (b) Upstream Airfoil at α_{ua} and $\alpha_{da} = 0^\circ$ and 10° respectively ($Sd = 1.5$ and $G = 0$) [43]

Based on Figure 2-29 (a), the introduction of the upstream airfoil resulted in the overall decrease in C_D for most α_{da} cases for the downstream airfoil. At $\alpha_{da} > 15^\circ$, the downstream airfoil produced lower C_D of up to 27% when compared to the single airfoil as shown in Figure 2-29 (a). When the downstream airfoil is at $\alpha_{da} = 5^\circ$ and the upstream airfoil is at $\alpha_{ua} = 10^\circ$, the largest C_D reduction of 48% was achieved when compared to the single airfoil case as circled in red in Figure 2-29 (a). The upstream airfoil exhibited an even larger reduction in C_D when comparing to the downstream airfoil as shown in Figure 2-29 (b) when $\alpha_{da} = 10^\circ$ which is circled in blue. This drag reduction was due to the increase in static pressure between the airfoils which led to the decrease in stream-wise pressure difference between the leading edge and trailing edge of the upstream airfoil [43].

2.2.3.5 Pressure Measurements Results [43]

Figure 2-30 : Pressure Distribution for (a) Downstream Airfoil at $\alpha_{da} = 10^\circ$ with $\alpha_{ua} = 0^\circ$ and 10° and for (b) Upstream Airfoil at $\alpha_{ua} = 10^\circ$ with $\alpha_{da} = 0^\circ$ and 10° ($S_d = 1.5$ and $G = 0$) [43]

From the pressure distributions plots for both upper and lower airfoil surfaces shown in Figure 2-30, the presence and location of the separation bubble were subjectively established. The start of the bubble was typically inferred as the beginning of the plateau region due to the fluid recirculation within the bubble and the end of the bubble is where the pressure distribution resumed to normal.

Based on Figure 2-30 (a), the decrease in C_L for the downstream airfoil was attributed to the significant overall reduction in pressure difference, Δp between the upper and lower airfoil surfaces. The separation bubble decreased in length with the increase in α_{ua} which indicated an increase in turbulent boundary layer, hence contributed to the increase in skin friction drag thus validating the slight increase in drag force for $\alpha_{da} = 10^\circ$ and $\alpha_{ua} = 0^\circ$. On the other hand, Figure 2-30 (b) shows that at $\alpha_{da} = 0^\circ$, there was a slight increase in pressure on the upper surface with minimal variations on the lower surface of the upstream airfoil in relation to the single airfoil. At $\alpha_{da} = 10^\circ$, the increase in static pressure and the consequential decrease of the separation bubble length at the upper airfoil surface are the two possible factors responsible for the increase in lift exhibited in Figure 2-30 (b).

2.2.3.6 Summary of Force Coefficient Measurements [43]

Table 2-3 : Force Coefficients and C_L/C_D Ratio for Tandem Airfoils and Single Airfoil Cases ($Sd = 1.5$, $G = 0$, $Re_c = 8.5 \times 10^4$) [43]

Incidence			C_L		C_D		C_L/C_D	
δ , decalage = $\alpha_{ua} - \alpha_{da}$ (deg)	α_{ua} (deg)	$\alpha_{da} = \alpha_s$ (deg)	single	total	single	total	single	total
-10	0	10	1.383	1.502	0.082	0.13	16.776	11.554
-5	0	5	0.686	1.077	0.065	0.095	10.507	11.337
-5	5	10	1.383	2.206	0.082	0.137	16.776	16.102
0	0	0	0.359	0.7	0.032	0.07	11.088	10
0	5	5	0.686	1.729	0.065	0.106	10.507	16.311
0	10	10	1.383	2.223	0.082	0.123	16.776	18.073
5	5	0	0.359	1.199	0.032	0.089	11.088	13.472
5	10	5	0.686	1.975	0.065	0.106	10.507	18.632
10	10	0	0.359	1.515	0.032	0.108	11.088	14.028

Table 2-3 shows the force coefficients and the C_L/C_D ratios of the tandem airfoils in comparison to the single airfoil. It can be concluded that for the tandem airfoils, despite the same relative angle of attack or decalage, δ , the force coefficients vary thus indicating the importance of the angle of incidence of the individual airfoils on aerodynamic performance. The total C_L and C_D were the summation of the individual force coefficients of the upstream and downstream airfoils. These total force coefficients for the tandem airfoils were compared with the single airfoil case so as to evaluate the benefits by employing the tandem wing configuration. Based on the measurements in Table 2-3, the tandem wing configurations demonstrated the highest improvement in C_L/C_D ratio of 77% when the downstream airfoil was at $\alpha_{da} = 5^\circ$ and the upstream airfoil at $\alpha_{ua} = 10^\circ$. Conclusively, for all positive values of δ ,

delage = $\alpha_{ua} - \alpha_{da}$, the tandem wing configuration consistently exhibited an improvement in the C_L/C_D ratio as it showcased the benefits of tandem airfoils over single airfoil under the same dynamic pressure and free stream velocity. Moreover, this study has provided a good basis for validation especially for static tandem airfoils force measurements.

2.2.4 Oscillating Tandem Wings Aerodynamics Review

As presented earlier on single wing aerodynamics, it is understood that the aerodynamics and the flow mechanisms in play between static and dynamic cases differ considerably. Scharpf and Mueller [43] have demonstrated the huge potential of utilizing static tandem airfoils such as optimization of the C_L/C_D ratio. Broering and Lian [44, 45] and Lian et al. [46] documented the influence of S_d and ϕ on the wing-wing and wing-wake vortex interactions on the aerodynamic performance of tandem wings in coupled heaving and pitching motion. In this section, the study on oscillating tandem NACA0012 airfoils by Lee [18] will be reviewed which is instrumental for the validation of the present study.

2.2.4.1 Experimental Methods

Figure 2-31 : Schematics of (a) Experimental Setup; (b) NACA0012 Airfoil Motion Profile ($\phi = 0^\circ$ and 180°) and (c) Two-Airfoil Tandem Arrangement [18]

Figure 2-31 (a) shows the experimental arrangements for Lee's study [18] which incorporated two identical NACA0012 airfoils that were controlled by four-bar linkages and flywheel mechanism that facilitated the oscillations of the airfoils about $1/4c$ at various α and f . The PIV measurements were carried out in a suction-type subsonic wind tunnel at a fixed Reynolds

number, Re_c of 8.5×10^4 and airfoils with a chord, c of 0.075m and span width, s of 0.19m. Both airfoils were oscillated with $\alpha(t) = 8^\circ + 5^\circ \sin \omega t$ at a $\kappa = 0.05$, three wing spacings of $L_{space} = 0.3c, 0.6c$ and $1.0c$ and at two phase angles, $\phi = 0^\circ$ and 180° as depicted in Figure 2-31 (b). Wing spacing, L_{space} is defined as the horizontal distance between the TE of the upstream airfoil and the LE of the downstream airfoil as shown in Figure 2-31 (c). L_{space} is represented in terms of its chord, c , for e.g. $0.3c$ or $0.6c$. For the surface pressure measurements, two NC-machined NACA0012 airfoils with chord, $c = 0.15m$ and span width, $s = 0.375m$ were used in a $0.9 \times 1.2 \times 2.7m^3$ wind tunnel. These airfoils were mounted to circular end disks measuring 0.30m in diameter to mitigate the free ends effects and to assume a 2-D flow condition. Only the downstream airfoil was fitted with 61 pressure taps that were distributed over the upper and lower airfoil surface in a staggered manner to avoid any upstream wake interaction on the downstream pressure tap. The recorded pressure signals were phase-locked ensemble-averaged over 100 oscillation cycles and were subsequently numerically integrated to obtain lift, C_L and pitching-moment, C_M coefficients. Three airfoil spacings at $L_{space} = 0.3c, 0.6c$ and $1.6c$ were tested for the pressure measurements.

2.2.4.2 Results and Discussion [18]

2.2.4.2.1 Baseline Oscillating Airfoil

Figure 2-32 : Normalized Instantaneous Iso-Velocity Contours for Baseline Oscillating Airfoil [18]

Based on the static case presented earlier in Section 2.1.1.1, the static stall occurred at $\alpha_{ss} = 11^\circ$. Firstly, the boundary layer across the airfoil remained attached up to $\alpha_u = 9^\circ$ and for $9^\circ < \alpha_u < 11^\circ$, the turbulent boundary layer at the trailing edge became progressively thicker due to

the onset of flow reversal as shown in Figures 2-32 (a) and (b). As α increases, the thickened low-velocity layer (flow reversal) moves upstream but the flow generally remained attached. The thin layer of flow reversal in this case was not detected due to the lack of seeding particles; however, its presence can be deduced from the drastic thickening of the turbulent boundary layer.

At $\alpha_u = 11.5^\circ$, the upstream movement of the flow reversal disrupted the laminar separation bubble and initiated the formation of the LEV with an initial length of $0.3c$ as shown in Figure 2-32 (c). At $\alpha_u = 12^\circ$ as indicated in Figure 2-32 (d), the LEV have propagated downstream which gave rise to the increase in C_L and a significant decrease in C_M which is typical of a single oscillating airfoil as presented earlier in Section 2.1.1.2.

At near dynamic stall condition, $\alpha_u < \alpha_{ds} = 12.5^\circ$, flow separation occurs as the flow could not overcome the adverse pressure gradients. As the airfoil pitched to its dynamic-stall angle, $\alpha_{ds} = 12.5^\circ$, the LEV was “spilled” over the trailing edge to the wake as seen in Figure 2-32 (e). For $\alpha_u = \alpha_{max} = 13^\circ$, in post-stall conditions, the upper surface boundary layer was completely separated leading to a significant drop in lift performance as shown in Figure 2-32 (f). As the airfoil began to pitch downwards, the onset of flow reattachment began at $\alpha_d \approx 9.5^\circ$ seen in Figure 2-32 (h) and at $\alpha_d < 8^\circ$, pre-stall conditions were soon re-established as shown in Figure 2-32 (i).

Figure 2-33 : Dynamic-Load Loops for the Downstream Airfoil for $\phi = 180^\circ$ at Various Wing Spacings; (a) C_L ; (b) C_M and (c) C_D [18]

The critical flow points during the oscillation are labeled in the dynamic-load loops shown in Figure 2-33. For the oscillating baseline airfoil, the dynamic stall was delayed to $\alpha_d = +14.6^\circ$

with $C_{Lmax} = 1.39$ as compared to the static airfoil where it stalled at $\alpha_{ss} = +12.5^\circ$ with $C_{Lmax} = 1.08$. Conclusively, the baseline oscillating airfoil results showed large improvements in the lift performance due to the formation and convection of LEV, however, the ideal case would be to delay dynamic stall thus optimizing the increase in C_L .

2.2.4.2.2 Two-Airfoil Configuration with $\phi = 180^\circ$

Figure 2-34 : Impact of $\phi = 0^\circ$ and 180° on C_p Distribution on the Downstream Airfoil at Selected α for $L_{space} = 0.3c$ [18]

For $\phi = 180^\circ$, the angle of incidence of the two tandem airfoils were always opposite to each other and the aerodynamic performance were greatly influenced by the interaction of the two airfoils. This set of surface pressure coefficient, C_p , distribution results of the downstream airfoil at $L_{space} = 0.3c$ or $S_d = 1.3$ shown in Figure 2-34 are correlated to the corresponding dynamic load loops plots that are highlighted as the red line in Figure 2-34. At $\alpha_u < \alpha_{fr}$ (onset of flow reversal), the large separated wake generated by the upstream airfoil was found to

reduce C_L as shown in Figure 2-34 (a) and rendered a flat C_p distribution at the upper surface of the downstream airfoil as shown in Figure 2-34 (a).

As the airfoil pitched upwards, for $\alpha_{fr} < \alpha_u \leq \alpha_{max}$ shown from Figure 2-34 (b) to (d), there was a continuous rise in the leading-edge suction peak due to the decreasing upstream interference effect and also the impingement of the upstream wake on the lower surface of the downstream airfoil which created a lowered pressure region. One significant finding was that due to the upstream wake interaction, its downwash disrupted the formation of LEV on the downstream airfoil eliminating dynamic stall thus drastically reducing the C_L - hysteresis as shown in Figure 2-34 (a). The C_L -hysteresis or $C_h = \int C_l d\alpha$ was computed to be 0.16 which is a 94% reduction as compared to the $C_h = 2.73$ for the baseline airfoil. The large hysteresis is normally responsible for the reduction of aerodynamic damping which can lead to a range of aero-elastic problems on the rotors. The absence of LEV also inevitably, decreases C_{Lmax} to 1.28 as shown in Figure 2-33 (a) and can be viewed as a reduction in effective incidence [18].

Throughout the pitch-down stroke, the pressure coefficients were unaffected by the downwash effect on the downstream airfoil, thus displaying the dominant suction peak that was attributed to the absence of LEV and flow separation throughout the entire cycle as shown in Figure 2-34 (e). This also gave rise to an improved C_L during pitch-down as compared to the baseline airfoil as shown in Figure 2-33 (a).

2.2.4.2.3 Two-Airfoil Configuration with $\phi = 0^\circ$

Figure 2-35: Dynamic-Load Loops for Downstream Airfoil at $\phi = 0^\circ$ and at Various Wing Spacings; (a) C_L ; (b) C_M and (c) C_D [18]

With $\phi = 0^\circ$ and $L_{\text{space}} = 0.3c$, the upstream airfoil generated turbulent wake interaction have detrimental effects on the aerodynamic performance of the downstream airfoil as exhibited in its dynamic-load loop plots shown in Figure 2-35 as the pressures on both upper and lower surfaces of the downstream airfoil were considerably reduced throughout the whole oscillation cycle as exhibited in Figure 2-34. The downstream airfoil also underwent the same critical flow points as the baseline oscillating airfoil in Section 2.1.1.2 such as flow reversal, turbulent breakdown, LEV formation and convection, dynamic stall, reattachment and relaminarization.

Figure 2-36 : Impact of ϕ on iso- $\zeta c/U_\infty$ Contours for the Downstream Airfoil at $L_{\text{space}} = 0.3c$ and at $\alpha_u = 13$; (a) $\phi = 180^\circ$ and (b) $\phi = 0^\circ$ [18]

At $\phi = 0^\circ$, the vorticity contours in Figure 2-36 (b) showed that the downstream airfoil was engulfed by the down-washed vortices which resulted in a significant decrease in C_L values when compared to both the baseline and the $\phi = 180^\circ$ cases with a $C_{L\text{max}}$ of only 0.58. The C_M values at $\phi = 0^\circ$ were also consistently lower than the baseline airfoil values.

2.2.4.3 Experimental Results Summary

Table 2-4 : Impact of ϕ and L_{space} on $C_{L\text{max}}$, α_{ds} , $C_{M, \text{min}}$, C_H , $C_{w, \text{cw}}$, $C_{w, \text{ccw}}$ and $C_{w, \text{net}}$ [18]

Configuration	ϕ ($^\circ$)	L	$C_{L\text{max}}$	α_{ds} ($^\circ$)	$C_{M, \text{min}}$	C_H	$C_{w, \text{cw}}$	$C_{w, \text{ccw}}$	$C_{w, \text{net}}$
SA	–	–	1.08	12.5	–0.0580	–	–	–	–
BA	–	–	1.39	14.6	–0.0256	2.73	–0.061	0.025	–0.036
TA	180	$0.3c$	1.28	15	–0.0096	0.16	–0.034	0.051	0.017
TA	180	$0.6c$	1.13	15	–0.0028	0.39	–0.015	0.002	0.013
TA	180	$1.6c$	1.05	15	–0.0011	0.42	–0.013	0.001	0.010
TA	0	$0.3c$	0.58	13.9	–0.0116	0.78	–0.032	0.068	0.034
TA	0	$0.6c$	0.79	14.2	–0.0062	1.00	–0.014	0.039	0.022
TA	0	$1.6c$	0.94	14	–0.0051	1.08	–0.011	0.030	0.019

SA, BA, and TA denote static airfoil, baseline airfoil, and two airfoils, respectively

Based on Table 2-4, the effects of the three various wing spacings, at $L_{\text{space}} = 0.3c$, $0.6c$ and $1.6c$ were analyzed. The wing spacings and the phase differences played vital roles in determining the nature of the flow interaction including the strength of detrimental interaction effects on the downstream airfoil. For both phase differences of 0° and 180° , the LEV-induced transient effects such as effects of stall were evident hence, reducing the C_h or C_L -hysteresis. However, for $L_{\text{space}} \geq 0.6c$, the C_h values were independent of the variations in L_{space} . C_L and C_M values for all the tandem airfoil configurations were consistently lower than that of the baseline oscillating airfoil. C_D was found to generally increase and decrease during pitch-up and pitch-down respectively, with $\phi = 0^\circ$ case displaying higher C_D values than phase difference at $\phi = 180^\circ$. For $\phi = 0^\circ$, the C_L and C_M improved with the increase in airfoil spacing indicating the significant detrimental effects of interference and as L_{space} increases, these effects diminish along with the wake deficit. In contrast, for $\phi = 180^\circ$, the force coefficients were found to improve when L_{space} decreases.

Conclusively, this study has demonstrated that tandem airfoils oscillating at $\phi = 180^\circ$ have aerodynamically outperform the $\phi = 0^\circ$ case. It is important to note the benefits of out-of-phase tandem oscillating airfoils especially so in its ability to retain its leading-edge suction peak, to delay the onset of stall and in reducing the C_L -hysteresis that increases aerodynamic damping. However, there are several gaps in this study such as the results were only limited to small pitch amplitudes, $\alpha^\circ \leq 13^\circ$ and with a low $\kappa = 0.05$. Moreover, with a given airfoil profile such as NACA4421 with a static stall angle of $\alpha_{ss} \approx 15^\circ$, it is essential that oscillations at post static stall conditions needs to be investigated. Lee [18] also did not give an in-depth evaluation on the vortex shedding characteristics and the effects of various tandem configurations on the upstream airfoil's aerodynamic performance.

3. THEORY

3.1 KINEMATICS OF FLUID MOTION

3.1.1 Velocity Field

To understand the governing continuum hypothesis surrounding the fluid flow, molecular structure of a fluid should be first studied. First and foremost, fluids are defined as a substance that continually deform upon any applied shear stress. Hence, at any point in time, there is a net motion of molecules from one point in space to another point. The fact is that if you were to zoom into a tiny portion of a fluid, it will consist of so many molecules that it becomes impractical to explain and track individual molecule behavior and motion. Based on these constraints, fluid flow can be more effectively described and visualized as a motion of fluid particles instead of its individual molecules where fluid motion can be represented in terms of velocity and acceleration of these fluid particles. Therefore, the above mentioned governing continuum hypothesis implies that fluid flow consist of fluid particles that interact with one another and with their surroundings [77].

$$\vec{V} = u(x, y, z, t)\hat{i} + v(x, y, z, t)\hat{j} + w(x, y, z, t)\hat{k} \quad (\text{Eq. 3.1})$$

In order to describe a typical fluid property such as velocity, it can be represented in its spatial coordinates (x , y and z directions) which is commonly termed as a field representation of the flow. However, for this specific flow field representation, it is important to note that the velocity at the specified point in the flow field is time-dependent as it may be different at different times. Hence, to comprehensively represent a velocity field, it has to be a function of its spatial coordinates and time as shown in the Equation 3.1 where u , v and w representing the three i , j and k components of a velocity vector respectively.

$$|\vec{V}| = (u^2 + v^2 + w^2)^{\frac{1}{2}} \quad (\text{Eq. 3.2})$$

The velocity vector, $\vec{V}$ for a particle is defined as the rate of change in position of a fluid particle. If the derivative of the above velocity vector in Equation 3.1 is being taken, the individual position vector for a specific fluid particle can be obtained. In addition, if the velocity vectors are mapped out for all the particles in the flow field, the field velocity vector can be represented as $\vec{V} = \vec{V}(x, y, z, t)$. As it is known, a single vector would consist of both magnitude and direction; the magnitude of the velocity vector can be calculated by taking the square root of its ‘squared’ velocity components as shown in Equation 3.2.

3.1.2 Vorticity

$$|\vec{\omega}| = 2 \Omega \quad (\text{Eq. 3.3})$$

It is important to understand the concept of vorticity as the flow structures surrounding the oscillating airfoils are represented by its vorticities in the phase-average vorticity contours. Following the governing continuum hypothesis explained above, in continuum fluid mechanics, vorticity is the pseudovector that illustrates the local rotation at an arbitrary point in the fluid as observed by an onlooker that moves along the fluid flow. To explain this point, imagine throwing a tiny sphere into a fluid that is free to move and we proceed to observe how this sphere rotates about its center. By definition, vorticity vector, $\vec{\omega}$ describes the direction of rotation of this sphere (using the Right-Hand Rule) as it moves along the fluid streamline and its vector length is twice the sphere's angular velocity, Ω as shown in Equation 3.3.

$$\vec{\omega} = \nabla \times \vec{V} \quad (\text{Eq. 3.4})$$

Mathematically, vorticity vector, $\vec{\omega}$ is expressed as the curl of the velocity vector where ∇ is the Del operator and $\vec{V}$ is the velocity vector as shown in Equation 3.4.

$$\nabla = \left(\frac{\partial}{\partial x}, \frac{\partial}{\partial y}, \frac{\partial}{\partial z} \right) \quad (\text{Eq. 3.5})$$

Where ∇ , del operator is represented by the Nabla symbol in vector calculus, acts as the vector differential operator and can be expressed in its Cartesian form as displayed in Equation 3.5.

$$\vec{\omega} = \left(\frac{\partial}{\partial x}, \frac{\partial}{\partial y}, \frac{\partial}{\partial z} \right) \times (u, v, w) \quad (\text{Eq. 3.6})$$

$$\vec{\omega} = \left(\frac{\partial w}{\partial y} - \frac{\partial v}{\partial z} \right) \hat{i} + \left(\frac{\partial u}{\partial z} - \frac{\partial w}{\partial x} \right) \hat{j} + \left(\frac{\partial v}{\partial x} - \frac{\partial u}{\partial y} \right) \hat{k} \quad (\text{Eq. 3.7})$$

Substituting, Equation 3.5 into Equation 3.4, the vorticity vector, $\vec{\omega}$ can be expressed as shown in Equations 3.6 and 3.7.

$$\vec{\omega} = \left(\frac{\partial v}{\partial x} - \frac{\partial u}{\partial y} \right) \hat{k} \quad (\text{Eq. 3.8})$$

In a 2-dimensional flow field, where $\vec{\omega} = 0$, the vorticity vector, $\vec{\omega}$ can be simplified and expressed as Equation 3.8.

Figure 3-1 : (a) 2-Dimensional Flow Condition demonstrating Vorticity Equation; (b) Fleming's Right Hand Rule and (c) Right Hand Grip Rule

The above simplified Equation 3.8 has shown that with flow velocity components; u and v in the x and y directions respectively, the resultant vorticity vector, $\vec{\omega}$ will only act perpendicular to the flow plane as demonstrated in Figure 3-1 (a). Using the Fleming's Right Hand Rule together with the direction of U and V , the direction of the vorticity vector, $\vec{\omega}$ can be determined as shown in Figure 3-1 (b). Subsequently, applying the Right Hand Grip rule on a rotating body, the rotational direction of the vorticity can be resolved. In the Figure 3-1 (c), with the thumb pointing upwards or out of the flow plane and gripping the imaginary vorticity vector axis, the fingers are curled in the counter-clockwise (CCW) direction thus indicating that the vorticity is rotating in the counter-clockwise direction.

$$\Gamma = \oint_C \vec{V} \cdot d\vec{l} = \iint_s \zeta \cdot d\vec{s} \quad (\text{Eq. 3.9})$$

Firstly, circulation, Γ , is defined as the line integral of the tangential component of the velocity, $\vec{V}$ taken around a closed curve, C where $d\vec{l}$ denotes the differential length along the curve as shown in Equation 3.9. Through the application of Stroke's theorem, the circulation, Γ can also be expressed as a function of vorticity, ω where the circulation can be obtained by taking the surface integral of vorticity, ζ over a closed surface, s .

$$\zeta = \frac{\Gamma}{A} \quad (\text{Eq. 3.10})$$

As the Stroke's theorem states that the integral of the differential velocity field, $\vec{V}$ over a boundary of a closed curve, C is equals to the surface integral of the vorticity over an infinitesimal loop area. Hence, vorticity, ζ can be expressed as the circulation, Γ over per unit area, A as expressed in Equation 3.10.

Figure 3-2 : Continuum Hypothesis applied to provide an Instantaneous Velocity and Vorticity Fields through 2-D PIV Measurements

Based on the theoretical concepts of both the velocity field where the continuum hypothesis is applied and also the vorticity in a 2-D flow field, the 2-D Particle Image Velocimetry (PIV) measurements can be better grasped and explained. PIV measurements are governed by the working principle of continuum hypothesis where the fluid flow structure is represented by the motion of fluid particles and the instantaneous velocity flow field is characterized as a function of its spatial coordinates (x , y and z) and also time, t as illustrated in Figure 3-2.

In these PIV measurements, the working fluid is seeded with micron-sized polymer particles. These particles are sufficiently small to follow the fluid flow yet big enough to reflect enough illumination to be captured by a CCD camera. The illumination source is provided by projecting a laser sheet through the cross section of the flow. Two images of the illuminated

flow cross section are captured at T and $T+\Delta t$ where T is the instantaneous time of measurement and Δt is the pulse time interval as shown in Figure 3-2 (a). These two images are then computationally superimposed via pixel-by-pixel interrogation to map out the displacements of the particles in the captured flow field. These displacements are processed together with the known pulse time interval, Δt and the size of the 2-D flow field to compute two velocity components, U and V thus enabling the instantaneous velocity vector plot of the flow field to be mapped out as shown in Figure 3-2 (b).

Figure 3-3 : The Concept of Linear Interpolation in Central Differencing Scheme

Subsequently, these two velocity components, U and V are used to compute the vorticities of the flow field to produce the vorticity contours as shown in Figure 3-2 (c). These vorticity contours are computed using MatLab via the application of the central differencing scheme [78]. Firstly, the central differencing scheme is one of three schemes under the finite difference method which is a generalized mathematical method in optimization of approximating differential operator in a central node in a given computational grid. The central differencing scheme is commonly used as a numerical solver for differential equations such as the Navier-Stokes equation and provides solutions for transport properties such as flow velocity, U [78]. The principle behind the central differencing scheme is based on the linear interpolation of the east and west cell values of a particular cell as depicted in Figure 3-3. In order to compute the velocity, U_i at cell position, $x = i$, the U_{i-1} (West) and U_{i+1} (East) cell values must be known. Hence, through linear interpolation over the known distance in the X direction, the cell value at $x = i$ is simply the summation of U_{i-1} and U_{i+1} divided over the distance of 2.

$$U_i = \frac{U_{i-1} + U_{i+1}}{2} \quad (\text{Eq. 3.11})$$

$$V_j = \frac{V_{j-1} + V_{j+1}}{2} \quad (\text{Eq. 3.12})$$

The flow velocities, U and V in the X and Y directions can be expressed as Equations 3.11 and 3.12 respectively through the application of the central differencing scheme where ‘ i ’ denotes the cell position in the X direction and ‘ j ’ denotes the cell position in the Y direction.

$$\zeta = \frac{\Gamma}{A} = \frac{\oint_C \vec{V} \cdot d\vec{l}}{A} \quad (\text{Eq. 3.13})$$

The concept of linear interpolation to calculate the cell values of the velocities for a 2-D flow field using the central differencing scheme is then applied to calculate the vorticities surrounding the closed boundary of the captured flow field. Using Equation 3.10, the vorticity, ζ , can also be expressed as Equation 3.13.

Figure 3-4 : Closed Boundary Conditions of 2-D Cell in a Computational Flow Field

For an infinitesimal closed surface, for example a closed cell measuring 2mm by 2mm in length as displayed in Figure 3-4. The vorticity surrounding this closed cell is defined as the total circulation over the entire length of the closed cell over the whole area of the cell.

$$\Gamma_{\text{total}} = \oint_C \vec{V} \cdot d\vec{l} \quad (\text{Eq. 3.14})$$

$$\Gamma_{\text{total}} = \Gamma_A + \Gamma_B + \Gamma_C + \Gamma_D \quad (\text{Eq. 3.15})$$

Applying the central differencing scheme,

$$\Gamma_A = \left[\frac{U_{i+1,j-1} + U_{i-1,j-1}}{2} \right] \times [X_{i+1,j-1} - X_{i-1,j-1}] \quad (\text{Eq. 3.16})$$

$$\Gamma_B = \left[\frac{V_{i+1,j+1} + V_{i+1,j-1}}{2} \right] \times [Y_{i+1,j+1} - Y_{i+1,j-1}] \quad (\text{Eq. 3.17})$$

$$\Gamma_C = \left[\frac{U_{i+1,j+1} + U_{i-1,j+1}}{2} \right] \times [X_{i+1,j+1} - X_{i-1,j+1}] \quad (\text{Eq. 3.18})$$

$$\Gamma_D = \left[\frac{V_{i-1,j-1} + V_{i-1,j+1}}{2} \right] \times [Y_{i-1,j-1} - Y_{i-1,j+1}] \quad (\text{Eq. 3.19})$$

$$\begin{aligned} \Gamma_{\text{total}} = & \left[\frac{U_{i+1,j-1} + U_{i-1,j-1}}{2} \right] \times [X_{i+1,j-1} - X_{i-1,j-1}] \\ & + \left[\frac{V_{i+1,j+1} + V_{i+1,j-1}}{2} \right] \times [Y_{i+1,j+1} - Y_{i+1,j-1}] \\ & + \left[\frac{U_{i+1,j+1} + U_{i-1,j+1}}{2} \right] \times [X_{i+1,j+1} - X_{i-1,j+1}] \\ & + \left[\frac{V_{i-1,j-1} + V_{i-1,j+1}}{2} \right] \times [Y_{i-1,j-1} - Y_{i-1,j+1}] \end{aligned} \quad (\text{Eq. 3.20})$$

The total circulation, Γ_{total} that encompasses the closed cell is calculated by applying the central differencing scheme to the flow velocities and can be expressed as the summation of all the circulations over the various lengths (A, B, C and D) that complete the entire closed cell as presented in Equations 3.14 to 3.20.

$$\zeta = \frac{\Gamma_{\text{total}}}{[X_{i+1,j-1} - X_{i-1,j-1}] \times [Y_{i+1,j+1} - Y_{i+1,j-1}]} \quad (\text{Eq. 3.21})$$

The vorticity, ζ of the cell can then be calculated by taking the total circulation, Γ_{total} surrounding the closed boundary divided by the total area of the cell as shown in Equation 3.21.

This numerical method of calculating the vorticities of the captured flow field using the central differencing scheme are processed using the MatLab program where the instantaneous

vorticity contours are generated to effectively visualize the flow fields over these oscillating NACA4421 airfoils. With incorporating standardized and consistent color map for these instantaneous vorticity contours, comparative analysis can be carried out in a more efficient and accurate manner. These PIV processing methods will be further elaborated in Chapter 4.

In summary, this section on kinematics of fluid motion has provided the theory on velocity field surrounding fluid flow and the associated components of a velocity vector. This laid out the foundation to present the theoretical concept on vorticity which fundamentally equates to the curl of the velocity vector. Subsequently, circulation was explained through the application of Stroke's theorem. All of these theoretical concepts of velocity field, vorticity and circulation provided a continuous and clear link to elaborate on the theory of 2-D PIV measurements and the numerical method employed in the present study to compute the vorticity contours.

3.2 PHYSICS OF FLAPPING WINGS AERODYNAMICS

3.2.1 Clap and Fling Mechanism

Figure 3-5 : Illustration of the ‘Clap and Fling’ Concept where (a) to (d) represents the ‘Clap’ and (e) to (h) represents the ‘Fling’ [13]

In 1973, Weis-Fogh [53] published his influential experimental study on hovering flight in animals and their novel mechanisms where he examined high speed films of the hovering flight of a tiny wasp, *Encarsia Formosa* and discovered an unsteady inviscid high lift mechanism occurring at very low Reynolds numbers. He [53] termed this lift enhancing mechanism; the ‘clap and fling’ mechanism or also known as the ‘Weis-Fogh’ mechanism.

The ‘clap and fling’ mechanism is one of the most complex kinematic motions employed by small natural fliers to generate the necessary lift during flight which simplistically consists of the pair of wings clapping together (clap) and then peeling apart (fling); an example of wing-wing interaction. Weis-Fogh [53] observed that the *Encarsia Formosa*’s pair of wings would come close together in a distinct dorsal clap before the start of its downstroke in Figure 3-5 (d) and this ‘clapping’ motion shown in Figures 3-5 (a) to (d) was previously presented in Vogel’s study [79] on the Fruit Fly, *Drosophila* but did not elaborate further on this phenomenon.

Figure 3-5 demonstrates the wing kinematics and the resultant vortex development during this ‘Clap and Fling’ mechanism [13, 53]. At the beginning of wing-beat cycle as shown in Figure 3-5 (a), the wings will rotate about their leading edges which acts like a hinge until the wings touches each other; forming an outflow and a downward-moving vortex as shown in Figure 3-5 (d) [30]. This half of the wing-beat cycle is known as the ‘clap’. Subsequently, the ‘fling’ starts with the wings in contact with one another and with the trailing edges acting as the hinge, separating the leading edges initially. As the maximum angular displacement of approximately 120° was achieved by the leading edges and forming a V-shape, the trailing edges begin to separate as shown in Figure 3-5 (g). This abrupt change in translation of the wings causes the air flow to accelerate into the expanding gap in between the wings as it generates powerful pair of LEV of equal and opposite signs that enhances the circulation, Γ around the wings which result in the increase in lift forces before the downstroke. Weis-Fogh [53] also concluded that lift was produced almost instantaneously from the beginning of the downstroke and without any Wagner effect. The Wagner effect effectively describes the time lag for the wing to attain the instantaneous circulation values at quasi-steady state conditions due to (1) the viscous effects acting on the stagnation point of the wing causing a delay in the development of the ‘Kutta’ condition at the TE and (2) the shed TE vortex rolling into the starting vortex which counteracts and impedes the growth of the circulation bound to the wing [53].

Subsequently, Lighthill [80] carried out a theoretical study based on a 2-D inviscid model of the ‘fling’ phase where he applied a correction factor to account for the viscous effects. Even though Lighthill’s predictions satisfied the Kelvin-Helmholtz theorem, according to which the circulation, Γ should be zero, he did not dismiss the likelihood that if the wings are broken down into their separate entities, there may be equal and opposite circulations

surrounding the wings, thus each wing is capable of generating its own lift forces for the subsequent kinematic motion [80]. Consequently, Weis-Fogh [53] and Lighthill [80] documented that due to the ‘clap’, as the wings pressed closely together, the opposing circulations of each of the wings cancel each other and this ensures that the trailing edge vortices that are shed on the subsequent stroke are considerably diminished. Hence, due to the absence of these TE shed vortices, the wings are allowed to develop circulation more quickly, thus explaining the immediate generation of lift as observed by Weis-Fogh [53]. This will explain the absence of the Wagner effect during the ‘fling’ phase.

$$\Gamma = \Omega c^2 g(a) \quad (\text{Eq. 3.22})$$

In addition, Lighthill [80] investigated on the effect of the opening angle of the leading edges in the ‘Fling’ phase as shown in Figure 3-5 (g) on the resultant circulation, Γ and demonstrated that a circulation proportional to the angular velocity of the flinging action was produced [13, 80]. The magnitude of circulation produced by the ‘Clap and Fling’ is provided by Weis-Fogh based on the inviscid theory presented as Equation 3.22 where Γ is the circulation, Ω is the angular velocity or the angular rate of change of incidence, c is the chord and $g(a)$ is the function of the leading edge opening angle before the trailing edges separation depicted in Figure 3-5 (g) [81].

Figure 3-6 : Particle Streak Photographs of the 2-D Flow over Rotating Wings undergoing the 'Fling' Phase at $Re \approx 32$ showcasing the Dominant Presence of Leading-Edge Separation Vortices; Time Interval between the Photographs is about 0.5s with a Camera Exposure Time of about 1/16s [55]

Following Lighthill's theoretical study on this 'clap and fling' mechanism, Maxworthy [30, 55] carried out experimental studies on the fluid dynamics of insect flights and in particular, to investigate the Weis-Fogh mechanism for lift generation utilized by insects during hovering flight. Importantly, Maxworthy [55] validated and demonstrated Lighthill's [80] hypothesis on influences of viscous effects specifically on the involvement of a leading-edge separation vortex as depicted in Figure 3-6 (c) on the enhancement of the overall circulation during the 'fling' phase through a series of flow visualization experiments in low $Re \approx 32$ number flow as shown in Figure 3-6.

Figure 3-7 : Sketch illustrating the Flow over the Wing during the 'Fling' Phase showing the Axial Flow in the Separation Vortex that feeds Vorticity into the Tip [55]

One critical finding by Maxworthy [30, 55] was that the circulations contained within these separation vortices were approximately three times greater than Lighthill inviscid theory predictions [80]. Maxworthy [30, 55] attributed these higher circulation values during the 'fling' phase to the actual fluid flow over the wings being able to store the vortical energy within the separation vortex whereas in the inviscid model, these vortex sheets were predicted to diminish in strength as the wings moved apart in the subsequent motion. In addition to that, the circulation enhancement also continued beyond the 120° opening angles. Maxworthy [30, 55] also carried out the investigation on the 'fling' phase using a 3-D model where he documented that the balance between the centrifugal forces and pressure gradients induced a span-wise flow. This span-wise flow helps to prevent the accumulation of the vorticity over the wing which was produced by the leading-edge separation vortices and subsequently, the flow formed a circular tip vortex as illustrated in Figure 3-7. These tip vortices were eventually shed at the trailing edge as a Von Kármán vortex street.

Bennett [54] also noted that the ‘clap and fling’ mechanism was not used continuously and demonstrated that the increase in lift forces are thus modest and exhibited for a only short period of time of approximately one chord of travel. Ellington [82] observed that during insect hovering flights, the wings exhibited a ‘near or partial clap and fling’ motion which were also employed by numerous species of birds and bats during take-off and landing when extra lift was required.

3.2.2 ‘Delayed Stall’ Effect and the LEV

Past literature studies such as by Ellington et al. [31], Maxworthy [55], Dickinson and Gotz [20] and Dickinson et al. [21] on insect flight aerodynamics have all acknowledged the potential gains of wing-attached vortices formed during flight on improving lift generation. These studies also provided evidence that a vortex formed at the leading edge was responsible for high lift forces during flight for these small natural fliers in low Reynolds number flows. In the review carried out by Sane [83] on the aerodynamics of insect flight, it was documented that as the wing increases α , the flow stream separates as it passes over the leading edge but reattaches itself at the trailing edge, thus satisfying the ‘Kutta’ condition. The LEV would reside in the separation zone on the wing surface, thus creating a region of low pressure which resulted in a large suction effect on the suction side of the wing [13]. As α increases and with the presence of the LEV, the flow velocity over the suction side increases thus imparting a larger downward momentum to the fluid leading to a considerable lift enhancement and also to additional drag forces [83]. The LEV was then observed to increase in size as α increases until it no longer can remain attached to the airfoil surface as the ‘Kutta’ condition breaks down. The wing was said to be ‘stalled’ at this point as the flow was unable to impart consistent downward momentum thus lift decreases. The vorticity develops at the trailing edge as the TEV as the detached LEV is shed into the wake. As the TEV is shed, a new LEV forms, thus developing a dynamic process which resulted in a wake of evenly spaced counter-rotating vortices known as the ‘Von Kármán vortex street’. Prior to stall, the presence of the attached LEV sustained high lift forces for about 3 or 4 chords of travel during translation motion [13]. Due to the fact that the flow is separated and yet capable of producing lift forces, this phenomenon is also justifiably known as the ‘delayed stall’ effect.

Figure 3-8 : Polhamus' Leading Edge Suction Analogy; (a) Flow over a Blunt Airfoil and (b) Flow over a Thin Airfoil [20, 84, and 85]

The physics behind the ‘delayed stall’ effect due to the LEV formation over the airfoil was elegantly explained by Polhamus [84] through the ‘leading edge suction analogy’ which is illustrated in Figures 3-8 (a) and (b). Take for example a blunt airfoil exposed to an axial flow as shown in Figure 3-8 (a); the flow will be diverted sharply around the leading edge thus resulting in a suction force, F_{suction} that acts in parallel to the chord axis. This F_{suction} vector adds to the potential force component, F_{normal} , which acts in the normal direction to the wing plane which in turn, causes the resultant force to act perpendicular to the flow velocity. Interestingly, if the blunt airfoil was at low angles of attack, due to the large leading edge suction effect, this resultant force vector will naturally lean towards the leading edge [83, 84]. However, this was not the case for an airfoil with a sharper leading edge as the flow separates once it passes the leading edge and it forms a LEV shown in Figure 3-8 (b). Due to this LEV, the suction force vector, F_{suction} at the LE was eliminated by the establishment of a Kutta-like condition on the leading edge. Conversely, a normal suction force was needed to retain an attachment point of the LEV on the suction side of the airfoil. This resulted in the 90° rotation of the F_{suction} to act in the normal direction to the wing plane as it supplemented the normal potential vector, F_{normal} , thus enhancing the resultant lift vector as demonstrated in Figure 3-8 (b) [20, 83 and 84]. As the resultant force vector acts in perpendicular to the wing plane and not the flow velocity, drag forces will logically increase as well [83, 84]. This is also known as ‘detached vortex lift’ which is similar to the mechanism responsible for lift production for Delta wings [86].

Figure 3-9 : Flow Visualization of the LEV observed during the Downstroke at; (a) $\phi(t) = 0^\circ$ and (b) $\phi(t) = -25^\circ$ using ‘the Flapper’ [31, 85]

Ellington et al. [31] and van den Berg and Ellington [85] were credited for the discovery of the unsteady lift enhancing mechanism by the generation of the LEV or the ‘Delayed Stall’ effect employed by these small natural fliers through experimental work on the Hawkmoth (*Manduca sexta*) in hovering flight. Flow visualizations of the Hawkmoth’s hovering flight were carried out and validated by utilizing a robotic mechanical ‘flapper’ that was capable of replicating the wing kinematics of the hovering Hawkmoth. Ellington et al. [31] and van den Berg and Ellington [85] found a leading-edge vortex formed on the wing of the Hawkmoth during its hovering flight which increased in size from its wing base to tip as shown in Figure 3-9 (a). This observation was in good qualitative agreement with the ‘hovering model’ of the Hawkmoth using ‘the flapper’ [31, 83]. By analyzing the vortical patterns during the downstroke of the ‘hovering model’, the LEV was initiated at 25% of the wing length near the wing base as shown in Figure 3-9 (a). Evidently, a span-wise flow helped to stabilize the LEV over the entire wing span, creating the ‘delayed stall’ effect, to enhance lift during this translation phase [13].

However, as the wing proceeds through its downstroke at $\phi = -25^\circ$ as shown in Figure 3-9 (b), the LEV became unstable and was detached from the wing surface at approximately 75% of the wing length. The detached LEV was subsequently convected outwards by the span-wise flow where it connected to the tip vortex with a broken down vortex core [85]. At this point, the diameter of its core was increased and the axial velocity component was no longer unidirectional [13]. Hence with the loss of axial velocity, the pressure was increased which led to a decrease in lift. This conjoined tip vortex was then shed in a ring-shaped vortex wake by each wing during the downstroke. This similar sequence of events was also previously documented by Maxworthy [55] during his experimental study on the 3-D ‘fling’ phase. Through analyzing the size and growth of the LEV via the flow visualization images and with

the axial flow velocity of the LEV, V_a calculated, the circulation and lift-enhancement characteristics of the LEV can be determined [85].

$$\Gamma_{lev} = \pi \cdot d \cdot V_\theta \quad (\text{Eq. 3.23})$$

Firstly, the circulation of the LEV, Γ_{lev} at any cross-section is proportional to the swirl velocity of the vortex, V_θ and the diameter of the LEV, d as expressed in Equation 3.23 [85]. With the assumption that the helix angle is 45° which is defined as the measured angle of the observed streak-lines of the LEV, the swirl velocity of the vortex, V_θ , is equals to the axial LEV velocity, V_a . With the diameter, d of the LEV measured as the averaged vortex height and width; the circulation of the LEV, Γ_{lev} can be calculated.

$$L' = \rho \cdot V_w \cdot \Gamma_{lev} \quad (\text{Eq. 3.24})$$

Based on the Kutta-Joukowski theorem, the sectional lift per span width, L' can be calculated using Equation 3.24 by substituting the computed Γ_{lev} and where ρ is the fluid density and V_w is the wing velocity [85]. After calculating the Γ_{lev} and L' generated by the bounded LEVs, van den Berg and Ellington [85] concluded that the LEV generated two-thirds of the lift required to support the weight of the Hawkmoth in hovering flight and that the remaining one-third of the lift required was produced by the circulation of the boundary layer vorticity. Subsequently, van den Berg and Ellington [85] investigated on the primary aerodynamic mechanism that was responsible of producing the LEV; either by dynamic stall or rotational lift mechanism. If the LEV was generated through dynamic stall, then the vortex would need to be formed at the start of the downstroke and that the variation in circulation along the wing span would most likely be proportional to the product of span width and chord, ' Sc '. If the LEV was produced by rotational lift mechanisms, the vortex would have to be present before the start of the downstroke in order to be recapture during the downstroke. In addition, the variation in circulation along the span should be proportional of the square of the chord, c^2 if rotational lift was the cause [85]. Through their detailed analysis, van den Berg and Ellington [85] proved that the LEV was produced by the dynamic stall mechanism and not by rotational lift mechanisms as the LEVs were consistently initiated at the leading edge at the beginning to the downstroke and that the variation in the calculated circulation values was proportional to ' Sc ' rather than ' c^2 '.

By definition, dynamic stall is a non-linear unsteady aerodynamic phenomenon that occurs when an oscillating airfoil rapidly changes direction or angle of attack thus resulting in the shedding of a strong vortex from the leading edge above the wing [48]. This powerful shed vortex is then convected downstream and interacts with the TEV being formed. This will then result in a brief increase in lift forces. As soon as this shed vortex passes by the trailing edge, the wing will experience normal stall, causing a dramatic decrease in lift forces [48].

3.2.3 Rotational Circulation

Figure 3-10 : Schematic of Wing Rotation adopted by Dickinson et al. [21, 26]

Dickinson et al. [21] have studied the aerodynamics of insect hovering flight through experiments using a scaled robotic model of the *Drosophila* where they observed that these insect wings undergo extensive pronation and supination near the end of every stroke. Section 1.1.2 has provided more details on this study on insect hovering flight by Dickinson et al. [21]. During these stroke reversals, the wings will rotate or pitch rapidly about the span-wise axis in order to maintain a positive angle of attack thus generating lift forces throughout the hovering stroke cycle as shown in Figure 3-10. These rapid wing rotations brought about an unsteady mechanism termed as the ‘rotational circulation’ which is known to generate ‘rotational forces’ and can be termed as ‘Kramer effect’ named after Kramer [87] who first described this phenomenon [83].

When a flapping wing is undergoing rapid wing rotation near to its stroke reversal, the flow begins to separate at its trailing edge as the stagnation point displaces from the trailing edge,

exhibiting a deviation from the ‘Kutta’ condition. This induces a sharp and energetic gradient at the trailing edge resulting in the shearing of the flow over the wing. Due to the fluid viscosity, the flow has the tendency to resist shear and in turn, promotes the generation of a rotational circulation around the wing in order to restore the ‘Kutta’ condition at the trailing edge and with the intention to oppose the rotational effects of the wing rotation. If the angle of attack of the wing continues to change rapidly, then the ‘Kutta’ condition at the trailing edge may not have the time to be redeveloped, however, the inclination of the flow needing to reattach itself may still produce additional rotational circulations. Hence, these rotational circulations are proportional to the angular rotational velocity of the wing and will continue to be produced till the ‘Kutta’ condition at the trailing edge is restored. This effect can also be known as ‘rotational forces’ [83]. Dickinson et al. [21] and Sane and Dickinson [88] have established that numerous factors can determine or control the effects of rotational circulation on the resultant aerodynamic forces of the flapping wings such as the position of the rotational axis of the wing and the timing when these circulations occurred.

The significance of this ‘rotational circulation’ mechanism was first experimentally investigated by Bennett [89] where simulations of an insect wing undergoing a downstroke that was subjected to a rapid change of incidence were carried out. Bennett [89] highlighted that large increases in lift force were produced given a substantial positive rate of change of incidence when compared to quasi-steady state values at the same angle of incidence. Bennett [89] concluded that there is a mechanism that exists in order for these insects to achieve these large lift forces during its flapping flight under fully unsteady flow regime.

Studies by Dickinson et al. [21] have also shown that the criticality of the ‘rotational circulation’ effect lies in the timing when these circulations occur. The effects of rotational circulation were investigated under three conditions; advanced, symmetrical and delayed in relation to the stroke reversals. The starting angle of attack of the wing determined the timing of the rotational circulation. Through studying the lift and drag forces produced by a pair of flapping wing undergoing the *Drosophila* kinematics motion, they [21] concluded that if the rotation circulation occurred in advance relative to the stroke reversal, it created a “backspin” effect which resulted in a positive lift peak at the end of each half stroke. In contrary, for rotational circulation that was delayed and occurred after stroke reversal, it resulted in a negative lift peak at the beginning of each half stroke. For rotational circulation under symmetrical conditions, it resulted in a positive lift peak before the stroke reversal and a

negative lift peak after the stroke reversal. This proves that small fliers such as insect can generate extra lift forces to supplement the 'delayed stall' induced forces during their translational part of the flapping motion by adjusting the timing of the wing rotation [21]. In addition, their results [21] also showed that the timing of the rotational circulations can have detrimental effects on the aerodynamic forces. Dickinson et al. [21] experimental results were also computationally validated by Ramamurti and Sandberg [64, 65] which was presented earlier in Section 2.12.

Figure 3-11 : Experimental and Numerical C_L for a Fruit-Fly Model Wing with Wing Rotation performed in 'Advanced' before the end of the Stroke [13, 21 and 41]

Apart from Ramamurti and Sandberg [64,65], Sun and Tang [41] also validated Dickinson et al. [21] experimental results to demonstrate these rotational circulation effects on a fruit fly model wing by solving the Navier-Stokes equations numerically. From the computed flow velocity and pressure fields, the aerodynamic forces and vorticity wake structure were generated. Figure 3-11 shows the computed numerical C_L by Sun and Tang [41] comparison with Dickinson et al. [21] experimental results for the 'Advanced' case whereby the wing rotation occurring before the stroke reversals. Conclusively, the numerical simulation results by Sun and Tang [46] matched well with Dickinson et al. [21] results.

Figure 3-12 : Fruit-Fly Model Wing undergoing a 'Rapid Pitch-Up' while Translating with Constant Speed to demonstrate the 'Rotational Circulation' Effects at $Re = 136$ [13, 41]

Similar to Dickinson et al. [21] analysis for the 'Advanced' case, the first peak was produced before the end of each stroke as shown in Figure 3-11 which was attributed to the 'rotational circulation' effects explained earlier. Based on Sun and Tang's results [41], these 'rotational circulation' effects were effectively demonstrated through the vorticity contours shown in Figure 3-12 where the wing was subjected to a 'rapid pitch-up' motion as the angle of incidence was rapidly increased from Figure 3-12 (a) to (c) while translating at a constant speed to simulate the 'Advanced' case. During this 'rapid pitch up' motion, the LEV as indicated by the red arrow was seen to be rotating backwards relative to the translation motion exhibiting the 'backspin' effect that was similarly documented by Dickinson et al. [21]. Sun and Tang [41] attributed the dramatic peak in lift and drag before the stroke reversal to a fast increase in vorticities around the LE and TE as shown in Figures 3-12 (b) and (c) respectively. Sun and Tang [41] and Dickinson et al. [21] also observed a second lift peak which was generated after the stroke reversal as shown in Figure 3-11 and was accredited to a 'wing-wake interaction' or commonly known as the 'wake-capture' mechanism. This 'Wake Capture' mechanism will be elaborated in the next section.

3.2.4 Wake Capture or Wing-Wake Interaction

Figure 3-13 : Schematic of the 'Wake Capture' Mechanism [83]

The 'Wake Capture' mechanism or wing-wake interaction as the name suggests is an unsteady flow mechanism that involves the interaction of the wing with the vorticities that were shed by the preceding stroke [83]. This 'Wake Capture' mechanism was first observed and documented by Dickinson [90] during a 2-D motion on an inclined plate. Figure 3-13 shows the hypothesis of this 'Wake Capture' mechanism [83]. Dickinson [90] documented that as the wing reversed its stroke in Figures 3-13 (a) and (b), it started generating vortices at the LE and TE. Subsequently, in Figures 3-13 (c) and (d), these LEV and TEV were shed into the wake and produced a strong velocity field which is depicted by the dark blue arrows. Dickinson et al. [21] also documented that the direction and magnitude of this strong 'Jet-like' velocity field depends greatly on wing kinematics immediately pre and post stroke reversal. As the wing came to a stop and moved in the opposite direction as displayed in Figures 3-13 (d) and (e), it interacted with the 'Jet-like' velocity field which resulted in a peak in lift forces as depicted by the enlarged light blue arrow in Figure 3-13 (f).

Figure 3-14 : Lift and Rotational Lift Forces at Three Instances of Wing Rotation in relation to Stroke Reversal; (a) Advanced; (b) Symmetrical and (c) Delayed; the Black Dots – Lift Peak due to 'Rotational Circulation' and White Dots – Lift Peak due to 'Wake Capture' [21]

In Dickinson et al. [21] experimental study on insect flight aerodynamics, a consistent lift peak immediately after stroke reversal was observed as shown in Figure 3-14. They [21] ascertained that 'rotational circulation' and vorticities generated by the translating motion alone were not sufficient to account for this peak in lift forces. Hence, they [21] hypothesized that due to the wing interaction with the wake induced by the preceding stroke, the effective flow velocity will be accelerated at the start of the stroke thereby resulting in an increase in lift force production. They [21] also proved their hypothesis by demonstrating that forces were still being produced after the wing has stopped. However, Sun and Tang [41] argued that this lift peak after the stroke reversal was primarily due to the acceleration of the wing rather than the 'wake capture' effects based on their CFD simulations. The reasons behind the differences between these two sets of inferences are still inconclusive but importantly, it was understood that the wing acceleration cannot be discounted in studies on 'wake capture'.

In recent times, Lua et al. [35] have also studied on the effect of 'wing-wake interactions' on lift generation during a flapping motion and found that there were two possible outcomes of wing-wake interactions. First outcome occurred when the wing meets a pair of counter-rotating wake vortices on the reverse stroke where the vortices impinged directly on the windward side, resulting in the acceleration of the incoming flow to the wing, thus increasing the lift produced. Second outcome occurred when the wing encounters one vortex on the reverse stroke; the close proximity together with the vortex suction effect caused a decrease

in net force which in turn reduced the lift generated. Conclusively, Lua et al. [35] revealed that lift generation due to wing-wake interaction depends on the flapping motion and the timing of the reverse stroke which was in agreement with Dickinson et al. [21] conclusions.

In the context of the present study, the implications of the ‘wake capture’ mechanism are relevant in the application of tandem wing aerodynamics. From these past literatures, the fundamental physics behind ‘wake capture’ is still debatable and not sufficiently understood. After reviewing past literatures and theories on these four unsteady flow mechanisms that were presented, it revealed that a lot of these experimental and computational works focused on single flapping wings in coupled heaving and pitching motion. There is still a lot more to be explored on oscillating airfoils at low Reynold number flows. The present study will explore these unsteady aerodynamic mechanisms in action specifically for oscillating tandem airfoils where literatures by Lee [18] and Broering and Lian [44, 45] on tandem airfoils will be referenced. Critically, the present hypothesis agrees that these unsteady flow mechanisms are important to tandem wing aerodynamics but also suggest that more parameters such as S_d , G and ϕ will be able to modify, improve and enable better control of these flow mechanisms, thus affecting the consequential flow structure and aerodynamic performance of these tandem airfoils.

3.3 PHYSICS OF VON KÁRMÁN VORTEX STREET

Before Von Kármán and Burgers [51] published their influential paper on the ‘general aerodynamic theory on perfect fluids’, several pioneering philosophers in classical physics such as D’Alembert [91], Helmholtz [92], Kirchhoff [93] and Rayleigh [94] presented their novel ideas and theories on fluid mechanics to explain the flow over a physical body.

D’Alembert [91] was one of the first to develop any form of a logical theory on air resistance who presented a purely mathematical theory which concluded that if a body moves through an incompressible and inviscid potential flow, the drag force would be zero as the body does not experience any resistance. However, this posed an obvious contradiction as many observations and experiments on moving objects and natural fliers relative to fluids such as air and water produced considerable drag especially so at high Reynolds number flows [91, 95]. This contradiction is well known as the ‘D’Alembert Paradox’.

Figure 3-15: Flow with Discontinuity Surfaces assumed in Theories developed by Kirchhoff [93] and Rayleigh [94]

Years later, in the 1800s', Helmholtz [92], Kirchhoff [93] and Rayleigh [94] attempted to disprove 'D'Alembert' paradox where they developed an inviscid flow theory to describe fluid drag. This inviscid flow theory was based on the free-streamline theory developed by Helmholtz [92] with a moving inclined plate as a flow model that was assumed to exhibit a surface of discontinuity formed at each edge of the plate as shown in Figure 3-15. Another condition inherited through Helmholtz's free-streamline theory [92] would be that the flow had a steady wake behind the body with a constant pressure and that the flow velocities were the same as the body velocity. An important assumption for this theory was that in order for the body to experience non-zero drag, this wake region must be extended to infinity behind the plate. This inviscid separated flow model could reasonably calculate the drag force with a plate perpendicular to the flow where the theory stated that the drag force was proportional to the square of the flow velocity. However, this inviscid flow theory cannot be used to explain drag of a moving body in fluid as documented by Kelvin [95] who highlighted fundamental problems in the assumptions made by Kirchhoff [93] and Rayleigh [94]. In Kelvin's experimental study [95] on the flow over a plate moving through a fluid at a constant velocity, he observed that the wake velocity was equal to the body velocity instead and he rejected that assumption of the infinite wake based on physical justifications. Kelvin [95] also documented that the drag forces obtained through his study were far greater than Kirchhoff [93] and Rayleigh [94] theoretical prediction and attributed the larger drag production to the greater pressure differences between the surfaces of the plate which gave rise to a suction effect at the wake facing side of the plate. This suction effect is created by the unsteady flow in the real wake observed which differed to the inviscid flow theory where it assumed a steady wake with constant pressure.

Figure 3-16 : Schematic of the Boundary Layer Concept by Prandtl [96]

These early theories on aerodynamics mostly dealt with inviscid flow conditions with the assumption that the fluid's viscosity is zero and the flow does not experience any viscous effects such as flow separation. In 1904, Prandtl [96] published a very short yet prominent paper which he presented during the Third International Mathematics Congress held in Heidelberg, Germany. In his presentation, he introduced a novel and revolutionary concept that will change the world of fluid dynamics; the boundary layer in a fluid flow over a surface as shown in Figure 3-16. The special thing about this concept is the outrageous thought of taking account frictional effects in order to effectively describe fluid flow. Euler theorized fluid flow by formulating partial differential equations to describe pressures and velocities of a fluid which is known as the 'Euler's Equations' presented in the earlier section of this chapter. However, these Euler's Equations for fluid flow do not take account these viscosity effects. The complete Navier-Stokes equations were then formulated in the following century to take account for viscosity effects but till this day, no one has presented a general analytical solution to these equations.

Prandtl [96] theorized that the frictional effects applied to the flow condition will cause the fluid directly adjacent to the body's surface to stick together, thus assuming the 'no-slip' condition at the surface which meant that the fluid velocity at the surface is zero. However, this 'no-slip' condition at the surface would only be experienced or applied to a 'boundary layer' which encompasses only a small thickness in relation to the thickness of the body and it consists of large velocity gradients. The outer edge of the boundary layer represents the point where the fluid flow velocity is 99% of the freestream flow velocity as depicted in Figure 3-16. With this introduction of the boundary layer concept by Prandtl, another key concept presented would be the tendency of the boundary layer to separate, especially so for

low Reynolds number flows where the viscous effects are dominant. As the angle of attack of an airfoil increases above its stall angle, the flow in the boundary layer separates due to the adverse pressure gradients and lift dramatically decreases. In addition, due to these frictional effects, the streamlines do not follow the contours of the airfoil surface from the leading edge all the way to the trailing edge due to this flow separation event, thus leaving an eddying region downstream called the ‘wake’ [96]. In the case of a laminar separated flow, this wake primarily consists of the residues of the boundary layer from the top and bottom airfoil surfaces that propagated as ‘shear layers’ and the region in between these ‘shear layers’ which is known as the ‘dead-air’ region which is similarly illustrated in Figure 3-15. Consequently, the ‘dead-air’ region cannot achieve high pressure values thus, creating an unbalance in the pressures between the front and the rear of the airfoil which result in a pressure drag also known as the ‘wake drag’ [96]. Apart from this ‘wake drag’, the airfoil also experiences ‘pressure drag’ which is the force component that acts in parallel to the direction of motion and is defined as the work that needs to be overcome in order to achieve lift. The next drag component would be ‘frictional drag’ or ‘skin friction drag’ is defined as the summation of tangential forces to the surface of the airfoil taken in the same direction. The last drag force is the ‘induced drag’ brought about by the wing-tip vortices applicable to ‘finite’ wing or airfoil. For the present study, the NACA4421 airfoils are assumed to be ‘infinitely’ long with negligible wing-tips effects and as a result, the ‘induced drag’ is zero.

Figure 3-17 : Von Kármán Vortex Street in the Wake of a Flow over a Cylinder [51]

Von Kármán at that time was a graduate student of Prandtl and he was intrigued by the fact that the flow pattern in the ‘wake’ was not well understood. Von Kármán [97] first started by characterizing and modelling the double rows of alternating vortices observed in the wake of a viscous flow behind a cylinder. With a flow from left to right as shown in Figure 3-17, Von Kármán [97] observed a double row of alternating vortices where the upper row vortices were turning clockwise and the lower row vortices were rotating in the opposite direction.

Figure 3-18 : (a) Symmetrical Arrangement of the Upper and Lower Rows of Vortices and (b) Asymmetrical Stable Arrangement of Alternating Rows of Vortices where L denotes the Axial Spacing and B denotes the Vertical Spacing [97]

$$\frac{B}{L} = \frac{1}{\pi} \cosh^{-1} \sqrt{2} \approx 0.2806 \quad (\text{Eq. 3.25})$$

$$\text{St} = \frac{fd}{U} \quad (\text{Eq. 3.26})$$

Von Kármán [97] went on to prove that, in Kirchhoff [93] and Rayleigh [94] inviscid separated flow theory, while the discontinuity at the body's surfaces could be considered as vortex sheets, these vortex sheets were highly unstable [51]. He also formulated a theoretical model for this system of vortices and proved that if these rows of vortices were arranged symmetrically as shown in Figure 3-18 (a), the system becomes unstable. Hence, he determined that only the asymmetrical arrangement with a certain ratio of axial distance between the vortices, L and vertical distance between the two rows of vortices, B ensures a stable system as shown in Figure 3-18 (b) [51, 97]. Von Kármán deduced this $\frac{B}{L}$ ratio in Equation 3.25 to be approximately 0.2806 for a flow over a cylinder [97]. With the publication of his concept on these two rows of alternating vortices, he also conceptualized the idea of deducing the wake drag induced by the momentum carried by the vortex system through analyzing the vortex patterns at the wake named as the 'Von Kármán vortex street' [97]. Another important non-dimensional parameter to effectively describe the characteristic of the vortex shedding is the Strouhal number, St which was defined earlier in Equation 3.26.

Figure 3-19 : (a) Von Kármán Vortex Street (Drag-Inducing) and (b) Reverse Von Kármán Vortex Street (Thrust- Inducing) [48]

Past studies have also successfully explained that the drag and thrust forces generated can be deduced by analyzing these vortex street patterns [51]. In Figure 3-19 (a), the Von Kármán vortex street behind the elongated body induces drag as the shed vortices create a mean velocity in the opposite direction of the mean flow. On the other hand, when the right flow conditions meets certain criteria, a reverse Von Kármán vortex street is introduced inducing a jet-like flow field as the shed vortices change its rotational direction, creating a velocity in the same direction as its mean flow as shown in Figure 3-19 (b). As a result, the flow gains substantial amount of momentum and gives reaction force back to the flow, thus producing thrust [48, 51]. If a vector is drawn in between each of the alternating pair of shed vortices based on the orientation and rotational directions of the pairing as illustrated by the red arrows shown in Figures 3-19 (a) and (b), the vectors will be pointing upstream for the drag-inducing Von Kármán Vortex street and pointing downstream for the thrust-inducing Reverse Von Kármán Vortex street. Hence, with these illustrated ‘vectors’, one can easily identify if the Von Karman Vortex Street induces drag or thrust forces.

Figure 3-20 : (a) Neutral Wake (Indicative of Zero Drag) and (b) Deflected Wake (Lift-Thrust Producing) [48]

Figure 3-21 : Dye Flow Visualization of the Various Vortex Patterns; (a) Von Kármán Vortex Street, (b) Neutral Wake, (c) Reverse Von Kármán Vortex Street and (d) Deflect Wake [48]

Based on Von Kármán and Burgers [51] work on these vortices, they deduced that the wake drag would be directly proportional to the vertical spacing, B . Hence, if the wake pattern exhibits vortices with zero vertical spacing similar to the one shown in Figure 3-20 (a), then the induced-wake drag would be zero and this wake is known as the ‘Neutral’ wake. Figure 3-20 (b) also exhibits a deflected wake which consists of an asymmetrical pattern. In this case, a lift-thrust producing wake is presented which is commonly observed for an airfoil undergoing large heaving velocities [48]. The various vortex wake patterns are also displayed in Figure 3-21 for reference [48].

This section has systematically presented the pioneering novel ideas and theories on fluid mechanics to explain the flow over a physical body such as the ‘Flow with Discontinuity Surfaces’ developed by Kirchhoff [93] and Rayleigh [94] and ‘Boundary Layer’ concept by Prandtl [96]. These fundamental concepts are important seeds of inspirations of the formulation of the Von Kármán vortex street which is the main focus of this section. This section then presented the theoretical concepts of Von Kármán Vortex Street and the ability to infer the nature of the vortex street by characterizing the vortex shedding patterns. Critically, this Von Kármán Vortex street concept inspired the development of the Vortex Shedding Model for the present study which will be elaborated in Section 5.7.

3.4 PERFORMANCE PARAMETERS FOR OSCILLATING AIRFOIL

Figure 3-22 : Single Airfoil Oscillating Motion Principle Aerodynamic Forces

In the context of the present study, the aerodynamic forces, kinematic motion and principal performance parameters relating to only oscillating airfoils will be presented in this section. Firstly, an asymmetrical airfoil with a chord, c and a span width, s that is pitching or oscillating about its aerodynamic center, ac , and is fully immersed in an axial fluid flow velocity, U_∞ as shown in Figure 3-22 is considered. In the present study, the airfoil will be pitched sinusoidally at pitch amplitudes denoted as $\alpha(t)$. Throughout its pitching or oscillating kinematic motion, the airfoil will generate three principal aerodynamic forces acting about its aerodynamic center; the lift force is denoted as F_y which acts in the y -direction, the drag force is denoted as F_x which acts in the x -direction parallel to the flow direction and the pitching moment denoted as T_z which acts in the z -direction aligned with the pitching axis. Negative drag force, $-F_x$ is assumed to be equivalent to positive thrust force which acts in the opposite $-X$ direction, hence the terms ‘negative drag force’ and ‘thrust force’ are interchangeable.

The aerodynamic center is defined as the point on the airfoil where the pitching moment, T_z does not vary with the increase in angle of attack, α [98] and in the present study, the aerodynamic center is taken at 25% chord behind the leading edge as shown in Figure 3-22. If the airfoil was pitching about its aerodynamic center, the expected variation in the pitching moment coefficients throughout the oscillations would be small. Importantly, in the experimental arrangement, the pitching axis is aligned with the center of the force transducer to ensure that there is no need for compensation to account for any offset. Moreover, the pitching axis is taken about the aerodynamic center and not the pressure center so as to ensure pitching stability [98].

Figure 3-23 : Single Airfoil Oscillating Kinematic Motion

As mentioned earlier, the oscillating airfoil is subjected to a sinusoidal kinematic motion as shown in Figure 3-23 which is usually employed by small natural fliers during pronation and supination near its stroke reversals. At its initial condition, $t/T = 0$ where T denotes the period of oscillation and t is the instantaneous time, the airfoil starts at $\alpha = 0^\circ$. Subsequently, the airfoil will be pitched up till its maximum pitch amplitude, $\alpha_{\max}$ at $t/T = 0.25$ and this covers half of the ‘upstroke’. Next, the airfoil will undergo its first stroke reversal as it begins its ‘downstroke’ from $t/T = 0.25$ to $t/T = 0.75$ and pitches from its $\alpha_{\max}$ to its minimum pitch amplitude, $\alpha_{\min}$. Then the airfoil changes stroke direction again to complete the full oscillating cycle with the 2nd half of the ‘upstroke’ back to $\alpha = 0^\circ$ at $t/T = 1.0$.

$$\alpha(t) = \alpha_{\max} \sin(\omega t) \quad (\text{Eq. 3.27})$$

$$\omega = 2\pi f \quad (\text{Eq. 3.28})$$

$$\alpha(t) = \alpha_{\max} \sin(2\pi f t) \quad (\text{Eq. 3.29})$$

This oscillating motion can be expressed in terms of its rotational speed, ω (rad/s) and instantaneous time, t as shown in Equation 3.27. The rotational speed can then be expressed in terms of its flapping frequency, f (Hz) as depicted in Equation 3.28. Thus, the pitching kinematic motion used in the present study, can also be expressed as shown in Equation 3.29. The time-averaged aerodynamic forces generated by the pitching airfoil will be elaborated. $F_x(t)$ and $F_y(t)$ denote the instantaneous drag force and lift force respectively. These notations

used to represent these time-averaged forces are referenced from Anderson et al. [99] in their propulsive efficiency study on oscillating foils.

$$\overline{F_x} = \frac{1}{T} \int_0^T F_x(t) dt \quad (\text{Eq. 3.30})$$

The time-average drag force, $\overline{F_x}$ per pitching cycle can be calculated by integrating the instantaneous drag force, $F_x(t)$ produced over the period of one pitching cycle, T as shown in Equation 3.30.

$$\overline{F_y} = \frac{1}{T} \int_0^T F_y(t) dt \quad (\text{Eq. 3.31})$$

The time-averaged lift force, $\overline{F_y}$ can be similarly calculated using Equation 3.31. These two time-averaged aerodynamic forces calculated are used to compute the two main principal averaged force coefficients, C_L and C_D . Clancy [98] documented that through carrying out a dimensional analysis, it was determined that the lift and drag forces were proportional to its fluid density, ρ , free stream flow velocity, U_∞ and wing area, A_w . However, these conditions will only stand provided that there are no changes to the shape of the airfoil, viscous and compressibility effects [98]. Hence, the utilization of these force coefficients is acceptable to facilitate unbiased comparisons for all the present experimental cases as there are no changes to the abovementioned properties.

$$C_D = \frac{\overline{F_x}}{\frac{1}{2} \rho U_\infty^2 A_w} = \frac{\overline{F_x}}{q A_w} \quad (\text{Eq. 3.32})$$

$$C_L = \frac{\overline{F_y}}{\frac{1}{2} \rho U_\infty^2 A_w} = \frac{\overline{F_y}}{q A_w} \quad (\text{Eq. 3.33})$$

The average drag force, $\overline{F_x}$, can then be non-dimensionalized to provide the drag coefficient, C_D using Equation 3.32 where $\frac{1}{2} \rho U_\infty^2$ denotes the dynamic pressure, q and A_w denotes the wing area. A_w is calculated by multiplying the chord, c and the span width, s and for the present study, A_w is fixed at 0.02625 m^2 . The lift coefficient, C_L can also be similarly calculated using Equation 3.33.

4. EXPERIMENTAL SETUPS AND METHODS

Figure 4-1 : Experimental Setups and Methods

In Figure 4-1, the experimental setups and equipments used for the present’s study are presented along with their respective validation and error analyses to ensure that inherent measurement errors are accounted for and that the experiments are carried out as accurately as possible without much external disturbances that might contribute to the measurement errors.

The overview and specifications of the NTU water tunnel are presented along with the pitot-tube and hot-film measurements results. The NACA4421 airfoil properties and its geometric scaling validation are also presented. Subsequently, the tandem airfoils oscillating mechanism primarily employed in the present study is elaborated. The overview and operation of the force transducers, force measurement error analysis and processing methods are documented. Lastly, the Particle-Image-Velocimetry (PIV) measurements experimental setup is introduced along with its validation, calibration and post processing methods.

4.1 NTU WATER TUNNEL

Figure 4-2 : (a) NTU Water Tunnel; (b) Centrifugal Water Pump with Volumetric Flow Rate Meter; (c) Dimensional Drawing of the Water Tunnel and (d) Pump Controller

The water tunnel shown in Figure 4-2 (a) comprises of a front water tank where the flow develops and converges into the test section with the use of a water centrifugal pump as shown in Figure 4-2 (b) that can provide a maximum free stream water velocity of 60Hz or 0.204m/s. The flow in the water tunnel is a closed loop as it is being circulated through a drainage point and a series of piping underneath the setup as shown in Figure 4-2 (c). The front water tank consists of an array of screeners as to ensure the flow remains laminar throughout the water tunnel. The test section is made of high transparency tempered glass as to enable the viewing from the side and bottom of the tunnel. At the end of the tunnel, the small vertical glass window will facilitate in viewing the front end of the foil in the downstream. The water tunnel is 6m long and is 1.5m at its widest portion at the rear end. The test section measures 0.6m by 0.45m which corresponds to an area contraction ratio of 2.5:1. The flow rate of the water in the tunnel is measured by an electronic magnetic flow rate meter as displayed in Figure 4-2 (b) and is controlled via the pump speed controller shown in Figure 4-2 (d).

4.1.1 Pitot Tube Flow Measurements

Figure 4-3 : DRUCK Pressure Transducer [100]

It is vital to calibrate the pump speed controller to the freestream water flow velocity, U_∞ across the water tunnel test section. This set of calibration was carried out using Pitot-tube flow measurements where it consists of mounting the Pitot tube at the horizontal-mid test section and connecting it to the DRUCK pressure transducer shown in Figure 4-3.

Figure 4-4 : Schematic of the Working Principle of Pitot Tubes [101]

Essentially the Pitot tube and pressure transducer are used to measure the pressure difference at the mid-section induced by the incoming freestream water velocity denoted as V_1 . This pressure difference, Δp between Point 2 and Point 1 as illustrated in Figure 4-4 will then be used to compute V_1 through employing the Bernoulli's equation which will be explained as follows.

$$P_1 + \frac{\rho V_1^2}{2} + \gamma h_1 = P_2 + \frac{\rho V_2^2}{2} + \gamma h_2 \quad (\text{Eq. 4.1})$$

$$P_1 + \frac{\rho V_1^2}{2} = P_2 \quad (\text{Eq. 4.2})$$

$$V_1 = \sqrt{\frac{2(P_2 - P_1)}{\rho}} = \sqrt{\frac{2\Delta p}{\rho}} \quad (\text{Eq. 4.3})$$

The Bernoulli's equation in Equation 4.1 is applied between Point 1 and Point 2 where P denotes the static pressure, the second term, $\frac{\rho V^2}{2}$ denotes the dynamic head and third term, γh denotes the hydrostatic head. With the flow velocity at Point 2, $V_2 = 0\text{m/s}$ and that $h_1 = h_2$, Equation 4.1 reduces to Equation 4.2. In Equation 4.2, the stagnation pressure at Point 2, P_2 is equals to the sum of freestream static pressure, P_1 and freestream dynamic pressure at Point 1, $\frac{\rho V_1^2}{2}$. The incoming freestream flow velocity, V_1 can then be computed using Equation 4.3 as a function of the pressure difference between Point 2 and Point 1, Δp and the fluid density, ρ .

Figure 4-5 : Water Flow Velocity Calibration Results

Consequently, the actual freestream water flow velocity, U_∞ can then be computed and tabulated at the various pump controller speed settings as shown in Figure 4-5. The straight line shows a linear relationship between U_∞ and the pump rotating speed. In the present study, all the experiments were carried out at a pump speed of 30Hz which corresponds to a freestream water flow velocity, U_∞ of 0.102 m/s as circled in red in Figure 4-5.

4.1.2 Constant Temperature Anemometry (CTA) Measurements

Figure 4-6 : (a) Schematic of mini-CTA System [102]; (b) CTA Experimental Setup and (c) Cross-section of Test Section

Prior to conducting any experiments in the water tunnel e.g. PIV or Force measurements, the water tunnel flow was calibrated so as to ensure that the uniformity of the flow and the turbulence intensity was within the acceptable range to produce reliable experimental results. The water tunnel calibration was being conducted using the Dantec Mini Constant Temperature Anemometry (CTA) System as shown in Figure 4-6 (a) where the uniformity and turbulence intensity of the flow across the test section in the Z (horizontal) and Y (vertical) directions were measured as illustrated in Figure 4-6 (b). The working principle of the CTA system is based on the cooling effect of a flow over a heated body which in this case, is a heated fine resistive nickel fiber-film that measures 1.25mm in length and has a quartz coating of 2 μ m in thickness. The ‘55R11’ fiber-film sensor probe was selected as it is the most suitable probe for low to medium flow velocities measurements in water. The film probe axis was positioned in parallel to the freestream flow direction as shown in Figure 4-6 (c) in order to measure the flow velocities. The film probe was then connected to one arm of a Wheatstone bridge and was heated by an electrical current. A servo amplifier kept the

bridge balanced by controlling the current to the sensor to keep the preset temperature constant. The bridge voltage, E , represents the heat transfer and is a direct measure of the flow velocity, U . The CTA measured flow velocity at a local point and provided continuous velocity plots in time series which were processed into amplitude and time-domain statistical data for e.g. mean velocity, turbulence intensity, higher order moments, auto-correlations and power Spectra as displayed in Figure 4-6 (a).

4.1.2.1 CTA Calibration Results

Mean velocity:
$$U_{mean} = \frac{1}{N} \sum_1^N U_i \quad (\text{Eq. 4.4})$$

Standard deviation of velocity:
$$U_{rms} = \left(\frac{1}{N-1} \sum_1^N (U_i - U_{mean})^2 \right)^{0.5} \quad (\text{Eq. 4.5})$$

Turbulence intensity:
$$T_i = \frac{U_{rms}}{U_{mean}} \quad (\text{Eq. 4.6})$$

Uniformity:
$$\text{Uniformity} = [1 - (U_{rms} / U_{mean})] * 100\% \quad (\text{Eq. 4.7})$$

One of the many advantages of using the CTA measurement system in the present study is that the instantaneous flow velocity, U_i at any particular point on the cross sectional area of the test section can be measured at a high sampling frequency of 1000Hz. These U_i measurements were averaged out to provide a high resolution mean flow velocity, U_{mean} as expressed in Equation 4.4. The standard deviation of the flow velocity, U_{rms} , was calculated using Equation 4.5. Subsequently, the turbulent intensity, T_i was computed by dividing the U_{rms} and U_{mean} in Equation 4.6. The flow uniformity was then be calculated using Equation 4.7.

Mini-CTA type 54T30, 54N80, 54N81 and 54N82: Selecting and adjusting overheat. 9054S4016

Probe identification Org. 980528/TSV
Rev. 100531/MHA
DMS no. 4867v7

Insert probe specific parameters etc.

Sensor resistance, R_{20}	6.40 Ω	See probe box
Sensor lead resist., R_L	0.50 Ω	SSP11/81 family
Support resistance, R_S	0.50 Ω	Non standard
Cable resistance, R_C	0.20 Ω	Cable 9006A1863
Sensor TCR, α_{20}	0.40% /K	
Desired Film temp, T_f	70 $^{\circ}\text{C}$	Sensor temperature
Temperature of flow	20 $^{\circ}\text{C}$	Temperature during measurement

Click box below to select resistance range.
(Check label on the Mini-CTA for actual range!)
 (Standard: 4-20 ohms)

Set decade controls as follows:
(grey dot indicates switch in down position)

Calculated film operating resistance etc.

Over temperature, ΔT	50 $^{\circ}\text{C}$
Operating resist, R_f	7.68 Ω
Total resistance, R_T	8.88 Ω
Overheat ratio, a	0.20
Bridge ratio, M	1:20
Decade resistance, R_D	177.6 Ω

Figure 4-7 : Input Parameters for the CTA system for the Water Tunnel Calibration

The Desired Film temperature, T_f was chosen to be moderately high at 70°C so as to produce the most stable and reasonably accurate results as highlighted in the red box in Figure 4-7 which also shows the other input parameters for the CTA system for the water tunnel calibration.

Figure 4-8 : CTA Calibration Results in the Z-direction displaying Flow Velocity and Turbulence Intensity

The first calibration set was carried out horizontally with the probe located in the middle of the testing section at $y = 0$ and then it measures the instantaneous water flow velocity, U_i at intervals of 1cm along the Z-direction. Figure 4-8 shows the calibration result that plots the instantaneous flow velocity, U_i and its turbulence intensity, T_i at each measurement point along the horizontal Z axis. Figure 4-8 also illustrates that both U_i and T_i along the Z-axis are relatively linear with the exception of the near-wall region where the wall boundary layer gives rise to higher turbulence intensities and inconsistent velocities. By taking into account the flow velocity and turbulent intensity measurements from $Z = -20\text{cm}$ to 20cm which translates to approximately 90% of the width of the test section (shaded in grey), the average turbulence intensity and average uniformity of the flow in the mid-horizontal plane of the test section are calculated to be 0.61% and 95.14% respectively.

- Average Turbulence Intensity = $U_{rms} / U_{mean} * 100\% = \underline{\underline{0.61\%}}$
- Average Uniformity = $[1 - (U_{rms} / U_{mean})] * 100\% = \underline{\underline{95.14\%}}$

Given that the NACA4421 airfoils have a chord, c of 0.075m oscillating about $\frac{1}{4}$ chord length and that the tandem airfoils oscillate at a maximum gap distance, G of $\pm 0.2\text{m}$, the maximum wing swept area covers a maximum width of $\pm 0.07625\text{m}$ of the test section as indicated in the Orange shaded area in Figure 4-8. This maximum wing swept area covers only 34% of the entire width of the test section.

This CTA calibration measurement results in the horizontal Z-direction demonstrate that the flow across the horizontal x-z plane of the test section have a very good uniformity of approximately 95% and an acceptably low turbulence intensity, T_i of 0.60%. In addition, by superimposing the maximum wing swept area of the NACA4421 airfoils, it shows in Figure 4-8 that the flow across these airfoils is uniform and not affected by near-wall effects and turbulences from the incoming flow.

Figure 4-9 : CTA Calibration Results in the Z-direction (Flow Velocity and Turbulence Intensity)

The second calibration measurement set was carried out along the vertical Y-axis where the probe was initially displaced from the bottom to the top of the test section at intervals of 1cm. The flow velocity measurements showed that the flow remains linear from 2cm to 41cm with the exception of near the bottom wall and near the top of the water surface as shown in Figure 4-9. Moreover, the flow exhibited very low turbulence intensity throughout most of the points along the Y axis with the only exception of relatively high turbulence intensity at the bottom wall region of approximately 2mm in width.

Considering that the airfoils were setup to have a minimum bottom wall clearance of approximately 0.03m and with a span width of 0.35m, the maximum wing swept area in the mid-vertical plane is superimposed in the Figure 4-9 shaded in green. Therefore, applying the presented statistical flow equations across this maximum wing swept area; the calculated average flow uniformity is 98.05% with an average turbulence intensity of 0.83%.

Critically, this second calibration measurements have proven that it is essential to ensure that the gap between the floor of the test section and the bottom tips of the airfoils are kept to a minimum of 0.03m where the flow is unaffected by the near bottom wall effects thus maintaining its good flow uniformity and low average turbulence intensity.

4.2 NACA4421 AIRFOIL

4.2.1 Profile Characteristics

Figure 4-10 : NACA4421 Profile [103]

NACA 4421 Airfoil Profile			
Profile		Aerodynamic Characteristics	
Thickness:	21.00%	Max C_l :	1.847
Camber:	4.00%	Max C_l Angle:	15.0°
Trailing Edge Angle:	28.10%	Static Stall Angle, α_{ss} :	15.0°
Lower Flatness:	46.10%	Max L/D:	37.59
Leading Edge Radius:	6.30%	Max L/D Angle:	7.0°
		Mean Operating Angle:	7.0°
		Zero-Lift Angle:	-4.5°

Table 4-1 : NACA4421 Airfoil Profile and Aerodynamic Characteristics [104]

Two NACA4421 airfoils are used for both static and oscillating tandem cases in the present study. The NACA4421 profile shown in Figure 4-10 is selected due to its asymmetrical profile and capability to generate lift at low Re number flows. It has been used in past studies on wind turbines such as by Kanya and Visser [105] on the design of small horizontal axis wind turbines and by Swalwell et al. [106] on turbulence intensity effects on wind turbine blades. Its profile and aerodynamic characteristics are presented in the Table 4-1 [104].

Figure 4-11 : NACA4421 Airfoil Physical Dimensions

Each airfoil is CNC machined from an Aluminum 6061 block and has a span width, s of 0.35m and a chord, c of 0.075m which correspond to a wing area, A_w of 0.02625m^2 and an aspect ratio, AR of 4.67 as shown in Figure 4-11. The pitching axis taken at $\frac{1}{4}c$ from the leading edge which normally corresponds to aerodynamic center of the airfoil based on the thin airfoil theory [107]. Based on the water tunnel calibration CTA results presented earlier, the flow exhibited a high uniformity of 98% and a low turbulence intensity of 0.83% in the region above 0.02m from the test section floor. Hence, in order to reduce any wing tips (span-wise) effects and near wall effects, the gap distance between the bottom airfoil tips and floor is maintained at 0.03m. Past studies on dragonflies such as by Rival et al. [108] documented that the span-wise flow effect is not an influential characteristic for dragonflies. For the present study, the freestream water flow velocity at the test section of the water tunnel is set at $U_\infty = 0.102\text{m/s}$ which corresponds to a low Reynolds number of 8.6×10^3 which falls within the laminar flow regime ($Re < 10^5$).

4.2.2 Geometric Scaling Validation

Figure 4-12 : Correlation Chart by Greenewalt [109] between the Wing length and Weight of Flying Subjects [26]

As described earlier, the purpose of the present study is to emulate the superior aerodynamic prowess of natural fliers for the application of MAVs. Ho et al. [26] documented in their study that the MAVs desired individual wing length would be approximately 7cm and based on the correlation chart shown in Figure 4-12, this will correlate to a body weight of about 7-10g as indicated by the green dotted lines. This would be the ideal wing length to body

weight ratio for a real-sized MAV which falls on the linear regression line of the natural fliers specifically, the humming birds. Due to the challenges of visualizing the flow structure and accurately measuring forces produced by such a small wing, it is necessary to geometrically scale the NACA4421 airfoils to ensure that the wing length to weight ratio falls within this desired weight length to weight correlation in Figure 4-12.

With the assumption that the NACA4421 airfoil covers 80% of the 3-D material volume, this gives an approximate material volume of 336 cm^3 . With the density of Aluminium 6061 at 2.70 g/cm^3 , this gives a total weight of each airfoil to be about 907.2 g. With the calculated weight of the NACA4421 airfoil and the wing length of 35 cm, the flapping condition does fall in the same regression line as the ideal MAV case indicated by the red dotted lines in Figure 4-12. Conclusively, this has shown that the NACA4421 airfoils used in the present study have been geometrically scaled accurately to size to replicate the flapping performance of small natural fliers and its ideal MAVs wing length to weight ratio.

4.3 TANDEM OSCILLATING AIRFOILS SETUP

4.3.1 Airfoil Oscillating Mechanism

Figure 4-13 : (a) Cool Muscle Servo-Stepper Motor and (b) CoolWorks Lite Software

The NACA4421 airfoils in the present experiments are oscillated or pitched at various pitching amplitudes and rotational speed by using a Cool Muscle Servo-stepper motor as shown in Figure 4-13 (a). The pitching mechanism is controlled via the Cool Works Lite Software in Figure 4-13 (b) by keying in the calibrated pulse inputs in the ‘Motor Parameter Column’. These pulse inputs correlate to various oscillation parameters such as pitch amplitudes, rotational speeds and acceleration. For the present experimental setup, there are two pulse inputs that are of interest; (1) the positional pulse input (pulse, p) that controls the

pitch angle and (2) the rotational speed pulse input (pulse per second, pps) that controls the velocity of the oscillation. In order to input the correct pulse to the required oscillation parameters, it is important to calibrate and correlate these position and velocity pulse inputs to its actual pitching parameters. One of the drawbacks of using this stepper motor is that these pulse inputs can only be entered into the control software as a whole integer. This will incur a small amount of measurement error which will be elaborated in the later section.

The first set of calibration would be to determine the correlation between the positional pulse input, p , to the rotational angle of the motor driving shaft. The rotational angle of the driving shaft will directly control the pitching angles and amplitudes of the NACA4421 airfoils. To determine its correlation, firstly, indication lines were marked on both the driving shaft and the base plate and subsequently, the rotational displacement of the driving shaft is physically measured using a calibrated protector at various positional pulse inputs. Through this first set of measurements, it was determined that 1 positional pulse input, p will correlate to 0.357° of rotational displacement.

Table 4-2 : Correlation Table for Positional Pulse Inputs, p

Approximate $\alpha(^{\circ})$	Positional Pulse Input, p	Actual Pitch Angle ($^{\circ}$)	Measurement Error (%)
1	3	0.99	0.65%
5	14	4.97	0.65%
7	19	6.95	0.65%
8	22	7.95	0.65%
10	28	9.94	0.65%
15	42	14.90	0.65%
20	56	19.87	0.65%
25	70	24.84	0.65%
30	83	29.81	0.65%
35	97	34.77	0.65%
40	111	39.74	0.65%
45	125	44.71	0.65%
50	139	49.68	0.65%
55	153	54.64	0.65%
60	167	59.61	0.65%
65	181	64.58	0.65%
70	195	69.55	0.65%
75	209	74.51	0.65%
80	223	79.48	0.65%
85	237	84.45	0.65%
90	250	89.42	0.65%
100	278	99.35	0.65%

Hence, a correlation table for the positional pulse inputs can be formulated as shown in Table 4-2. Due to the inherent nature of stepper motor control, with the whole integer positional pulse inputs, the actual pitch (rotational) angle of the driving shaft will have a slight deviation from the set pitch angle which corresponds to a systematic ‘measurement error’ of 0.65%. This measurement error is relatively small and moving forward in the present study, the experimental results will be presented in terms of the approximate pitch angles or amplitudes.

Figure 4-14: Actual Rotational Speed (°/s) versus Pitch Amplitude, $\pm \alpha$ (°) at 3pps

In the second set of calibration, a few sets of power setting measurements were carried out to determine the correct correlation of the rotational speed pulse inputs, pps to the actual rotational speed of the motor driving shaft. The complication of this correlation is attributed to the non-linear nature of the rotational speed setting of the motor. This means that, if the rotational speed pulse input is set at 3 pulse/sec (pps) and when subjected to a range of pitch amplitudes, the actual rotational speed will exhibit an increasing non-linear trend as displayed in Figure 4-14.

Table 4-3 : Correlation Table for Motor Rotational Speed

Position		Speed	
$\alpha(^{\circ})$	Pulses	Pulse / Sec	Rotational Speed ($^{\circ}/s$)
10	28	6	93.02
15	42	4	88.235
20	56	4	97.56
30	83	3	88.88
40	111	3	92.485
45	125	3	93.75
60	167	3	96.774

Hence, due to the non-linear actual motor rotational speed characteristic exhibited at a fixed rotational speed pulse input, the next logical approach is to fix the desired rotational speed instead. With the desired rotational speed set at $100^\circ/\text{s}$, the correct rotational speed pulse input (pps) which will produce an actual rotational speed closest to the set $100^\circ/\text{s}$ would be determined for the entire range of α . The correlation table for the motor rotational speed and speed pulse inputs (pps) to the corresponding pitch amplitudes is formulated as shown in Table 4-3.

Figure 4-15: Linearization of Actual Motor Rotational Speed

Figure 4-15 demonstrates that at this fix rotational speed setting and by inputting the correct rotational speed pulse inputs, the motor will be able to produce a relatively linear output rotational speed that averages at $93^\circ/\text{s}$ with a tolerance of $\pm 5^\circ/\text{s}$ across the range of pitch amplitudes. Figure 4-15 also shows that the motor displays a very stable output at a higher α range of $> \pm 30^\circ$.

$$\kappa = \frac{\pi f c}{U_\infty} \quad (\text{Eq. 4.8})$$

Table 4-4 : NACA4421 Oscillation Parameters

Pitch Amplitude, $\pm \alpha$ (°)	Rotational Speed (°/s)	Distance Covered in 1 cycle (°)	Time Taken for 1 cycle, T (s)	Flapping Frequency, f (Hz)	Reduced Frequency, κ
10	93.02	40	0.43	2.33	5.37
15	88.235	60	0.68	1.47	3.40
20	97.56	80	0.82	1.22	2.82
30	88.88	120	1.35	0.74	1.71
40	92.485	160	1.73	0.58	1.34
45	93.75	180	1.92	0.52	1.20
60	96.774	240	2.48	0.40	0.93

Through employing the correct correlated positional and rotational speed pulse inputs into the motor control software, the oscillation parameters of the NACA4421 airfoils can be controlled. Table 4-4 displays the oscillation parameters used for the present experiments and its corresponding flapping frequencies, f , and reduced frequencies, κ . Flapping frequency, f is defined as the number of oscillation cycles completed in 1 second. The reduced frequency, κ is calculated based on Equation 4.8 where c is the chord and U_∞ is the freestream flow velocity. Theodorsen [50] has proven that for instability aerodynamic studies, κ is the appropriate dimensionless parameter used to characterize the degree of unsteadiness of the flow. With the chord of the NACA4421 airfoil kept constant at 0.075m and U_∞ fixed at 0.102m/s, the only parameter that affects the resultant reduced frequency, κ for each of the pitch amplitudes would be the flapping frequency, f which is directly proportional to κ . Hence, with the relatively constant rotational speed of the oscillation, as the pitch amplitude decreases, this will result in the increase of both f and κ as shown in Table 4-4.

Table 4-5 : Reduced Frequency Classification of Flow Unsteadiness [110]

$\kappa = 0$	Steady State Aerodynamics
$0 < \kappa \leq 0.05$	Quasi-Steady State Aerodynamics
$\kappa > 0.05$	Unsteady Aerodynamics

Leishman [110] in his publication on “Principles of Helicopter Aerodynamics” classified the degree of flow unsteadiness by the reduced frequency, κ as shown in the Table 4-5. Based on this classification of flow unsteadiness, it demonstrates that the oscillations carried out in the present study falls under the ‘unsteady aerodynamics’ regime as all of the calculated reduced frequencies, κ in Table 4-4 are more than 0.05.

4.3.2 Tandem Airfoils Configuration Setup

Figure 4-16 : In-Phase ($\phi = 0^\circ$) Tandem Pitching Mechanism

The tandem airfoils configuration experimental arrangement essentially enables the two airfoils to oscillate or pitch at various pitch angles (static) or pitch amplitudes (dynamic) and to allow the airfoils to be configured at various stagger distances, S_d and gap distances, G . The setup consists of an Aluminum base plate where a Cool Muscle CM1 Servomotor and two sets of force transducers and belt idlers are mounted on. The base plate has slots and a slider mechanism to allow the adjustment of S_d and G respectively. This base plate is mounted onto an aluminum support fixture that is secured onto the laboratory floor and walls to ensure that external vibrations are minimized. The tandem airfoils are installed vertically in the test section of the tunnel using connectors onto the belt idlers. These belt idlers are cylindrical roller bearings that ensure minimal frictional force during oscillation and provide the necessary linkage between the airfoils and force transducers.

For the in-phase, $\phi = 0^\circ$ tandem pitching mechanism shown in Figure 4-16, the two tandem airfoils oscillates in synchronisation with the aid of two flat rubber belts that are being driven by the servomotor driving shaft powered by the Cool Muscle CM1 servomotor. The pair of flat belts wrapped around the belt idlers and the driving shaft belt pulley that is fixed at the end of the main motor driving shaft. When one belt is installed, each airfoil will experience a tensile force due to the pull of the belt thus there is a risk of causing a slight deflection in the span direction. Hence, to mitigate this problem, the selected belts are identical and of the same age. Assuming, no degradation of elastic and mechanical properties, with the belts exerting the same tensile resultant forces in the opposite direction to one another, the forces will cancel out thus not affecting the span-wise alignment of the tandem airfoils.

Figure 4-17 : Out-of-Phase, $\phi = 180^\circ$ Tandem Pitching Mechanism

The out-of-phase, $\phi = 180^\circ$ tandem pitching mechanism shown in Figure 4-17 will enable the tandem airfoils to oscillate opposite to one another through the utilization of a gear driving system for the downstream airfoil and retaining the belt drive for the upstream airfoil. The gears are made of MC Nylon© which is commonly used thermoplastic to manufacture gears due to its excellent sliding properties, abrasive wear resistance and strong impact resistance, thus an ideal material choice for our gear system [111].

Figure 4-18 : Out-of-Phase Pitching Mechanism Force Vector Analysis

In order for $\Sigma F_x = 0$ (in equilibrium),

$$\Sigma F_x = F_1 - 2(F_2 \cos 23.8^\circ) = 0$$

$$F_2 = \frac{F_1}{2 \cos 23.8^\circ} = \underline{0.546 F_1}$$

In the Y direction,

$$\Sigma F_y = F_2 \sin 23.8^\circ - F_2 \sin 23.8^\circ$$

$\Sigma F_y = 0$ (In Equilibrium)

Figure 4-19 : Free-Body Diagram of Forces at the Main Driving Shaft

For the out-of-phase experimental arrangement, the single belt drive exerts a ‘pulling’ force, F_1 which causes the span-wise deflection of the upstream airfoil and critically, without the counteracting force of the downstream airfoil belt drive, the meshing of the gears that drives the downstream airfoil will be affected. Hence, to provide the necessary counteracting force onto the downstream airfoil, two identical elastic bands are used to provide the necessary tension, F_2 that approximately equals to $0.546 F_1$ each as shown in the Figures 4-18 and 4-19.

$$F = K_{\text{belt}} X_{\text{belt}} \quad (\text{Eq. 4.9})$$

Based on the free body diagram of the force vectors at the main driving shaft in Figure 4-19, it is ascertained that in order for F_1 to be cancelled out, F_2 which is the resultant tension force produced by each of the elastic bands would need to be approximately 0.546 times of F_1 . Both F_1 and F_2 can be calculated based on Hooke's Law as shown in Equation 4.9 where K_{belt} is the stiffness constant for the flat belt/elastic band and X_{belt} is the distance where the flat belt/elastic band elongates. With F_2 known, the dimension and the stiffness of the elastic bands required to provide the necessary counteracting forces to F_1 can be determined.

With all these necessary measures in place, the meshing of the gears for the downstream airfoil and the span-wise alignment of the two NACA4421 airfoils remain unaffected. However, these measures employed do not eliminate all the external disturbances or noise during oscillations, but it ensures that the both airfoils are aligned in the span-wise direction and that the power transmission via the gears for the out-of-phase motion is smooth and the vibrations induced through the oscillatory motion are minimized.

Despite these measures, there will still be the effects of inertia and also dynamic errors induced by the reciprocating motion of the elastic belts which can be very difficult to quantify during the oscillations. Hence, the force measurement data processing methods that will be introduced in the next section will further minimize these external disturbances and inertial effects through the force averaging method and noise filtering processing method.

4.3.3 Experimental Cases

Table 4-6 : Static Experimental Cases

Experimental Parameters	Single Static Case	In-Phase, $\phi = 0^\circ$ Static Case		Out-of-Phase, $\phi = 180^\circ$ Static Case	
	Single Airfoil	Upstream Airfoil	Downstream Airfoil	Upstream Airfoil	Downstream Airfoil
Pitch Angles, $\pm\alpha$ ($^\circ$)	+10	± 10		± 10	-/+ 10
	+15	± 15		± 15	-/+ 15
	+20	± 20		± 20	-/+ 20
	+30	± 30		± 30	-/+ 30
	+40	± 40		± 40	-/+ 40
	+45	± 45		± 45	-/+ 45
	+60	± 60		± 60	-/+ 60
Stagger Distance, Sd	N.A	2 & 2.5		2	
Gap, G	N.A	0		0	

Table 4-7 : Oscillating Experimental Cases

Experimental Parameters	Baseline Oscillating Case	In-Phase, $\phi = 0^\circ$ Tandem Case		Out-of-Phase, $\phi = 180^\circ$ Tandem Case	
	Single Airfoil	Upstream Airfoil	Downstream Airfoil	Upstream Airfoil	Downstream Airfoil
Pitch Amplitude, $\pm\alpha$ ($^\circ$)	10	10		10	
	15	15		15	
	20	20		20	
	30	30		30	
	40	40		40	
	45	45		45	
	60	60		60	
Stagger Distance, Sd	N.A	2 & 2.5		2 & 2.5	
Gap, G	N.A	0, 0.133, -0.133, +0.266, -0.266		0	

For both the static and oscillating experimental cases shown in Tables 4-6 and 4-7, single airfoil cases are carried out first to demonstrate the force and PIV measurements' capabilities before progressing to the tandem cases. The out-of-phase cases meant that the downstream airfoil is 180° out of phase in relation to its upstream counterpart. Primarily, the present study will evaluate the effect of axial spacings by varying the Sd between 2 and 2.5. Both static and oscillating cases are tested at pitch angles/amplitudes that ranges from 10° to 60° , thus effectively covering pre, during and post static stall conditions similar to Lee and Gerontakos [60] experimental work on oscillating airfoil.

4.4 FORCE MEASUREMENT EXPERIMENTAL SETUP

Figure 4-20 : Overview of the Force Measurement Experimental Setup [112]

Table 4-8 : Force Measurement Equipments

Force Measurements Experimental Setup		
No.	Description	Qty
1	ATI 9105 TW Gamma IP65 Force Transducer	1
2	ATI 9105 TW Gamma Force Transducer	1
3	Force Transducer Connector Cable	2
4	Interface DAQ Power Supply Box	2
5	12 Volt Wall Mount Power Supply	2
6	DAQ Card Connector Cable (68-Pins Connector)	2
7	RTSI Bus Cable (DAQ Cards Connector Cable) – 2 Boards	1
8	NI PCI Based DAQ Card (160bits and 16 Analog Inputs)	2
9	Desktop running LabView™ DAQ Program	1

The force and torque measurement experimental arrangement for the present study is illustrated in Figure 4-20. The key components are numbered with its corresponding descriptions and quantities listed in Table 4-8. This arrangement shown in Figure 4-20 consists of two distinct series of connections specifically for the two force transducers used to measure the instantaneous force and torque applied to the upstream and downstream NACA4421 airfoils. Each series of connection comprises of the ATI 6-axis Gamma force transducer (1 and 2) which is connected to Interface Power Supply box (4) via the force transducer connector cable (3). The primary role of the Interface power supply box is to

provide the precision power for the force transducer and to provide the high level voltages to transfer the strain gauge data or ‘composite loading information’ to the Data Acquisition (DAQ) hardware [121]. This Interface power supply box has its own power supply via the 12V Wall Mount Adaptor (5) and it is subsequently connected using the DAQ card connector cable (6) to the PCI based Data Acquisition (DAQ) card (8). This DAQ card is installed in the desktop’s CPU (9) and forms the “Data Acquisition Hardware” where its primary role is to convert the analog voltages or ‘strain gauge data’ from the force transducers to useful force and torque values [121]. With research budget constraints, the present study was unable to acquire the multi-sensor interface box which will enable the synchronized operation of two force transducers. Hence, with given circumstances, the two force transducers are connected and synchronized by acquiring a RTSI bus cable (7) and programming a customized data acquisition program using LabView™ (9). The RTSI cable is used to connect the two internal PCI based DAQ cards (8) together in the CPU (9) where it connects the timing and allow the synchronization of the two force transducers’ signals [112].

Figure 4-21: LabView™ DAQ Program for Synchronized Force Transducers Operation

Due to the need to synchronize the operation of these transducers, the default ATI DAQ Software cannot be used as it is designed for a single transducer operation. Hence, the DAQ software was customized and programmed using LabView™ which functions to facilitate the synchronized conversion of the force transducers’ composite loading information (strain gauge data) to its force and torque values by using the respective transducers’ calibration files that were provided by the supplier. The LabView™ program also created an interface to

display, record and save the real-time force and torque measurements from the two force transducers simultaneously as shown in Figure 4-21. Importantly, it also allows the ‘biasing’ of the measurements before the start of any measurements to eliminate residual external disturbances.

4.4.1 Force Transducers Overview and Operation

Figure 4-22 : (a) 6-Axis ATI 9105 TW Gamma Force Transducer for the Upstream Airfoil [112]; (b) 6-Axis ATI 9105 TW Gamma IP65 Force Transducer for the Downstream Airfoil [112]; (c) Applied Force and Torque Vectors [112] and (d) Orientation of the Main (x, y and z) Axes

Table 4-9 : ATI Gamma Force Transducer Sensing Ranges and Resolution [112]

Sensor	(SI) Metric Calibration	Fx,Fy (N)	Fz ³ (N)	Tx,Ty (Nm)	Tz (Nm)	Fx,Fy (N)	Fz ³ (N)	Tx,Ty (Nm)	Tz (Nm)
Gamma	SI-32-2.5	32	100	2.5	2.5	1/160	1/80	1/2000	1/2000
		Sensing Ranges				Resolution (DAQ, Net F/T) ⁴			

Notes:

1. These system resolutions quoted are the effective resolution after dropping four counts of noise.
The effective resolution can be improved with filtering.
2. Applied loads must be within range in each of the six axes for the F/T sensor to measure correctly.
3. For IP68 version see caution on physical properties page.
4. DAQ resolutions are typical for a 16-bit data acquisition system.

Table 4-10 : ATI Gamma Force Transducer Physical Specifications [121]

Single-Axis Overload	ATI 9105 TW Gamma Force Transducer	ATI 9105 TW IP65 Gamma Force Transducer
Fxy	±1200 N	±1200 N
Fz	±4100 N	±4100 N
Txy	±79 Nm	±79 Nm
Tz	±82 Nm	±82 Nm
Stiffness (Calculated)		
X-axis & Y-axis forces (Kx, Ky)	9.1×10 ⁶ N/m	9.1×10 ⁶ N/m
Z-axis force (Kz)	1.8×10 ⁷ N/m	1.8×10 ⁷ N/m
X-axis & Y-axis torque (Ktx, Kty)	1.1×10 ⁴ Nm/rad	1.1×10 ⁴ Nm/rad
Z-axis torque (Ktz)	1.6×10 ⁴ Nm/rad	1.6×10 ⁴ Nm/rad
Resonant Frequency		
Fx, Fy, Tz	1400 Hz	1000 Hz
Fz, Tx, Ty	2000 Hz	970 Hz
Physical Specifications		
Weight ¹	0.255 kg	1.09 kg
Diameter ¹	75.4 mm	111 mm
Height ¹	33.3 mm	52.3 mm
Note: 1. Specifications include standard interface plates.		

A force transducer or commonly known as a load cell is defined as a compact, rugged and monolithic structure that converts the applied forces and torques into analog strain gauge signals [112]. For the present study, two 6-axis ATI 9105 TW Gamma force transducers are used to measure the applied instantaneous force and torque during static and dynamic experiments for the upstream and downstream airfoils as shown in Figure 4-22. These two transducers have identical sensing ranges and resolutions which are presented in Table 4-9. However, one major difference is that the force transducer for the downstream airfoil is an IP65 model which meant that it is certified as water-spray proof, thus physically, it is a larger model with a lower resonant frequency as indicated in Table 4-10. The flapping frequencies, f , for the NACA4421 airfoils in the present study are in the range of 10¹ Hz which is lower than the specified resonant frequencies for both transducers. This meant that, with the present experimental oscillating parameters, resonance effects are deemed to be negligible.

4.4.2 Force Measurement Error Analysis

In the present study, the sources of errors are determined to originate from three potential sources which are (1) Water Tunnel, (2) Tandem Oscillating Mechanism and (3) Force Transducers. As dynamics errors from the tandem oscillating mechanism are hard to quantify, numerous measures presented earlier have been incorporated to minimize span-wise deflection of the airfoils and vibration effects induced by the out-of-phase oscillating mechanism. During the oscillations, it is understood that some of these vibration induced by the mechanism and the inertial effects of the airfoil pitching motion do have an effect on the

force and torque measurements. These dynamic errors and external disturbances or ‘noise’ will be minimized further through a series of force data processing methods that will be elaborated in the next section.

In this section, the focus is to quantify the measurement errors stemmed from the force transducers. In order to ensure the accuracy of the measurements and to account for the measurement errors, dead weight calibration tests were carried out in the X-axis (parallel to the water flow direction) and in the Z-axis. For X-axis calibration, the dead weights tested ranged from 10g to 100g and for the Z-axis calibration, the dead weights ranged from 10g to 500g. Force measurements were taken at a high sampling rate of 50 Hz for a total number of 1000 data points.

Figure 4-23 : Dead Weight Calibration Setups; (a) in the Z-Axis and (b) in the X-Axis

The dead weight test in the Z-axis consists of placing the transducer on a table with the tooling adaptor plate facing up and positioning each dead weight at the center of the transducer as shown in Figure 4-23 (a). The applied force onto the force transducer will be acting in the $-Z$ direction. For the dead weight testing in the X-axis, the force transducer was mounted using a clamping tool with the $+X$ -axis direction pointing downwards as shown in Figure 4-23 (b). The dead weights are then installed onto a customized metallic hanger. Prior to installing the dead weights, the combined weight of the screw, hanger and also the exerted clamping force were “biased” to ensure that only the dead weights were measured.

Table 4-11 : Dead Weight Calibration Results in Z-Axis; (a) Front Force Transducer and (b) Rear Force Transducer

Front Force Transducer - Dead Weight Calibration Test in Z-Axis					
Tested Dead Weights (g)	10g	20g	100g	200g	500g
Theoretical Weight	0.0981	0.1961	0.98054	1.96105	4.90261
Mean (N)	0.0981	0.1960	0.9753	1.9598	4.8375
Max (N)	0.1023	0.1976	0.9900	1.9756	-4.8946
Min (N)	0.0949	0.1944	0.9605	1.9439	4.7532
Error (%)	0.007%	-0.058%	-0.535%	-0.065%	-1.329%
Error (N)	0.0000	-0.0001	-0.0052	-0.0013	-0.0651
Standard Deviation	0.0029	0.0023	0.0209	0.0224	0.0695

(a)

Rear Force Transducer - Dead Weight Calibration Test in Z-Axis					
Tested Dead Weights (g)	10g	20g	100g	200g	500g
Theoretical Weight	0.0981	0.1961	0.98054	1.96105	4.90261
Mean (N)	0.0975	0.1966	0.9813	1.9623	4.8993
Max (N)	0.1034	0.2028	0.9979	1.9784	4.9124
Min (N)	0.0910	0.1902	0.9583	1.9407	4.8906
Error (%)	-0.596%	0.261%	0.076%	0.062%	-0.068%
Error (N)	-0.0006	0.0005	0.0007	0.0012	-0.0033
Standard Deviation	0.0054	0.0060	0.0163	0.0136	0.0116

(b)

Figure 4-24 : Dead Weight Calibration Test - Mean Measured Weights in Z-Axis

Figure 4-25 : Dead Weight Calibration Test in Z-Axis - Measurement Errors %

Table 4-12 : Dead Weight Calibration Results in X-Axis; (a) Front Force Transducer and (b) Rear Force Transducer

Front Force Transducer - Dead Weight Calibration Test in X-Axis										
Weight (g)	10	20	30	40	50	60	70	80	90	100
Theoretical Weight (N)	0.0981	0.1962	0.2943	0.3924	0.4905	0.5886	0.6867	0.7848	0.8829	0.9810
Mean (N)	0.0951	0.1934	0.2816	0.3861	0.4725	0.5714	0.6597	0.7624	0.8567	0.9573
Min (N)	0.0947	0.1866	0.2723	0.3814	0.4875	0.5625	0.6523	0.7519	0.8455	0.9436
Max (N)	0.0986	0.1983	0.2910	0.3907	0.4937	0.5824	0.6683	0.7735	0.8688	0.9610
Error (%)	-3.058%	-1.427%	-4.315%	-1.606%	-3.670%	-2.922%	-3.932%	-2.854%	-2.967%	-2.416%
Error (N)	-0.0030	-0.0028	-0.0127	-0.0063	-0.0180	-0.0172	-0.0270	-0.0224	-0.0262	-0.0237
Standard Deviation	0.0012	0.0011	0.0010	0.0012	0.0007	0.0006	0.0006	0.0006	0.0023	0.0006

(a)

Rear Force Transducer - Dead Weight Calibration Test in X-Axis										
Weight (g)	10	20	30	40	50	60	70	80	90	100
Theoretical Weight (N)	0.0981	0.1962	0.2943	0.3924	0.4905	0.5886	0.6867	0.7848	0.8829	0.9810
Mean (N)	0.0956	0.1947	0.2877	0.3756	0.4786	0.5768	0.6630	0.7627	0.8610	0.9634
Min (N)	0.0913	0.1934	0.2737	0.3712	0.4713	0.5659	0.6551	0.7534	0.8571	0.9536
Max (N)	0.0982	0.1961	0.2964	0.3847	0.4975	0.5861	0.6762	0.7767	0.8687	0.9762
Error (%)	-2.548%	-0.765%	-2.243%	-4.281%	-2.426%	-2.005%	-3.451%	-2.816%	-2.480%	-1.794%
Error (N)	-0.0025	-0.0015	-0.0066	-0.0168	-0.0119	-0.0118	-0.0237	-0.0221	-0.0219	-0.0176
Standard Deviation	0.0028	0.0011	0.0094	0.0056	0.0110	0.0083	0.0087	0.0096	0.0048	0.0093

(b)

Figure 4-26 : Dead Weight Calibration Test –Mean Measured Weights in X-Axis

Figure 4-27 : Dead Weight Calibration Test in X-Axis - Measurement Errors %

As presented in Figures 4-24 and 4-25, the dead weight calibration that was carried out in the Z-axis which is aligned to the pitching axis show that both the front and rear force transducers exhibits very consistent results that agree well with the entire range of dead weights tested. Table 4-11 shows that the front transducer produces a higher maximum measurement error of 1.33% when comparing to the rear transducer that exhibits a lower measurement error of 0.6%.

The dead weight calibration that was performed in the X-axis is more critical than in the Z-axis as drag forces act in the X-direction. Based on the airfoil size and the low Reynolds number flow at $Re = 8.6 \times 10^3$, the dead weights were chosen to be in the range of 10g to 100g in order to represent the range of forces that the present experiments will most likely to produce. The dead weight calibration test results shown in Figures 4-26 and 4-27 demonstrate that both transducers consistently measure marginally lower values than the theoretical weight values with the front transducer and rear transducer measuring an average of 2.92% and 2.48% lower than its theoretical benchmarks respectively. Importantly, both transducers exhibit maximum measurement errors of about 4.3% as shown in Figure 4-27. It is also imperative to point out that the measurement errors peaked in the region of 30 g to 70 g which equates to theoretical weights of 0.2943N to 0.6267N. Due to the monolithic and symmetrical beams construction of these transducers, the present study shall assumed that the measurement errors in the X-axis and Y-axis to be the same. Hence to be conservative, all the measured forces in the X and Y directions will have to take account 5% in measurement error and that forces measured within the 0.2943N to 0.6267N range would be more prone to the measurement errors inherent to these transducers.

$$U_c^2 = U_s^2 + s_i^2 \quad (\text{Eq. 4.10})$$

As mentioned earlier, there are three potential sources of measurements errors; the water tunnel, the tandem oscillating mechanism and the force transducers. Based on Hugh and Glenn [113] publication on experimental uncertainty analysis, it is understood that there are two distinct types of errors that can contribute to the overall or combined measurement uncertainty denoted as U_c which are (1) Bias or Systematic Error, U_s and (2) Random or Precision Error that can be represented as the standard deviation, s_i for variable, i , as shown in Equation 4.10. The Bias or Systematic Error, U_s is defined as the difference between the mean measured values to the true or theoretical values. In the present error analysis, this systematic error would be the measurement error obtained earlier through the dead weight calibration test. The Random Errors are errors in the measurements that cause the measurable values of a certain attribute or variable to be inconsistent when repeated measurements are taken. The term ‘random’ implies the challenge in predicting the subsequent measured values though random errors can be reduced by averaging multiple measurements [113]. In the present context, statistically, the random errors can be represented as a normal distribution where the standard deviations in the measured values are taken as the random error component in Equation 4.10 [114].

Table 4-13: Force Measurement Error Analysis

Force Measurement Error Analysis		Max. Systematic Error, U_s (%)	Max. Random Error, S_i	Max. Combined Uncertainty, U_c (%)	Water Tunnel Error (%)	Oscillating Mechanism Measurement Error (%)	Total Force Measurement Error (%)
In the Z-Axis	Front Force Transducer	1.33	0.0695	1.3318	5	0.065	6.397
	Rear Force Transducer	0.6	0.0163	0.6002	5	0.065	5.665
In the X-Axis	Front Force Transducer	4.315	0.0023	4.3150	5	0.065	9.380
	Rear Force Transducer	4.281	0.011	4.2810	5	0.065	9.346

Taking the maximum measurement error % and standard deviation values highlighted in yellow in Tables 4-11 and 4-12 from the dead weight calibration tests, the combined uncertainty for the front and rear transducers in the Z-axis and X-axis directions can be determined. In addition, the water tunnel's errors and the oscillating mechanism measurement errors are accounted for in the calculation of the total force measurement errors. The total force measurements errors are the summation of the combined uncertainty, U_c , the water tunnel error and the oscillating mechanism error. Based on the CTA measurements that were carried out, the water tunnel error is taken as 5% and the oscillating mechanism error to be 0.065%. Table 4-13 shows the breakdown of the respective total force measurement errors for both force transducers. To be conservative, force measurements in the Z-axis and X-axis have a total measurement error of 7% and 10% respectively. It is assumed that the total measurement error for the Y-axis is taken to be 10%.

4.5 FORCE MEASUREMENT PROCESSING METHODS

Figure 4-28 : Force Measurement Processing Methods Flow Chart

The previous section has comprehensively determined the approximated total measurement errors based on the various calibration tests carried out. However, as the present study is predominantly investigating on unsteady and dynamic experimental cases on oscillating airfoils, the errors induced by the oscillating mechanisms must be taken account as well. The present study formulated an extensive series of force measurement processing methods as presented in Figure 4-28. These processing methods aim to produce instantaneous force measurement plots that are filtered from these dynamic errors or ‘noise’ as well as phase-averaging these instantaneous force plots to minimize the random errors associated with these repeated sets of force measurements.

In the present study, all of the force measurements for the oscillating cases are carried out in 10 oscillating cycles. Prior to recording these force measurements, the water tunnel pump speed is adjusted to 30Hz which corresponds to a flow velocity of 0.102m/s, the water flow is then stabilized for 2 mins and the residual forces are ‘biased’ via the LabView software. These measures taken are necessary to further minimize measurement errors.

In summary, the force measurement processing methods consist of three main processes; (1) Force Averaging Process; (2) Noise Filtering Process and (3) Phase Averaging Instantaneous

Force Plot Process that will be elaborated in the subsequent sections. The force averaging process produces the ‘averaged force’ plots and ‘averaged noise’ plots based on the ‘peak values alignment’ concept from the raw data collected. The noise filtering process allows the alignment and subtraction of the ‘averaged force’ and ‘averaged noise’ plots to produce a ‘filtered averaged force plot’ for 10 oscillation cycles. Following that, the phase-averaging process will ensure that the 10 oscillation cycles are ‘sliced’ correctly and help align and average these ‘sliced’ instantaneous force plots to reduce random measurement errors. In the present study, ‘instantaneous’ force plots can also be termed as ‘force time histories’ or simply the forces measured in one oscillation cycle. Lastly, the finalized instantaneous force measurement plots can be represented in its dimensionless force coefficients using Equations 3.43 and 3.44 presented in the previous chapter.

4.5.1 Force Averaging Method

The force averaging method is the first processing method to be carried out after acquiring the raw force measurements from the experiments and is applied to only the oscillating cases in order to obtain the ‘averaged force’ and ‘averaged noise’ plots. For static cases, for one measurement set, forces are measured for a period of 30 seconds which correspond to 1500 measurement data points. In order to reduce measurement random errors, for each static experimental case, 3 to 4 measurement sets are carried out and all of the measured data will then be averaged out.

Figure 4-29 : Concept of Identification of Common Peaks in the Force Averaging Method

On the other hand, for each of the oscillating cases, 3 to 4 measurements sets are carried out where one measurement set consists of 10 oscillation cycles to produce the raw force plots as shown in Figure 4-29. These raw force plots from the repeated measurement sets exhibit some variations due to random errors for e.g. minor variation in the starting angle of incidence of the airfoil or any other unpredictable external disturbances that might produce a random ‘spike’ in the raw force measurements.

Table 4-14 : Peak Values Alignment via Data Manipulation

Y1	Y2	Y3	Y Average
0.682	0.743	0.749	0.73675
0.767	0.712	0.787	0.75825
0.733	0.736	0.747	0.75725
0.719	0.762	0.781	0.769
0.853	0.803	0.811	0.827
0.768	0.795	0.795	0.792
0.851	0.797	0.79	0.79775
0.77	0.766	0.771	0.7655
0.768	0.745	0.747	0.74975

Alignment of
Peak Values

Figure 4-30: Resultant Averaged F_y Plot after Peak Values Alignment

This force averaging method is based on identifying common peak values in the respective raw force plots from the various measurement sets as circled in red in Figure 4-29. After these peak values are identified, they are then aligned through identifying the relevant numerical data. This data processing step comprises of shifting the respective numerical data columns to align the respective peak values as shown in Table 4-14. After the alignment of

these peak values, all of the raw force plots are then averaged out to produce the ‘Y average’ column in Table 4-14 which is plotted to generate the ‘averaged force’ plot as illustrated in Figure 4-30.

As mentioned earlier, one of the primary objective for this force processing methods is to minimize the ‘dynamic errors’ induced by the oscillating mechanism. For the in-phase oscillating mechanism, it consists of two flat belts pulling in alternating directions to drive the oscillating motion. Due to the arrangement of these flat belts, the alternating pulling action during oscillation produces greater forces in the X-direction that must be taken account in the present study; these forces are termed as ‘noise’. These ‘noise’ forces are measured with reference to the same corresponding pitching parameters as the experimental pitching cases presented in Tables 4-7 but without the installation of the NACA4421 airfoils.

Figure 4-31 : Noise Force Measurements in X-Direction used for Peak Values Alignment

As demonstrated earlier, the common peak values were identified using the raw force data plots in the y-direction (the lift force plot) for the averaged force plot, however, for the ‘noise’ measured, the common peak values are ascertained by analyzing forces measured in the X-direction instead. Through analyzing the raw data, it was observed that the peaks in the ‘noise’ measured in the X-direction are more clear and distinct due to the orientation of the flat belts during oscillation as explained earlier. These distinct peaks are important in providing a more accurate identification of the peak values. Similar to the averaging of the

force measurement plots, these peak values are then aligned and averaged out to produce the ‘averaged noise’ plot as shown in Figure 4-31.

Figure 4-32 : Noise Force Measurements in X-Direction from Front and Rear Load Cells utilizing Belt Drive and Gear Drive Mechanisms respectively

For the out-of-phase oscillating mechanism, the downstream airfoil is oscillated 180 degrees out-of-phase in relation to its upstream counterpart using a geared drive system. Similar to the in-phase oscillating mechanism, the ‘noise’ forces induced by the geared drive system for the out-of-phase oscillating mechanism are measured without the installation of the NACA4421 airfoils. For the out-of-phase oscillating mechanism, the noise forces measured in the X-direction produced clearer and more distinct peaks throughout the oscillation cycles as compared to the noise forces in the Y-direction. This is similar to the in-phase belt drive oscillating mechanism. Applying the same force averaging concept to the raw noise force measurements, the average noise force plot in X-direction can be produced as shown in Figure 4-32. As a comparison, the average noise force plot for the in-phase oscillating mechanism (blue colored plot) is also presented in Figure 4-32. It is clearly observed that the out-of-phase gear drive oscillating mechanism produced significantly higher noise force peak values than the in-phase belt drive oscillating mechanism. These noise force peaks coincide with the instantaneous times when the oscillation cycle changes its direction hence, these peaks may be attributed to the vibration induced due to the rapid change in direction of the gear drive. In addition, the higher average noise forces produced by the gear drive when

compared to the belt drive may also be contributed to vibration induced by the gear meshing during operation. To further minimize the vibration induced by the gear drive system, the gears are installed with great care and precision to ensure that the gears are meshed correctly and the movement of the two gears are smooth. Importantly, through capturing the ‘noise’ forces induced by both the in-phase and out-of-phase oscillating mechanisms, the present study is able to address, quantify and filter these undesirable external disturbances or ‘dynamic errors’ which will be elaborated in the next section.

4.5.2 Noise Filtering Method

The matching of the averaged force plots and the averaged noise plots proved to be tricky. The challenge would be to identify the starting points for both plots. Hence, the present study had to make some assumptions in order to match these plots as accurately as possible. For the averaged force plots, the starting point is ascertained by analyzing the lift force (y-direction). With the residual forces eliminated through the ‘biasing’ of the force transducers prior to recording these force measurements, the starting point is taken when the lift force, F_y is approximately zero at the beginning of the first oscillating cycle.

On the other hand, for the average noise plot, the starting point is taken from the noise force plot in the x-direction as shown in Figure 4-31 as the forces measured in the x-direction produced a distinct representation of the oscillation cycles. The starting point for the X-direction average noise plot is also assumed to be approximately zero due to the force measurement ‘biasing’ as well.

Figure 4-33 : (a) Filtered Average F_y Plot and (b) Filtered Average F_x Plot

These identified starting points are then applied to all the other forces and moments measured in the x, y and z directions as well. With both starting points identified for the average force

plots and average noise plots, the two sets of numerical data from both plots can be aligned based on their respective identified starting points. These aligned average force and average noise plots are shown in Figures 4-33 (a) and (b) for the y and x directions respectively. Once the alignment between the average force and average noise is attained, the two plots are subtracted to produce the ‘filtered averaged’ force plots for 10 cycles as represented by the green line in Figures 4-33 (a) and (b), thus demonstrating this noise filtering process that is applicable to both in-phase and out-of-phase oscillating mechanisms.

4.5.3 Phase-Averaging Instantaneous Force Measurement Plots

Figure 4-34 : Average Lift Force, F_y computed for Starting Point at $t/T = 0$

The problem with the ‘filtered averaged’ force plot for 10 cycles is that it is difficult for the present study to correlate with the processed vorticity contours for a comprehensive analysis. Hence, there is a need to ‘slice’ the ‘filtered averaged force’ plot into its individual force plots for one oscillation cycle. This ‘sliced’ individual force plot is also known as the ‘instantaneous force plot’ or force time histories. The challenge in ‘slicing’ the 10 cycles would be determining the location to slice these cycles. In order to determine its ‘slicing location’ at the start of each cycle, the present study assumed that the lift force, F_y at $\alpha \approx 0^\circ$ at the beginning of each cycle does not equal to zero due to the asymmetrical NACA4421 airfoil profile generating a small lift force with $U_\infty \neq 0\text{m/s}$. Therefore, the ‘slicing locations’ are ascertained by averaging the first few measurement points captured by the raw lift force, F_y plots for each measurement set taken as highlighted in red in Figure 4-34. These first few points represent the lift forces measured when the airfoil is approximately at $\alpha \approx 0^\circ$ before it starts oscillating. The average F_y value of these first few points determines the approximated starting point or ‘slicing location’ for these instantaneous force plots as shown in Figure 4-34.

Figure 4-35 : Instantaneous Force Measurement Plot Slicing Step

In order to visualize this concept, Figure 4-35 illustrates an example of a ‘filtered averaged force’ plot over 10 cycles where the computed starting point or ‘slicing location’ is represented as a horizontal blue line drawn at $y = -0.095\text{N}$ which is the average value of the first few data points calculated in Figure 4-34. The starting points for each of the ‘sliced’ instantaneous force plots are the intersections of the blue line and the filtered average force plot as represented by the blue dots. The distance between each successive blue dot equates to the cyclic period, T . Red dotted vertical lines are drawn in Figure 4-35 to clearly visualize the ‘slicing’ of the ‘filtered averaged force’ plot into its instantaneous force plots.

Figure 4-36 : Sliced and Aligned Instantaneous (a) F_y Plots and (b) F_x Plots

Subsequently, these ‘sliced’ plots are averaged out using the force averaging concept presented earlier in Section 4.5.1 via aligning the common peak values to produce a ‘phase-averaged’ instantaneous force plot as shown in Figure 4-36. For the present study, the first two oscillation cycles will not be taken in generating the ‘phase-averaged’ instantaneous force plot in order to eliminate the transient effects during the force measurements.

4.6 PARTICLE IMAGE VELOCIMETRY (PIV) SYSTEM

Figure 4-37 : Schematic Diagram of the PIV System Sequences [115]

Particle Image Velocimetry (PIV) system is basically a non-intrusive, whole flow field technique using ‘photographic’ technology to capture and obtain real-time mapping of the velocity flow field. Firstly, the seeded flow is illuminated in a selected target area with a pulsating laser sheet (Nd: YAG) projected via a system of optical components that comprises of mirrors and a diverging lens as illustrated in Figure 4-37. With the illumination of two short duration laser pulses in the measurement target area, the CCD camera is able to capture each laser pulse in separate image frames as shown in Figure 4-37. These image frames of the flow field are then divided into small subsection called the interrogation areas and these interrogation areas from the two images frames, I_1 and I_2 are then cross-correlated pixel by pixel via the Dantec Dynamic Studio V1.45 software. Through this cross-correlation for each of these interrogation areas, it produces a signal peak and an average particle displacement vector, ΔX . By repeating this correlation for all the interrogation areas, an average displacement vector map is produced which can then be divided by the known pulse time interval, Δt in order to convert to a velocity vector map. Subsequently, using the velocity vector numerical output files which consist of its U and V velocity components in the X and Y direction respectively, the vorticity contours for the flow field can be generated using the central differencing scheme presented in Section 3.1.2.

4.6.1 PIV System Setup

Figure 4-38 : PIV System Setup and Equipments; (a) Support Frame; (b) Laser Optical System Setup; (c) Adjustable Joints for Optical Components; (d) Polyamide Seeding Particles; (e) CCD Camera Placement at Test Section Base and (f) CCD Camera with 60mm Nikon Micro Nikkor Lens

The 3-D model of the support frame shown in Figure 4-38 (a) is constructed from an assembly of Aluminum 6061 square profile extrusions using cast iron connectors. The primary design of the support frame is to support the weight of the laser generator on the aluminum base plate and also to accommodate the projection of the laser sheet across the test section of the water tunnel in two orientation; horizontally or vertically. The support frame shown in Figure 4-38 (b) and the NACA4421 airfoils are anodized black as to prevent any scattered reflection of the laser beam that might harm the user when the laser system is in operation.

The PIV laser system in Figure 4-38 (b) used for the present PIV measurements is the Nano T 135-15 PIV system from Litron Lasers Systems. Essentially, this PIV laser system generates a Nd: YAG laser beam measuring 5mm in diameter which can be transmitted as a continuous or pulsed beam with a max output energy of 135mJ per laser head at 532 nm. The Nano T system was chosen as it has been designed to incorporate stable telescopic resonators which make it capable of generating very low divergence output beams that allows thinner light sheets to be projected across the airfoils' cross section as compared to other Nano laser models with convectional stable resonators [116].

Figure 4-39 : Schematic of the PIV Experimental Setup

The Nd:YAG laser beam will be deflected upwards towards the diverging lens using an optical mirror. Importantly, the diverging lens will project a horizontal laser sheet across the test section of the water tunnel as illustrated in Figure 4-39. Both the optical mirror and diverging lens are mounted onto the PIV support frame via fabricated adjustable joints to allow the adjustment of their respective position as shown in Figures 4-38 (b) and (c). These positional adjustments will ensure that the horizontal laser sheet is projected at the targeted flow field which is set at the mid-section of the airfoil's span width as illustrated in Figure 4-39. The CCD camera which is equipped with a 60mm Nikon Nikkor lens is positioned at the base of the test section to capture the illuminated flow field as shown in Figures 4-38 (e) and (f). The CCD camera in Figure 4-38 (f) has a maximum recording frequency (trigger rate) of 15Hz or 15 images per second and has an image resolution of 1600 x 1200 pixels. One big limitation of the present PIV measurement setup would be that the captured flow field can only effectively cover the flow field of one airfoil as using a camera with a shorter focal length resulted in PIV images with poor resolution. Moreover, due to space constraints at the base of the test section, the CCD camera cannot be positioned further away from the target flow field to capture both airfoils. Hence, for PIV measurements of tandem airfoils, the optical

components, project laser sheet and the CCD camera would need to be positioned and setup for the respective upstream and downstream airfoils.

The unidirectional water flow is seeded with polyamides particles measuring 20 μm in diameter as displayed in Figure 4-38 (d). These polyamides particles were carefully selected based on the flow velocity that the present experiments are operating in as well as its neutrally buoyant property in water. The optimum particle size of 20 μm will also ensure that the camera images are able to track the position and displacements of these particles thus effectively producing good image resolution.

4.6.2 Calibration Methods

Before starting any PIV measurements, through the Dantec Dynamic Studio V1.45 software, the user would need to create a new “database” which enable the user to control system parameters and stores all the captured images and its time stamp data. This software will facilitate in connecting the CCD camera and the laser generator and to ensure that the laser pulses and the camera’s triggering function are synchronized. The user would need to define the control system parameters which are as follows:

1. Time Pulse Interval, Δt
2. Number of Images Captured
3. Trigger Rate, Hz
4. Type of Acquisition Mode. (Single Frame or Double Frame)

The Dantec Dynamic Studio V1.45 software also allows the user to carry out these post processing tasks to produce the necessary velocity vector maps. The post processing tasks are as follows:

1. Visualize these captured images.
2. Process the captured images into raw-velocity vector maps.
3. Carry out ‘Manual Validation’ for these velocity vectors to remove error vectors.
4. Select a Filtering Method to produce the ‘Filtered’ velocity vector maps.
5. Save all the captured images, the filtered velocity vector maps and the corresponding numerical data.

However, before starting any PIV measurements, there are two main calibration methods that must be carried out to ensure that the captured PIV images are accurate and reliable. In this section, the two main calibration methods that will be presented are (1) Scale Factor Calibration Method and (2) Laser Sheet Alignment Method.

Figure 4-40 : Measurement Volume Schematic illustrating Scale Factor, S_f and Magnification Factor, M_f

As illustrated in Figure 4-40, the scale factor, S_f is defined as the ratio between the object dimensions and the image dimensions which also the inverse of the magnification factor, M_f [115]. It is the essential link between the real world coordinates and the local CCD camera coordinates. The Dantec Dynamic Studio software utilized this scale factor for velocity vector calculations, hence it is vital that this scale factor, S_f is calibrated and accurately defined before the start of the every PIV measurement. This scale factor is calibrated and determined by the ‘measure scale factor’ option or the ‘scale factor calibration method’. Firstly, a 15cm ruler is positioned directly in the targeted flow field at the mid-section of the span width of the airfoil using a locally fabricated tooling that ensures the ruler is mounted parallel to the flow direction and allows the depth of the ruler to be adjusted as well. The CCD camera image visualized through the Dantec Dynamic Studio software needs to be focused with all the markings on the ruler as sharp and clear as possible by adjusting the camera lens’ aperture.

Figure 4-41 : 'Measure Scale Factor' Dialogue using Dantec Dynamic Studio V1.45 Software using a 60mm Nikon Nikkor Lens

Once the camera image of the ruler is focused and clear similar to the image shown in Figure 4-41, the 'measure scale factor' dialogue is opened to enable the user to mark out two points; Point A and Point B on the ruler image. Take for example in Figure 4-41, the actual real distance between Point A and Point B is 10mm hence, once these two points are marked, the user then input the "abs. distance" as 10mm as highlighted in the red box in Figure 4-41. This "abs. distance" is also analogous to the "object dimension". The system will then correlate this "object dimension" to the pixelated "image dimension" to provide the scale factor, S_f for this PIV measurement setup.

The CCD camera is mounted on a same tripod and the same ruler and tooling are used throughout the entire span of the study. In addition, for tandem airfoil experiments, this scale factor calibration is carried out consistently for each of the airfoils as the CCD camera and the optical components need to be repositioned to project the horizontal laser sheet to effectively cover each airfoil's flow field. The scale factor, S_f for the present study is 0.133 pixels/ mm which correspond to a spatial resolution of 7.5 pixels / mm. This scale factor will be used together with its average displacement vector to compute the velocity vectors using the software. Subsequently, the measures and considerations taken in the present study to

ensure optimum resolution of the capture flow field will be presented. The second calibration method would be the ‘Laser Sheet Alignment’ method where the projected horizontal laser sheet is inspected to ensure that the two laser sheets from the two laser heads are reasonably aligned.

Figure 4-42 : (a) Laser Sheet Alignment Setup using Photographic Paper and (b) Examples of Laser Sheet Alignments [115]

A photographic paper is used in the laser sheet alignment setup as illustrated in Figure 4-42 (a) which is pasted on the glass side wall of the test section nearest to the diverging lens. Through the activation of the laser generator, two laser beams from the two laser heads will be diverged via the diverging lens and projected horizontally across the test section. If the two laser beams from the generator is misaligned considerably, the tendency to have misaligned laser sheets will be higher as shown in Figure 4-42 (b), thus the alignment method is crucial to ensure that the projected laser sheet is reasonably aligned. With the use of the photographic paper, it will filter out some of the laser intensity thus allowing the inspection of the laser sheets alignment. The two laser heads are activated sequentially, and through the inspection of the alignment, the respective laser head intensity for each laser head can be adjusted to produce the best possible laser alignment. This alignment method is also vital to ensure that the laser sheet is horizontal and parallel to the flow direction in order to cut across the airfoil cross section at mid-span section and capture the flow structures over these airfoils. Adjustments can be made by positioning the diverging lens and optical mirror to ensure that the laser sheet is horizontal and aligned as well. Realistically, since there will always be some degree of misalignment of the two laser beams, the two correlating laser sheets will not be perfectly aligned as well. However, for the present experiments, the laser beams alignment were calibrated prior the PIV measurements, hence the misalignment of the laser sheets were very good which is similarly illustrated in the leftmost picture in Figure 4-42 (b).

4.6.3 PIV Parameters

Table 4-15 : PIV Parameters for Static Cases

Cases	α (°)	Water Velocity (m/s)	Pulse Time Interval, Δt (μ s)	Trigger Rate (Hz)	No. of Images Captured	Interrogation Area (pixels)	Overlapping Criteria (%)	Average Filter	Laser Intensity (Laser 1 and Laser 2)
Single & Tandem Static	10	0.102	3000	5	40	32 x 32 (Cross-Correlation Method)	50	3 x 3	9, 8
	15								
	20								
	30								
	40								
	45								
	60								

Table 4-16 : PIV Parameters for Oscillating Cases

Cases	α (°)	Water Velocity (m/s)	Pulse Time Interval, Δt (μ s)	Trigger Rate (Hz)	No. of Images Captured	Interrogation Area (pixels)	Overlapping Criteria (%)	Average Filter	Laser Intensity (Laser 1 and Laser 2)
Single & Tandem Oscillating	10	0.102	3000	5	25	32 x 32 (Cross-Correlation Method)	50	3 x 3	9, 8
	15				40				
	20				45				
	30				70				
	40				90				
	45				100				
	60				130				

For both static and oscillating single and tandem airfoils experimental cases shown in Tables 4-15 and 4-16, the PIV parameters are similar with the exception of the number of images captured. As the static cases are not cyclic time-dependent, the present experiments set the number of images captured at 40. PIV measurement trials were carried out to evaluate the effect of the pulse time intervals at 2000 μ s, 3000 μ s and 4000 μ s in the early part of the present study and it was concluded that given the flow velocity of 0.102m/s, the pulse time interval at $\Delta t = 3000\mu$ s produced the best results with the lowest signal-to-noise ratio as it exhibited velocity vector plots with the least errors vectors and required the least post-processing and filtering. The trigger rate of the CCD camera is set at 5Hz which translates to the one image being captured in every 0.2 seconds. Each image captured has a time stamp which is important in the post processing of the vorticity contours which will be elaborated in the next section.

Figure 4-43 : Pixel and Interrogation Area Relationship in a Flow Field [115]

Another important control parameter is the interrogation area and the interrogation area is set at 32 x 32 pixels for the present study. Interrogation area denotes the size of the “cells” that the captured flow field is divided to as illustrated in Figure 4-43. An interrogation area of 32 x 32 meant that each of the interrogation area measures 32 pixels by 32 pixels. The specification of this interrogation area will affect the overall spatial resolution which is defined as the smallest velocity structure that can be measured. Each of these interrogation areas will encompass and track down a number of seeded particles and correlate the two images captured within the pulse time intervals to provide one average displacement vector for that particular interrogation area. Hence, with the average displacement vector determined and dividing with the know pulse time interval, Δt , the velocity vector for that particular interrogation area is ascertained. With the CCD camera specified to have a captured flow field measuring 1600 pixels by 1200 pixels, interrogation areas set at 32 by 32 pixels will translate to a velocity vector map consisting of 50 by 37.5 vectors and a spatial resolution of 7.5 pixels /mm as determined earlier via the ‘scale factor calibration’.

Another parameter to consider would be the correlation method, which defines the method where the pixels in each of the interrogation area are correlated between the two images captured within the pulse time interval, Δt . As the flow conditions for the present study in laminar and unidirectional and given the seeded particle size of 20 μ m, cross-correlation method is selected. One of the main advantages that cross-correlation has over the auto-correlation would be that the number of random correlations or ‘noise’ is significantly lower thus it improves the ambiguity in the determination of the initial and final position of these tracked particles. In addition, due to the reduced total number of correlations, the computational time using cross-correlation also greatly shortened as compared to auto-correlation. One of the criteria to ensure that the captured flow field is seeded adequately and to ensure accurate generation of velocity vectors is the recommended minimum number of particles within each interrogation area which is set at 5 particles per interrogation area for

the cross-correlation method [115]. For the cross correlation to work effectively, the particles captured in the two images within the pulse time interval, Δt , must exhibit a particle displacement less than $\frac{1}{4}$ of the length of the interrogation area [115]. Hence, it is vital to choose the correct pulse time interval, Δt to match the actual flow velocity to ensure that the captured images satisfy the particle displacement criteria.

Figure 4-44 : Cross-Correlation Validation of Two Correlated Particles at (a) its Initial Position at $T = t$ and (b) at its Final Position at $T = t + \Delta t$

The seeding density criteria and the particle displacement criteria must be satisfied in each PIV measurement setup so as to ensure that the captured flow field images are validated under the cross-correlation method. The captured flow field shown in Figure 4-44 of a single oscillating airfoil at $\alpha = \pm 45^\circ$ and at $t = 0.281s$ under the PIV experimental parameters as shown in Table 4-16 will be analyzed to demonstrate this cross-correlation validation.

In Figure 4-44, the interrogation areas were superimposed on both the captured flow field images at its initial condition, $T = t$ and at its final condition $T = t + \Delta t$ where Δt is the pulse time interval. One interrogation area located near the start of the trailing wake is selected and circled in white in Figure 4-44. Under magnification, the seeding density criteria can be determined as the illuminated particles can be observed. Through analyzing the two close-up

interrogation areas, the particles can be correlated with apparent displacements in the X-direction (parallel to the flow direction) as illustrated in Figures 4-44 (a) and (b). For the particle displacement validation, two particles, Particle 1 and Particle 2 are scrutinized with its displacements and velocities calculated as shown below.

Seeding Density Criteria for Cross-Correlation:

Minimum No. of Correlated Particles = 5

No. of Correlated Particles = 18 > 5 **(Validated)**

Particle Displacement Validation Criteria:

Particle Displacement < $\frac{1}{4}$ of the Interrogation Length = $32 / 4 = 8$ pixels

Particle 1 Displacement, $\Delta X_1 = 2.07$ pixels < 8 pixels **(Validated)**

Particle 2 Displacement, $\Delta X_2 = 2.56$ pixels < 8 pixels **(Validated)**

Velocity Validation

Based on the scale factor calibration, the spatial resolution = 7.5 pixels / mm and pulse time interval, $\Delta t = 3000\mu s$,

Particle 1 Velocity, $U_1 = \left(\frac{2.07}{7.5} \times 10^{-3} \right) \div (3000 \times 10^{-6}) = 0.092$ m/s

Particle 2 velocity, $U_2 = \left(\frac{2.56}{7.5} \times 10^{-3} \right) \div (3000 \times 10^{-6}) = 0.1137$ m/s

Table 4-17: Numerical Data Correlation for Single Oscillating Airfoil at $\alpha = \pm 45^\circ$ and at $t = 0.281s$

x	y	x (pix)	y (pix)	x (mm)	y (mm)	U pix	V pix	U	V
20	22	335.5	367.5	44.73333	49	-2.28017	0.010801	-0.10134	0.00048
20	23	335.5	383.5	44.73333	51.13333	-2.31518	0.00069	-0.1029	3.07E-05
20	24	335.5	399.5	44.73333	53.26667	-2.33164	-0.01286	-0.10363	-0.00057
20	25	335.5	415.5	44.73333	55.4	-2.33389	-0.02867	-0.10373	-0.00127

Based on these two particles velocities in the X direction, the average velocity, U_{ave} for this interrogation area is 0.010283m/s. Based on the numerical data from the Dantec Dynamic Studio software, at the same coordinates, the computed velocity component in the X-direction, U is 0.01029m/s highlighted in red in Table 4-17. **(Validated)**

Conclusively, this cross-correlation validation analysis has proven that the seeding density criteria and the particle displacement criteria are satisfied. Furthermore, the particle velocities validation also demonstrates that even with only two particles selected, these average particle velocities agreed very well the software's numerical data at the same coordinates shown in Table 4-17. The software essentially uses all the correlated particles' displacements to

calculate the average velocity vector for the particular interrogation area which is more accurate due to the utilization of more data points. With the selection of 32 by 32 interrogation area and a spatial resolution of 7.5 pixels / mm, together with the seeding density and particle displacement criteria satisfied, the resultant velocity vectors generated have sufficiently good resolution to resolve the flow structures near the airfoil surfaces.

For the purpose of computing the flow field vorticities through interpolations, the present study employed overlapping of 50% where, the interrogation areas overlapped each other by 50% thus increasing the number of correlations especially so with correlations near the interrogation edges due to loss-of-pairs. This overlapping essentially increases the number of vectors which helps in ensuring the interpolations were carried out with great accuracy thus producing reliable and high quality vorticity contours. With the 50% overlapping of the interrogation area, the velocity vector map will consist of 100 x 75 vectors which is 4 times more the total number of vectors as compared to without any overlapping introduced.

As explained earlier, after the captured images are processed to produce the raw velocity vector maps, there will still be a need to manually removed erroneous vectors or noise by “manual validation” where the user can adjust the peaks amplitudes of the signal and vector lengths. After this manual validation step, the present study employed the ‘3 x 3’ average filtering method to smoothen out these velocity vector maps. Lastly, all the numerical data, captured camera images, filtered velocity vector maps will be saved and exported for the processing of vorticity contours.

4.6.4 Improved PIV Experimental Setup

Figure 4-45 : PIV Plots of Single Static NACA4421 Airfoil at $\alpha = +25^\circ$ at $Re = 8.6 \times 10^3$ showing the Shadow Region; (a) Captured Camera Image ; (b) Velocity Vector Map and (c) Vorticity Contours (ζ^s)

Due to the initial PIV experimental arrangement shown in Figure 4-39, the projected horizontal laser sheet only illuminates the top suction side of the airfoil; hence the CCD camera which is positioned at the base of the test section is also able to capture an incomplete image of the flow field as illustrated in Figure 4-45 (a). The bottom pressure side of the airfoil is not illuminated as it lies in the ‘shadow region’ of the laser sheet thus the resulting velocity vector maps produced are incomplete, displaying voids or erroneous velocity vectors in the shadow region as exhibited in Figure 4-45 (b). The corresponding computed vorticity contours also exhibit erroneous patches in the ‘shadow region’ as these contour plots are processed with reference to the velocity vector maps as illustrated in Figure 4-45 (c). Thereafter, the PIV superimposition method was developed to overcome this problem and is elaborated in Appendix A.

Figure 4-46 : Improved PIV Experimental Setup with Hinged Mirrors for Shadow Region Illumination

The PIV superimposition method offers a solution to generate a complete vorticity contour plot through the substitution of the missing data in the ‘shadow region’ with actual raw numerical data however; it is an extremely tedious and time-consuming process. Hence, the present study developed an innovative method that both practical and effective in order to fill in those voids in the ‘shadow region’. The improved PIV setup consists of using hinged mirrors which allowed the residual incidence laser sheet to be reflected onto the ‘shadow region’ as illustrated in Figure 4-46. The hinged mirrors consists of one ‘big’ mirror measuring 14cm by 60cm that is placed in the center of two ‘small’ mirrors measuring 12cm by 60cm. The incorporated hinges will allow the control of the angle of reflection of the residual incidence laser sheet which is instrumental in projecting the required illumination to the ‘shadow region’. In addition, the center ‘big’ mirror is designed to be wider than the airfoil chord in order to allow direct reflection of the incidence laser sheet onto the leading and trailing edge region of the flow field as shown in Figure 4-46. In order to ensure that the shadow region is adequately illuminated, the same validation check as presented in Section 4.6.3 is carried out on the illuminated ‘shadow region’ and the laser intensities from the two laser heads were also increase to near maximum levels. In terms of processing the captured images, as mentioned earlier, a 3 x 3 average filtering is applied to the velocity vector plots that will ensure complete coverage of the illuminated ‘shadow region’.

Figure 4-47 : Cross-Correlation Validation of Two Correlated Particles in the Illuminated ‘Shadow Region’ at (a) its Initial Position at $T = t$ and (b) at its Final Position at $T = t + \Delta t$

Seeding Density Criteria for Cross-Correlation:

Minimum No. of Correlated Particles = 5

No. of Correlated Particles = 12 > 5

✓ **Validated with a 33% drop in seeding density as compared to the validation presented in Section 4.6.3 on the same image captured.**

Particle Displacement Validation Criteria:

Particle Displacement < $\frac{1}{4}$ of the Interrogation Length = $32 / 4 = 8$ pixels

Particle 1 Displacement, $\Delta X_1 = 1.88$ pixels < 8 pixels (**Validated**)

Particle 2 Displacement, $\Delta X_2 = 2.51$ pixels < 8 pixels (**Validated**)

Velocity Validation

Based on the scale factor calibration, the spatial resolution = 7.5 pixels / mm and pulse time interval, $\Delta t = 3000\mu s$,

Particle 1 Velocity, $U_1 = \left(\frac{1.88}{7.5} \times 10^{-3} \right) \div (3000 \times 10^{-6}) = 0.08356$ m/s

Particle 2 velocity, $U_2 = \left(\frac{2.51}{7.5} \times 10^{-3} \right) \div (3000 \times 10^{-6}) = 0.11156$ m/s

Based on these two particles velocities in the X-direction, the average velocity is 0.09756m/s. Based on numerical data from the Dantec Dynamic Software, the measured U at the same point is 0.10171m/s which indicate a small difference of 4% between the two particles average velocity and the measured velocity. Conclusively, satisfying both the seeding density and particle displacement criteria for the illuminated ‘shadow region’ as shown in Figure 4-47, it validates the improved PIV setup’s capability in producing complete and reliable PIV measurements.

4.6.5 Vorticity Contours Stitching Process

Figure 4-48 : Tandem Airfoils Vorticity Contours Stitching Process Flow Chart

Following the validated implementation of the new PIV arrangement, there’s one big challenge to overcome in terms of processing these PIV measurements. One of the limitations of the present PIV arrangement is that the CCD camera can only capture the flow field surrounding one airfoil, hence, the optical components and CCD camera are setup twice to cover the flow fields for both upstream and downstream airfoils. Consequently, with the flow fields for the tandem airfoils captured independently with the same experimental parameters, the challenge is to stitch the vorticity contours from the two tandem airfoils together. It is not simply cutting and pasting these plots into one but the stitching process as illustrated in Figure 4-48 ensures that these stitched vorticity plots have accurately scaled and positioned superimposed airfoil geometries. In addition, the stitching process must be sufficiently robust and versatile to handle tandem cases at various stagger and gap distances. The functions of each step in the tandem airfoil vorticity contours stitching process are defined in Figure 4-48 and elaborated in Appendix B.

5. RESULTS AND DISCUSSION

5.1 SINGLE STATIC AIRFOIL

The force measurement results for a single static NACA4421 airfoil will be presented where it is subjected to a range of α from 0° to $+60^\circ$ at 5° intervals with a freestream water velocity, U_∞ of 0.102m/s and a corresponding Reynolds number, Re of 8.6×10^3 . These results are compared to Swalwell et al. [106] and 2-D CFD simulation results and are subsequently correlated with the PIV measurements so as to analyze the flow dynamics around the single static airfoil.

Figure 5-1 : (a) Arrangement of Pressure Taps in Rows from Top View; (b) The 28 Pressure Taps Rows in Airfoil Cross-Sectional View [106]

Swalwell et al. [106] experimentally investigated on the effects of turbulence intensity on the performance of a NACA4421 airfoil measuring 0.125m in chord where they carried out pressure measurements in a 450 kW wind tunnel at a $Re = 2.85 \times 10^5$. The airfoil embodied 5 arrays of pressure taps that were installed internally and were flushed to the airfoil surface. These pressure taps arrangement is shown in the Figure 5-1. The pressure measurements were carried out using the ‘Scanivalve’ system where the sampling measurement rate was at 1000Hz for 35 seconds at each angle of attack, α . Using Row B1 and B2 as illustrated in Figure 5-1 (a), the average C_L and C_D across the airfoil cross section were calculated by summing the resultant forces in the horizontal and vertical directions. The turbulence in Swalwell et al. [106] study was generated by placing a grid measuring 2m by 2m in the upstream section of the wind tunnel. However, for the purpose of relevance to the present study, only the C_L and C_D plots without any grid were presented where the flow had a similar T_i of 0.6% to the present flow condition.

5.1.1 Average Force Characteristics of a Single Static Airfoil

In order to validate the present force and PIV results, 2-D CFD simulations on the single static airfoil were carried out at the same Re of 8.6×10^3 using the κ - ϵ turbulence model which is ideal for viscous flow conditions [53]. These CFD simulations were performed using the Fluent Ansys 18.1 software at five pitch angles, α , ranging from $\alpha = 0^\circ$ to $\alpha = +60^\circ$. The iteration-based SIMPLE solution method was employed to compute the pressure and velocity values for the various pitch angle static cases. Coarse meshes were used in order to reduce the computational time for these simulations, taking into consideration that the required simulation of the flow over a single NACA4421 airfoil is relatively straightforward.

Figure 5-2 : Average Lift Coefficient, C_L for Single Static NACA4421 Airfoil

Firstly, the present average C_L results in Figure 5-2 shows a linear increase in lift from $\alpha = 0^\circ$ to its static stall angle at $\alpha_{ss} = +15^\circ$ which is followed by a plateau in lift force until $\alpha = +20^\circ$. C_L then increases gradually with the increase in α until $+45^\circ$ where the single static airfoil exhibits a local maximum C_{Lmax} of 0.82. After achieving its C_{Lmax} , the lift force decreases to $C_L = 0.72$ as α increases to $+60^\circ$.

When comparing to the C_L results by Swalwell et al. [106], the present C_L results displays a slower rate of increase before the static stall angle at $\alpha_{ss} < +15^\circ$. The plateau in the present C_L results between $\alpha = +15^\circ$ to $\alpha = +20^\circ$ indicates that there is deceleration in lift production which corresponds to when static stall occurs. Swalwell et al. [106] demonstrates this static stall more evidently where it attains a distinct lift peak of 1.4 at $\alpha_{ss} = +15^\circ$ before decreasing by 35% as α increases from $\alpha_{ss} = +15^\circ$ to $\alpha = +20^\circ$. For the present C_L results, the peak in lift

is only observed at approximately $\alpha = +45^\circ$ which is aligned with the second lift peak in Swalwell et al. [106] results as shown in Figure 5-2. Importantly, the present study showcases a consistently lower average C_L results by an average of 50% in relation to the average C_L results by Swalwell et al. [106] throughout the entire range of α tested.

These variations in average C_L results between the present study and Swalwell et al. [106] are attributed to the different flow conditions. The flow across the present single static airfoil is at a lower Re of 8.6×10^3 as compared to Swalwell et al. [106] experiments that ran at a higher Re of 2.85×10^5 . At a higher Re , the flow over the static airfoil in Swalwell et al. [106] was able to attain a distinct peak in lift force at its static stall condition as the higher flow velocity over the suction airfoil side induced a larger pressure difference between the airfoil sides. However, as α increases from $\alpha_{ss} = +15^\circ$ to $\alpha = +20^\circ$, this higher flow velocity over the suction side correlates to stronger adverse pressure gradients which explains the higher degree of flow separation event [77]. This higher degree of flow separation explains the 35% reduction in average C_L after attaining its C_{Lmax} at its α_{ss} for Swalwell et al. [106].

Figure 5-2 also shows identical average C_L trends for both the present and CFD results where the average C_L gradually increases with the increase in α and did not exhibit any distinct peak in C_L at its static stall condition of $\alpha_{ss} = +15^\circ$ as it only attained its C_{Lmax} at a larger α of $+45^\circ$. However, at pre-static stall pitch angles of $\alpha \leq +15^\circ$, the computational results produce consistently higher average C_L than the present results by an average C_L difference of $+0.12$. As α increases from $\alpha > 15^\circ$ to 60° , the difference between the present results and computational results decreases to an average C_L difference of $+0.07$.

Figure 5-3 : Average Drag Coefficient, C_D Plot for Single Static NACA4421 Airfoil

For the average C_D plot shown in Figure 5-3, the present results show a similar increasing trend when comparing to the results by Swalwell et al. [106] and the 2-D CFD simulations as α increases from $\alpha = 0^\circ$ to $\alpha = +60^\circ$. The present C_D results exhibit the highest rate of increase in C_D after the static stall angle, $\alpha_{ss} = +15^\circ$ to $\alpha = +20^\circ$ which corresponds to the average C_L ‘plateau’ region shown in Figure 5-2. This inverse correlation between drag and lift forces is also seen in the results of Swalwell et al. [106] where the sudden decrease in C_L correlates to a sudden increase in C_D in between $+15^\circ < \alpha < +20^\circ$. In the present study, the opposite effect of this inverse correlation is seen at $\alpha = +45^\circ$ where the average C_D plateaus at 1.2 (decelerated rate of increase) which correlates to the instance when the single static airfoil produces its C_{Lmax} before decreasing steadily as α increases to $\alpha = +60^\circ$. The average C_D result in Figure 5-3 also shows a good agreement between the computational and experimental present results especially so for lower $\alpha \leq +30^\circ$. At $\alpha = +45^\circ$, the present results exhibit a 20% higher C_D than the computational results before producing the similar average $C_D \approx 1.50$ at the largest α at $+60^\circ$.

Despite the marginally lower average C_L and the 20% higher average C_D at $\alpha = +45^\circ$, the present results showed good agreement to the k- ϵ turbulent CFD model results. Variation in the average C_L results between the present and computational results may be attributed to the employment of coarse meshes for the CFD simulations in order to reduce computational processing time.

5.1.2 Vorticity Characteristics of a Single Static Airfoil

Figure 5-4 : Vorticity Contours, ζ (s^{-1}) for Single Static NACA4421 Airfoil at $\text{Re} = 8.6 \times 10^3$ and at $\alpha = 0^\circ$ to $+60^\circ$

Figures 5-4 (a) to (h) display the vorticity contours of the single static airfoil at α ranging from $\alpha = 0^\circ$ to $+60^\circ$ at $\text{Re} = 8.6 \times 10^3$. These vorticity contours are scaled to size with respect to the airfoil size and with standardized color bars to facilitate comparison between the vorticity contours. The red vorticity contours denote vortices rotating in the Counter-Clockwise (CCW) direction whereas the blue contours denote vortices rotating in the Clockwise (CW) direction. With the freestream water flowing from right to left in the present study, the flow over the suction surface of the static airfoil forms the CCW (red) shear layer whereas the pressure surface forms the CW (blue) shear layer.

At $\alpha = 0^\circ$, the flow over the suction side remains attached with the vortex structures on both sides are relatively weak in magnitude and size as shown in Figure 5-4 (a) which explains the low average C_L production of -0.043. The trailing wake shows a Von Kármán vortex street characterized by the alternating pairing of the CCW and CW shed vortices [57]. The trailing wake remains defined without being affected by the presence of turbulent eddies due to the laminar nature of the vortex structures. The orientation of the CW and CCW vortex pairings exhibited a typical ‘neutral’ wake structure which supports the low average C_D of 0.10.

As α increases from $\alpha = 0^\circ$ to its static stall angle, $\alpha_{ss} = +15^\circ$, Figures 5-4 (a) to (c) exhibit the strengthening of the CCW shear layer over the suction side which is characterized by the darker red CCW vorticity contours. The CCW shear layer also displays a consistent increase in length (elongation) as α increases from 0° to $+15^\circ$. On the other hand, the flow remains attached over the pressure side without any apparent changes to the CW shear layer. With the strengthening of CCW shear layer over the suction side, it induces a larger pressure

difference, Δp , between the two airfoils surfaces thus resulting in an increase in average C_L . This increase in Δp between the airfoil surfaces can be identified from the steep linear increase in average C_L from $C_L = -0.043$ at $\alpha = 0^\circ$ to $C_L = 0.52$ at $\alpha_{ss} = +15^\circ$ as shown in Figure 5-2.

In this pre-static stall conditions, as α increases from 0° to $+15^\circ$, the trailing wake structure changes from a neutral wake structure at $\alpha = 0^\circ$ to ‘drag-inducing’ Von Kármán vortex streets as shown in Figures 5-4 (b) and (c). In addition, the CCW shear layer starts to separate from the suction surface at $\alpha = +10^\circ$ at approximately $0.4c$ from the LE. As α continues to increase to $\alpha = +15^\circ$, this separation point moves towards the LE. Despite the differences in separation point, Figures 5-4 (b) and (c) reveal similar wake structures in terms of the ‘dead-air’ region area coverage. This ‘dead-air’ region is defined as the region in between the CCW and CW shear layers and it covers till where the two shear layers converge. The similarities in ‘dead-air’ region and the ‘drag-inducing’ Von Kármán vortex street between $\alpha = +10^\circ$ and $\alpha = +15^\circ$ gave rise to the similar average C_D of 0.3 for these two static cases as displayed in Figure 5-3.

As α increases from $\alpha_{ss} = +15^\circ$ to $\alpha = +20^\circ$, Figure 5-4 (d) shows a weaker CCW shear layer formation with a reduction in length in relation to Figure 5-4 (c) which may explain the deceleration in the increase in C_L at $\alpha = +20^\circ$. This plateau in C_L correlates to a sudden increase in average C_D which is similar to Swalwell et al. [106] as displayed in Figure 5-3. In relation to the trailing wake structure in Figure 5-4 (c), Figure 5-4 (d) display an apparent widening of the ‘dead-air’ region and no clear definition of Von Kármán shed vortex pairings as α increases to $+20^\circ$. This may indicate that the static airfoil at $\alpha = +20^\circ$ is dominated by the effects of ‘wake drag’ forces rather than ‘induced-wake’ drag forces produced by the ‘drag-inducing’ Von Kármán vortex street thus leading to the sudden increase in average C_D from $\alpha_{ss} = +15^\circ$ to $\alpha = +20^\circ$ shown in Figure 5-3.

Another point of interest would be at $\alpha = +45^\circ$, where both Swalwell et al. [106] and the present results produced a local maximum in lift. Swalwell et al. [106] accredited this increase in C_L to the upward deflection of the flow from the pressure side. Based on the vorticity contours in Figures 5-4 (e) to (h), the CCW shear layer exhibits a gradual increase in curvature which is illustrated by the black dotted lines. This gradual increase in curvature of the CCW shear layer may indicate the effects of the upward deflection of the flow from the pressure side which was documented by Swalwell et al. [106]. The single static airfoil at $\alpha =$

+45° showcases the strongest and largest CCW shed vortices as seen in Figure 5-4 (g) which give rise to the highest average C_{Lmax} of 0.82 attained by the present single static airfoil.

Through the transition from $\alpha = +30^\circ$ to $+60^\circ$ in the post-static stall conditions, Figures 5-4 (e) to (h) clearly demonstrate the widening of the CCW shear layer which correlates to a steady increase in ‘dead-air’ region. Therefore, this increase in ‘dead-air’ region give rise to steady increase in ‘wake-drag’ force production which is clearly identified by the relatively linear increase in average C_D shown in Figure 5-3. For these cases in the post-static stall condition, there is no evidence of ‘drag-inducing’ Von Kármán Vortex Street which was clearly observed in the pre-static stall conditions of $\alpha \leq +15^\circ$. This would indicate that the overall drag production is dominated by ‘wake-drag’ forces and not by ‘induced-wake’ drag forces for these post-static stall cases.

Figure 5-5 : Laminar Separation Point Distance from Leading Edge (LE), X_{LE} / c for Single Static Airfoil

Aligned with Lee and Gerontakos [60] experimental results on single static airfoil, the laminar separation points for the present single static cases are presented alongside Lee and Gerontakos [60]’s results in Figure 5-5. These laminar separation points are non-dimensionalized by the chord, c and represented as X_{LE} / c where X_{LE} denotes the distance between the separation points from the LE. Firstly, the present results show identical trend to Lee and Gerontakos [60]’s results where the gradient is steeper for $\alpha \leq +10^\circ$ and as $\alpha > +10^\circ$, the X_{LE} / c starts to decrease in a decreasing rate. The present X_{LE} / c results indicate that the

flow over the suction side starts to separate near the TE and as α increases, the laminar flow separation point progresses towards the LE. This observation also agrees well with the conclusions made by Swalwell et al. [106] where it states that for a thick asymmetrical cambered airfoil like NACA4421, it would be typical that the flow separation point started at the TE and progresses towards the LE as α increases.

In conclusion, for this single static case, the production of higher C_L can be attributed to stronger and more elongated CCW shear layer formation over the suction side. The ‘drag-inducing’ Von Kármán vortex streets were clearly observed for $\alpha \leq +15^\circ$ which gave rise to the production of ‘wake-induced’ drag forces [57]. As α increases to $> +15^\circ$, the trailing wake is characterized by the ‘widening’ of the ‘dead-air’ region which correlates to the increase in ‘wake drag’ force production. The laminar separation point behavior exhibited by the present results agrees very well with Lee and Gerontakos [60]’s results and Swalwell et al. [106]’s conclusions.

5.2 IN-PHASE STATIC TANDEM AIRFOILS

The single static airfoil results have provided the foundation to understand and analyse the flow structures and its corresponding aerodynamic forces produced in steady-state flow conditions at low Re. The novel conception for tandem wing configuration was first proposed by Schmidt [79] where he highlighted that some of the vortical energy can be attained by introducing a stationary airfoil in the trailing wake of an oscillating airfoil. Schmidt [79] subsequently proved that the lift efficiency was doubled in the tandem configuration as the aft wing placed in the fore wing trailing wake was able to convert the vortex energy to lift.

One of the pioneering experimental study was conducted by Scharpf and Mueller [43] where they have investigated on the flow across two identical Wortmann FX63-137 airfoils in tandem at low $Re = 8.5 \times 10^4$ and at $Sd = 1.5$ and $G = 0$. Their flow visualizations and force measurements are compared to the present results. In this section, the effects of varying Sd at 2 and 2.5 and the effects of in-phase, $\phi = 0^\circ$ and out-of-phase, $\phi = 180^\circ$ static tandem cases will be investigated. In addition to validating the present tandem static results with the results of Scharpf and Mueller [43], the single static results will also be used as a baseline for comparison to evaluate the effects of the tandem wing configuration such as the effects of down-washed vortex interactions on downstream airfoil’s aerodynamic force production.

5.2.1 Average Force Characteristics of In-Phase Static Tandem Airfoils

Table 5-1 : Average Force Coefficients for In-Phase Static Tandem Airfoils at $\alpha = +15^\circ$ and $\alpha = +45^\circ$ with $S_d = 2$ and 2.5

α ($^\circ$)	Average Force Coeff.	Upstream Airfoil, $S_d = 2$	Downstream Airfoil, $S_d = 2$	Upstream Airfoil, $S_d = 2.5$	Downstream Airfoil, $S_d = 2.5$	Single Static Airfoil
$+15^\circ$	C_L	0.16	-0.01	0.38	0.56	0.57
	C_D	0.15	-0.03	0.25	0.03	0.24
$+45^\circ$	C_L	0.63	0.09	0.65	0.2	0.8
	C_D	0.84	0.06	0.79	0.19	1.19

In addition to analyzing the average force coefficients across the range of α , the vorticity contours for the static tandem airfoils at $\alpha = +15^\circ$ and $\alpha = +45^\circ$ will be further investigated. These two selected α will encompass the key changes in flow structures and force production starting from ‘pre-static stall’ to ‘post-stall’ condition for these in-phase static cases. Table 5-1 tabulates the average C_L and C_D for the in-phase static tandem airfoils at $\alpha = +15^\circ$ and $+45^\circ$ to facilitate the vorticity contours analysis in Sections 5.2.2 and 5.2.3.

Figure 5-6 : Average C_L Plot for In-Phase Static Tandem Airfoils at $Re = 8.6 \times 10^3$ and at +ve α for $S_d = 2$ and 2.5

Figure 5-7 : Average C_D Plot for In-Phase Static Tandem Airfoils at $Re = 8.6 \times 10^3$ and at +ve α for $Sd = 2$ and 2.5

Figures 5-6 and 5-7 present the average C_L and C_D for in-phase ($\phi = 0^\circ$) tandem static airfoils at various positive α and at two tested stagger distances of $Sd = 2$ and 2.5 respectively. At $Sd = 2$, both upstream and downstream airfoils exhibit consistently lower average C_L and C_D when comparing to the single static airfoil over the entire range of α . The upstream airfoil also demonstrates its ability to consistently produce higher average C_L and C_D than its downstream counterpart. From $\alpha_{ua} = +10^\circ$ to $\alpha_{ua} = +20^\circ$, the upstream airfoil exhibited a similar trend in C_L as the single static case. As α increases from $\alpha_{ua} = +20^\circ$ to $\alpha_{ua} = +30^\circ$, the upstream airfoil exhibit the steepest C_L increment from 0.14 to 0.58 which also corresponds to the largest C_D increment from 0.07 to 0.60. After $\alpha_{ua} = +35^\circ$, the lift reduces to local $C_{Lmin} = 0.55$ at $\alpha_{ua} = +40^\circ$ before it increases as α increases to $\alpha_{ua} = +45^\circ$. Subsequently, the average C_L plateaued at 0.63 from $\alpha_{ua} = +45^\circ$ to $\alpha_{ua} = +60^\circ$. The upstream airfoil produces consistently lower C_D than the single static case with a maximum C_D reduction of approximately 90% at $\alpha_{ua} = +20^\circ$ as shown in Figure 5-7.

For $Sd = 2$, the average C_L results in Figure 5-6 clearly illustrate the influence of the upstream airfoil on its downstream counterpart. Firstly, the downstream airfoil at $Sd = 2$ exhibits a consistently lower average C_L across the α range with a different trend in relation

to both the single static case and its upstream counterpart. It starts off by decreasing to its minimum of -0.04 at $\alpha_{da} = +15^\circ$ before increasing to its maximum of 0.27 at $\alpha_{da} = +30^\circ$. Subsequently, C_L decreases linearly as α increases from $\alpha_{da} = +30^\circ$ to $\alpha_{da} = +60^\circ$. Positively, the downstream airfoil produces consistently lower average C_D than its upstream counterpart and the single static case. The downstream airfoil at $Sd = 2$ produces the lowest average C_D of -0.03 at $\alpha_{da} = +15^\circ$ and -0.05 at $\alpha_{da} = +60^\circ$ which corresponds to lowest average C_L values shown in Figure 5-6. These negative average C_D values indicate the production of thrust forces by the downstream airfoil at $Sd = 2$. When comparing the downstream airfoil to the single static case and its upstream counterpart, the general trend shows that with the increase in α , the difference between the C_D results increases thus demonstrating that the downstream airfoil is able to produce lesser drag at a larger α .

Next, similar to $Sd = 2$, both upstream and downstream airfoils at $Sd = 2.5$ produce consistently lower average C_L and C_D than the single static case throughout the tested α range with the exception of the downstream airfoil at $Sd = 2.5$ generating a slightly higher average C_L between $+15^\circ < \alpha < +22^\circ$ seen in Figure 5-6.

The upstream airfoil at $Sd = 2.5$ produces much higher C_L than its upstream counterpart at $Sd = 2$ between $+10^\circ < \alpha_{ua} < +25^\circ$ with the largest margin at $\alpha_{ua} \approx +20^\circ$. This increase in C_L corresponds well to the increase in C_D with the largest C_D increment of 86% at $\alpha_{ua} \approx +20^\circ$. Subsequently, the upstream airfoil at $Sd = 2.5$ generates a constant average C_L of 0.5 between $\alpha_{ua} = +25^\circ$ to $\alpha_{ua} = +40^\circ$ which is approximately 20% lower than at $Sd = 2$. In this α range, the upstream airfoil at $Sd = 2.5$ also produces lower average C_D values up to a maximum reduction of 48% at $\alpha_{ua} = +40^\circ$ when compared to the upstream airfoil at $Sd = 2$. As α_{ua} increases from $+40^\circ$ to $+60^\circ$, C_L increases to its maximum of 0.64 at $\alpha_{ua} = +45^\circ$ before plateauing till $\alpha_{ua} = +60^\circ$ thus matching the C_L trend and values produced by the upstream airfoil at $Sd = 2$ in the same α range. Despite producing similar average C_L values between $\alpha_{ua} = +40^\circ$ to $+60^\circ$ at both Sd , the upstream airfoil produces consistently lower C_D at the larger Sd of 2.5 . The general trends for both C_L and C_D for the upstream airfoil at $Sd = 2.5$ are relatively similar to the single static case, thus demonstrating the diminishing influence of the presence of the downstream airfoil as stagger distance increases. This is a similar effect of stagger distance on the upstream airfoil documented by Broering and Lian [44] in their study on tandem airfoils.

The downstream airfoil at $S_d = 2.5$ demonstrates its ability to generate consistently higher average C_L across the entire α range when comparing to the downstream airfoil at $S_d = 2$. For α_{da} between $+10^\circ$ and $+20^\circ$, the downstream airfoil at $S_d = 2.5$ displays the steepest increase in lift production as it attains its maximum of 0.62 at $\alpha_{da} = +20^\circ$ which is more than four times the C_L produced by its downstream counterpart at $S_d = 2$. In the region of $+15^\circ < \alpha_{da} < +20^\circ$, the downstream airfoil at $S_d = 2.5$ generates the highest C_L values when comparing to all the other cases. Subsequently, as α_{da} increases from $+20^\circ$ to $+60^\circ$, the downstream airfoil at $S_d = 2.5$ exhibits a linear reduction in C_L at approximately the same gradient as its downstream counterpart at $S_d = 2$ as it produces lower lift forces than the upstream airfoils and the single static case.

Based on the upstream airfoil's force coefficient results, an increase in C_L corresponds to increase in C_D and similarly, a decrease in C_L corresponds to a decrease in C_D . However, the downstream airfoil at $S_d = 2.5$ exhibits a different relationship between C_L and C_D . As documented earlier, the downstream airfoil at $S_d = 2.5$ generated the highest C_L values in the region of $+15^\circ < \alpha_{da} < +20^\circ$ but Figure 5-12 clearly illustrates that C_D values are consistently lower than its upstream counterpart at $S_d = 2.5$ and single static case. Importantly, this demonstrates the downstream airfoil's ability to suppress its drag production despite attaining the highest C_L values in this α range. In addition, the downstream airfoil at $S_d = 2.5$ exhibits identical 'n-shaped' trend as its downstream counterpart at $S_d = 2$ but at consistently higher average C_D values.

Figure 5-8 : $(C_L / C_D)_{total}$ Ratio for In-Phase Static Tandem Airfoils at $S_d = 2$ and $S_d = 2.5$

In Figure 5-8, the $(C_L/C_D)_{\text{total}}$ ratio is presented to better visualize the effects of the tandem static airfoil configuration when comparing to the single static case. Similar to the tandem wing studies such as by Scharpf and Mueller [43], the $(C_L/C_D)_{\text{total}}$ ratio is used to compare the combined static tandem airfoils to the single airfoil performances and to optimize the various tandem airfoil configurations for the most efficient aerodynamic performance. As the two tandem airfoils are operating in the same free stream flow conditions in the present study, the total force coefficients produced by the tandem airfoils are simply the summation of its upstream and downstream force coefficients. Hence, the $(C_L/C_D)_{\text{total}}$ ratio is expressed as [43]:

$$C_{L(\text{total})} = C_{L(\text{upstream})} + C_{L(\text{downstream})} \quad (\text{Eq. 5.1})$$

$$C_{D(\text{total})} = C_{D(\text{upstream})} + C_{D(\text{downstream})} \quad (\text{Eq. 5.2})$$

$$\left(\frac{C_L}{C_D}\right)_{\text{total}} = \frac{C_{L(\text{total})}}{C_{D(\text{total})}} \quad (\text{Eq. 5.3})$$

Figure 5-8 shows a typical C_L/C_D ratio of an asymmetrical airfoil over an increasing range of α for the single static case where C_L/C_D increases linearly and peaks at 2.35 at its static stall angle at $\alpha_{\text{ss}} = +15^\circ$ before decreasing sharply due to the increase in C_D as α increases.

For the tandem static airfoils at $S_d = 2$, the maximum $(C_L/C_D)_{\text{total}}$ of 2.17 occurs at a larger α of 20° thus indicating a delay in static stall events and that the peak is prolonged before it decreases to match the single static case at $\alpha = +30^\circ$ onwards as shown in Figure 5-8. However, at $S_d = 2$, the tandem airfoils performed poorly at smaller $\alpha < +15^\circ$ due to low lift force generation from both upstream and downstream airfoils. Hence, to optimize the benefits of the tandem airfoil configuration at $S_d = 2$, the static tandem airfoils would need to operate in the post static stall region of between $\alpha = +20^\circ$ to $+30^\circ$.

On the other hand, at $S_d = 2.5$, the tandem static airfoils performed remarkably well demonstrating its capability of producing $(C_L/C_D)_{\text{total}}$ results that are consistently higher than the single static case at every pitch angle tested. It attains its maximum $(C_L/C_D)_{\text{total}}$ of 3.5 at the same static stall angle of $\alpha_{\text{ss}} = +15^\circ$ as the single static airfoil which is 49% higher than the single static case's maximum $(C_L/C_D)_{\text{total}}$. This substantial increase in $(C_L/C_D)_{\text{total}}$ is

primarily due to the tandem airfoils' ability to produce high C_L while suppressing C_D at larger α especially so for the downstream airfoil.

Conclusions for Average Force Characteristics of in-phase tandem static airfoils at S_d of 2 and 2.5

1. At both stagger distances, $S_d = 2$ and 2.5 , both upstream and downstream airfoils exhibit lower average C_L and C_D when compared to the single static case. Scharpf and Mueller [43] had documented similar findings where in general, both the upstream and downstream in-phase static tandem airfoils showed lower C_L and C_D when compared to the single airfoil case.
2. For the upstream airfoil, the effect of S_d is mixed and dependent on α . At the larger $S_d = 2.5$, the downstream airfoil effect on the upstream airfoil diminishes as shown in Figures 5-11 and 5-12 as the general trend for $S_d = 2.5$ is similar to the single static case. This is also similar observation made by Broering and Lian [44].
3. For the downstream airfoil, with the increase in S_d to 2.5 , average C_L increases at small α_{da} of $< +20^\circ$ and even surpassing the single static case between $+15^\circ < \alpha_{da} < +20^\circ$ region and is able to suppress the corresponding drag forces.
4. The presence of the upstream airfoil influences the downstream airfoil's aerodynamic performance by reducing C_D across the entire range of α . As S_d decreases, the influence of the upstream airfoil increases thus decreasing C_D to the extent of producing negative C_D (thrust forces) at the two ends of the α range at $\alpha_{da} = +15^\circ$ and $\alpha_{da} = +60^\circ$.

5.2.2 Vorticity Characteristics of In-Phase Static Tandem Airfoils Case at $\alpha = +15^\circ$

Figure 5-9 : Vorticity Contours, ζ (s^{-1}) for In-Phase Static Tandem Airfoils at $\alpha = +15^\circ$ and at $Re = 8.6 \times 10^3$ for (1) $Sd = 2$ and (2) $Sd = 2.5$

As shown in Table 5-1, at $Sd = 2$, the upstream airfoil produces an average C_L of 0.16 and C_D of 0.15 which is 72% and 37.5% lower than the single static airfoil at $\alpha = +15^\circ$ respectively. The CCW shear layer over the suction surface shows a separated flow at about the same laminar separation point with similar vortical strength in relation to the single static case as seen in Figures 5-9 (1). On the other hand, the upstream airfoil at $Sd = 2$ exhibits a CW shear layer formation with an average $\zeta \approx -0.03$ as seen in Figure 5-9 (1) whereas the single static airfoil produces a weaker CW shear layer with an average $\zeta \approx -0.017$ as seen in Figure 5-4. This shows that the upstream airfoil at $Sd = 2$ produces a 70% stronger CW shear layer formation when comparing to the single static case which then indicates an increase in flow velocity over the pressure side. Given the similar strength of the CCW shear layers between the upstream airfoil at $Sd = 2$ and the single static case, this increase in flow velocity over the pressure side leads to a decrease in Δp between the airfoil surfaces and in turn, decreases the average lift production for the upstream airfoil at $Sd = 2$.

Figure 5-9 (a) (1) shows the upstream airfoil at $Sd = 2$ generating a ‘drag-inducing’ Von Kármán vortex street where the CCW and CW shed vortices are orientated to produce a resultant velocity vector (red arrow) which points upstream. This ‘drag-inducing’ Von Kármán vortex street is also observed in the single static case at $\alpha = +15^\circ$ as shown in Figure 5-4 (c). When comparing the trailing wake structure between the upstream airfoil at $Sd = 2$ and the single static case shown in Figures 5-4 (c) and 5-9 (1) respectively, it can be seen that the CW shear layers’ trajectories are different as the single static case produces a flatter profile as it propagates along downstream whereas the upstream airfoil at $Sd = 2$ produces an upward-curved profile. This upward-curved CW shear layer profile give rise to a smaller ‘dead-air’ region for the upstream airfoil at $Sd = 2$ when comparing to the single static case. Given the similar nature of the ‘drag-producing’ Von Kármán vortex streets observed, this smaller ‘dead-air’ region may account for 37.5% lower average C_D for the upstream airfoil at $Sd = 2$ in relation to the single static case.

Next, based on the average force results presented in the previous section, the downstream airfoil at $Sd = 2$ produces the lowest average C_L of -0.01 and C_D of -0.03 at $\alpha_{da} = +15^\circ$. The effect of the interaction between the down-washed CW and CCW vortex pairing shed by the upstream airfoil and the downstream airfoil at $Sd = 2$ can be rationalized by analyzing the vortex interactions circled in blue and red respectively in Figure 5-9 (b)(1).

Firstly, the down-washed shed CW vortex interacts with the pressure side of the downstream airfoil at a very close proximity to the LE (as circled in blue in Figure 5-9 (b) (1)). Based on Broering and Lian [44, 45] findings on vortex interactions, ‘liked-signed’ vortex interaction is deemed to be constructive as it accelerates the flow velocity in the interaction region thus, adding energy to the flow. Hence, due to the similar nature of their rotational direction, the vortex interaction between the shed CW vortex and the CW shear layer over the pressure side of the downstream airfoil at $Sd = 2$ is deemed to be a ‘liked-signed’ vortex interaction. Due to the constructive effect of this ‘liked-signed’ vortex interaction, the flow velocity may be accelerated thus decreasing the localized pressure over the pressure side at the interacting region.

The down-washed CCW (red) vortex interacts with the suction side of the downstream airfoil at $Sd = 2$ (as circled in red in Figure 5-9 (b) (1)). Based on the vorticity contours for the single static airfoil at $\alpha = +15^\circ$ shown in Figure 5-4 (c), the flow over the suction side undergoes flow separation due to the increasing influence of adverse pressure gradients [60]. However, this is not the case for the downstream airfoil at $Sd = 2$ as the flow remains attached which indicates that there is a decrease in flow velocity over the suction side, thus enabling the mitigation of adverse pressure gradients. If the ‘like-signed’ vortex interaction is deemed to be constructive then the vortex interaction between the down-washed shed CCW vortex and the CCW shear layer over the suction side of the downstream airfoil will cause an increase in flow velocity and decrease in pressure over the suction side. However, the vorticity contours shown in Figure 5-9 (b) (1) does not demonstrate the strengthening of the CCW shear layer over the suction side of the downstream airfoil. In order to explain this contradicting reduction in flow velocity over the suction side, the influence of the down-washed CW vortex near the LE has to be taken into account. Due to the close proximity of this CW interacting vortex to the suction side as highlighted in Figure 5-9 (b) (1), it may impede the acceleration of the fluid particles at the LE over the suction side, resulting in a decrease in flow velocity. As a consequence, the decrease in flow velocity over the suction side of the downstream airfoil increases the pressure over the suction side.

Due to the effects of these vortex interactions, the decrease in localized pressure over the pressure side of the downstream airfoil and the increase in pressure over the suction side contribute to a large decrease in overall Δp between the downstream airfoil sides which may explain the considerably low average C_L of -0.01 produced by the downstream airfoil at $Sd =$

2. This reduction in C_L as a direct result of the decrease in Δp across the downstream airfoil surfaces was also reported by Sharpf and Mueller [43].

The downstream airfoil at $Sd = 2$ demonstrates its ability to produce one of the lowest average C_D of -0.03. Based on its vorticity contours in Figures 5-9 (1), due to the CCW down-washed vortex interaction, the flow over the suction surface remains relatively attached with minimal separation while keeping the drag forces over the downstream airfoil very low thus producing negative drag forces which equates to thrust forces. Due to the relatively attached flow structure over the suction side of the downstream airfoil at $Sd = 2$, the ‘dead-air’ region is considerably smaller than its upstream counterpart thus indicating a significantly weaker effect of ‘wake drag’. In terms of its trailing wake structure for the downstream airfoil, both CCW and CW shear layers are weak and the shed vortex pairing is also not well defined. The trailing wake resembles a ‘neutral wake’ where the CW and CCW shed vortex pairing are oriented in parallel to each other as circled in black in Figure 5-9 (a) (1). The key observations on the downstream airfoil at $Sd = 2$ are; (1) relatively attached flow over the suction side; (2) decrease in ‘dead-air’ region area coverage and width and (3) ‘neutral wake’ trailing wake structure; justifying the weak influence of ‘form drag’, ‘wake drag’ and ‘induced wake drag’ respectively which supports the low average C_d of -0.03.

As Sd increases from 2 to 2.5, the upstream airfoil produces nearly doubled the average C_L as it increases from 0.16 to 0.38 however it is still approximately 33% lower than the single static case. Despite this large increase in average C_L , the upstream airfoil at $Sd = 2.5$ generates a 66% increase in C_D when comparing to its upstream counterpart at $Sd = 2$ while matching the single static airfoil’s average C_D of 0.24.

With reference to Figure 5-9 (c), one observation is that the upstream airfoil at the larger Sd of 2.5 forms a more defined and elongated CCW shear layer when comparing to the $Sd = 2$ case which supports the more than 100% increase in average C_L at the larger Sd of 2.5. Two red dotted lines are then drawn from the flow separation points to better illustrate the differences in CCW shear layer profiles for both Sd cases as shown in Figure 5-9 (c). The upstream airfoil at $Sd = 2.5$ shows a narrower trailing wake structure than its upstream counterpart at $Sd = 2$ which exhibits a wider trailing wake structure as its CCW shear layer displays a steeper profile gradient as illustrated in Figure 5-9 (c) (1).

Due to the narrower trailing wake structure, the upstream airfoil at $S_d = 2.5$ produces a smaller ‘dead-air’ region than its counterpart at $S_d = 2$ which correlates to lesser ‘wake drag’ forces. Despite the lower induction of ‘wake drag’ forces, the narrower trailing wake give rise to an opposite-signed vortex pairing that is closer in proximity. The closer proximity of the shed CCW and CW vortex pairing coupled with a more defined vortical structure in relation to its upstream counterpart at $S_d = 2$ produces to a more powerful ‘jet force’ vector which points upstream as illustrated by the red arrow in Figure 5-9 (c) (2). This indicates that the upstream airfoil at $S_d = 2.5$ produces a much stronger ‘drag-inducing’ Von Kármán vortex street when comparing to its counterpart at $S_d = 2$, thus generating higher ‘induced-wake’ drag forces. It can be determined that the 66% increase in average C_D for the upstream airfoil as S_d increases from 2 to 2.5 is attributed primarily to the increase in ‘induced-wake’ drag forces due to the closer proximity of the down-washed CW and CCW vortex pairing.

The aerodynamic performance of the downstream airfoil at $S_d = 2.5$ is considered the best among all the tandem static cases tested as it attains the highest $(C_L/C_D)_{\text{total}}$ ratio of 3.5 at $\alpha = +15^\circ$ as presented in Figure 5-8. At the larger S_d of 2.5, this indicates the downstream airfoil’s ability to produce very high average C_L of 0.56 which is comparable to the single static case and generating very low average C_D of 0.03 that is 87% lower than the single static case at $\alpha = +15^\circ$.

In short, the downstream airfoil at $S_d = 2$ experiences two key destructive vortex interactions that cause the decrease in Δp between the downstream airfoil surfaces thus resulting in a reduction in average C_L , namely; (1) the ‘like-signed’ vortex interaction between the down-washed CW vortex and CW shear layer on the pressure side near the LE which decreases the localized surface pressure and (2) the close proximity of the CW interacting vortex near the LE which hinders the reinforcing effect of the CCW ‘like-signed’ vortex interaction at the suction side of the downstream airfoil; causing a decrease in flow velocity and an increase in pressure over the suction side.

With the increase in S_d from 2 to 2.5, there are considerable changes to the effects of the down-washed vortex interactions. Firstly, the shed CW vortex produced by the upstream airfoil loses its strength by the time it reaches the downstream airfoil and it interacts further downstream on the underside of the downstream airfoil as displayed in Figure 5-9 (d) (2). Hence, the decrease in pressure over the pressure side reduces. Secondly, as the shed CW

vortex no longer interacts near the LE, the ‘liked-signed’ CCW vortex interaction between the shed CCW vortex and the CCW shear layer increases the flow velocity over the suction side, thus strengthening the CCW LEV generated which is illustrated by the darker red CCW contours in Figure 5-9 (d) (2). Both these effects of vortex interactions contributes to the increase in Δp between the downstream airfoil surfaces which corresponds to the higher average C_L of 0.56 when comparing to its downstream counterpart at $Sd = 2$.

The downstream airfoil at $Sd = 2.5$ exhibits its ability to suppress drag forces by producing very low average C_D of 0.03 at $\alpha_{da} = +15^\circ$ as seen in Figure 5-7. Figure 5-9 (d) (2) also shows that despite the ‘like-signed’ CCW vortex interaction over the suction side, the flow remains fairly attached which reduces the ‘dead-air’ region. The shed vortex pairing seems to be less structured when compared to its upstream counterparts and exhibits a similar ‘neutral wake’ structure as its downstream counterpart at $Sd = 2$. The reduction in ‘dead-air’ region and the resultant ‘neutral wake’ structure shown in Figures 5-9 (d) (2) give rise to the low C_D generated by the downstream airfoil at the larger Sd of 2.5.

5.2.3 Vorticity Characteristics of In-Phase Static Tandem Airfoils Case at $\alpha = +45^\circ$

Figure 5-10 : Vorticity Contours, $\zeta \text{ (s}^{-1}\text{)}$ for In-Phase Static Tandem Airfoils at $\alpha = +45^\circ$ and at $Re = 8.6 \times 10^3$ for (1) $Sd = 2$ and (2) $Sd = 2.5$

Table 5-1 shows that the tandem static airfoils at $\alpha = +45^\circ$ for both Sd of 2 and 2.5 produce lower average C_L and C_D when comparing to the single static case. The upstream airfoil generates higher average C_L values than its downstream counterparts at both Sd . Meanwhile, the downstream airfoil exhibits its superiority in terms of producing lower average C_D values than its upstream counterpart and the single static case. The $(C_L/C_D)_{total}$ ratios at $\alpha = +45^\circ$ for the static in-phase tandem airfoils at $Sd = 2$ and 2.5 improve by 18% and 28% respectively in relation to the single static case as presented in Figure 5-8. These improvements in $(C_L/C_D)_{total}$ ratios are primarily due to the downstream airfoil's ability to suppress drag force production at $\alpha = +45^\circ$.

Comparing the vorticity contours for the upstream airfoil at $Sd = 2$ as shown in Figures 5-10 (b) (1) to the single static case's vorticity contour at $\alpha = +45^\circ$ in Figure 5-4 (g), the CCW shear layers exhibit similar structure and profile with an approximate maximum vorticity of $\zeta_{max} \approx +0.025$. In addition, the upstream airfoil at $Sd = 2$ and the single static airfoil show similar CCW shear layer flow separation point at approximately, $X_{LE} / c \approx 0.25$. On the other hand, the upstream airfoil at $Sd = 2$ produces a CW shear layer over its pressure side that has an approximately 75% higher maximum ζ than the single static airfoil. This increase in CW

shear layer vorticity indicates an increase in flow velocity, thus resulting in a decrease in pressure over the pressure side. As the CCW shear layer characteristics between the upstream airfoil at $S_d = 2$ and the single static case are similar, this decrease in pressure over the pressure side give rise to the decrease in overall Δp between the upstream airfoil's surfaces which substantiates the 21% decrease in average C_L when comparing to the single static case. This similar effect is documented by Scharpf and Mueller [43] and observed in the static upstream airfoil at $+15^\circ$.

The single static airfoil and upstream airfoil at $S_d = 2$ exhibit differences in its CW shear layer profiles. As highlighted by the white dotted arrows, the CW shear layer profile for the single static airfoil exhibits an upward deflection as seen in Figure 5-4 (g) whereas the upstream airfoil displays a straight trajectory as shown in Figure 5-10 (a) (1). Based on these differences in CW shear layer profiles, the upward deflection of the CW shear layer profile produces a smaller 'dead-air' region which implies a lower induction of 'wake drag' forces by the single static airfoil. However, the single static airfoil produces 29% higher average C_D when comparing to the upstream airfoil at $S_d = 2$ which contradicts the effects of a smaller 'dead-air' region.

Therefore, this indicates that the overall increase in average C_D for the single static case is not due to the 'wake drag' forces but via a different drag force component. Due to the upward deflected CW shear layer profile produced by the single static airfoil, it causes the CCW and CW shears layers to converge as they propagate further downstream as shown in Figure 5-4 (g). The converging of the shear layers induces a 'jet' force vector pointing upstream as illustrated by the red arrow in Figure 5-4 (g) which indicates the contribution of 'induced-wake' drag forces [57] for the single static airfoil. Conversely, for the upstream airfoil at $S_d = 2$, the straight trajectory of the CW shear layer causes the divergence of shear layers instead, thus reducing the effects of this 'jet' force vector and 'induced-wake' drag forces. Importantly, this reduction in 'induced-wake' drag forces supports the 29% reduction in the overall average C_D for the upstream airfoil at $S_d = 2$.

The downstream airfoil at $S_d = 2$ produces the lowest average C_L and C_D when compared to the rest of the static cases at $\alpha = +45^\circ$. The downstream airfoil at $S_d = 2$ generates an average C_L of 0.09 at $\alpha_{da} = +45^\circ$ which is 85.7% and 88.8% lower than its upstream counterpart at $S_d = 2$ and the single static case respectively. The downstream airfoil at $S_d = 2$ also produces the

lowest average C_D of 0.06 which sustains a respectable $(C_L/C_D)_{\text{total}}$ ratio of 0.79 for the tandem in-phase static configuration at $\alpha = +45^\circ$ that is 18% higher than the single static case.

As shown in Figures 5-10 (1), the upstream airfoil-generated CCW shear layer sheds its primary CCW vortex as it propagates downstream. Subsequently, this primary shed CCW vortex breaks down into smaller and weaker vortices to form a large CCW vortex cluster that is circled in Figure 5-10 (c) (1). Importantly, this CCW vortex cluster impinges onto the LE of the downstream airfoil at $S_d = 2$. The bottom half of the cluster as circled in red in Figure 5-10 (d) (1) interacts directly onto the LE and due to the ‘liked-signed’ interaction, it will most likely reinforce the CCW LEV formation over the suction side of the downstream airfoil [44, 45]. However, due to the top half of the cluster that passes over the LE as circled in blue in Figure 5-10 (d) (1), it quickly displaces the CCW LEV formation before it can gain any form of momentum to create the necessary circulation for lift generation. This ‘rapid LEV displacement’ event due to down-washed vortex interactions was also documented by Broering and Lian [44, 45] and may explain the large reduction in average C_L of 85.7% for the downstream airfoil at $S_d = 2$ in relation to its upstream counterpart. One of the secondary effect of this ‘rapid LEV displacement’ event is that the flow over the suction side remains attached without a ‘dead-air’ region as shown in Figure 5-10 (d) (1). Moreover, the downstream airfoil at $S_d = 2$ did not exhibit any CW shear layer formation over its pressure side. All of these observations will substantiate the low average C_D of 0.06 due to negligible contribution of ‘wake-drag’ and ‘induced-wake’ drag forces to the overall average C_D .

At $S_d = 2.5$, the upstream airfoil’s average C_L and C_D results does not differ much from its upstream counterpart at $S_d = 2$ with the exception of a 6% reduction in average C_D . Based on the vorticity contours between the two upstream airfoils in Figure 5-10 (a), the upstream airfoil at $S_d = 2.5$ exhibits a CW shear layer that is deflected upwards towards the end which is similar to the single static case, thus decreasing the ‘dead-air’ region marginally which supports the small 6% reduction in average C_D when comparing to its upstream counterpart at $S_d = 2$.

At the larger S_d of 2.5, the downstream airfoil experiences a down-washed vortex interaction between the CCW shed vortex cluster onto the LE, similar to the $S_d = 2$ case. However, due to the increase in axial spacing, the CCW shed vortex cluster loses some of its vortical strength by the time it interacts with the LE of the downstream airfoil at $S_d = 2.5$. As a

consequence, the detrimental effects associated with the CCW shed vortex cluster interaction reduces for the downstream airfoil at $Sd = 2.5$ which manages to produce more than twice the average C_L of 0.20 when comparing to its downstream counterpart at $Sd = 2$.

The downstream airfoil at $Sd = 2.5$ shows a similar trailing wake structure as its downstream counterpart at $Sd = 2$ that does not have any ‘dead-air’ region due to the attached flow over the suction side and an absent CW shear layer. Thus, the contributions of ‘wake-drag’ forces and ‘induced-wake’ drag forces are kept minimal which may explain the similarly low average C_D of 0.19 as its downstream counterpart at $Sd = 2$ which also equates to a 76% reduction in average C_D in relation to the upstream airfoil at $Sd = 2.5$.

Conclusions for In-Phase Tandem Static Airfoils

1. The static tandem in-phase airfoils demonstrated both detrimental and constructive effects of down-washed vortex interactions on the downstream airfoils’ C_L and C_D results.
2. The down-washed vortex interactions are responsible in decreasing the CCW shear layer’s vortical strength and disrupting CCW LEV formation over the suction side which resulted in the reduction of average C_L for the downstream airfoils.
3. However, the static tandem in-phase airfoils also exhibited the constructive effects of down-washed vortex interactions at the larger Sd of 2.5 where the ‘like-signed’ CCW shed vortex interaction onto the LE reinforces the CCW shear layer formation for the downstream airfoil at $\alpha = +15^\circ$ which increases the average C_L .
4. In terms of C_D , the down-washed vortex interactions ensure that the flow over the suction side of the downstream airfoils remained attached, thus decreasing the ‘dead-air’ region which result in decreasing ‘wake-drag’ forces. Importantly, this demonstrates the benefits of these ‘down-washed vortex’ interactions in reducing overall average C_D up to for the in-phase static downstream airfoils.
5. The influence of these down-washed vortex interaction effects on the downstream in-phase static airfoil reduces with the increase in Sd which was similarly documented by Broering and Lian [44, 45] in their study on effects of axial spacing on tandem airfoils.

5.3 OUT-OF-PHASE STATIC TANDEM AIRFOILS

5.3.1 Average Force Characteristics of Out-of-Phase Static Tandem Airfoils

Figure 5-11 : Average Lift Coefficient, C_L Plot at $Re = 8.6 \times 10^3$ and at $Sd = 2$ for Out-of-Phase and In-Phase Static Tandem Airfoils

Figure 5-12 : Average Drag Coefficient, C_D Plot at $Re = 8.6 \times 10^3$ and at $Sd = 2$ for Out-of-Phase and In-Phase Static Tandem Airfoils

The out-of-phase static tandem configuration consists of the upstream airfoil pitched at various α with its downstream counterpart pitched out-of-phase by $\phi = 180^\circ$ with reference to the upstream airfoil's α . Figures 5-11 and 5-12 compare the average C_L and C_D produced by the out-of-phase tandem static airfoils respectively to the in-phase static tandem airfoils at both positive and negative α which provide the corresponding baselines for the respective out-of-phase tandem static airfoils. For brevity of the present study, this analysis only covers the out-of-phase static tandem airfoils at $Sd = 2$.

Firstly, the out-of-phase static upstream airfoil produces consistently high average C_L values throughout the positive α range tested as displayed in Figure 5-11. For $\alpha_{ua} < +30^\circ$, the out-of-phase upstream airfoil produces average C_L values that are approximately 14% lower than the single static case but consistently higher than the in-phase static upstream airfoil at the same α . As α_{ua} increases from $+30^\circ$ to $+60^\circ$, the out-of-phase upstream airfoil exhibits a sharp linear increase in C_L where it attains its maximum C_L of 1.2 at $\alpha_{ua} = +60^\circ$ which is 67.5% higher than the single static case. At $\alpha_{ua} > +35^\circ$, the upstream airfoil generates the highest average C_L values when comparing to the single static case and the rest of the in-phase static cases as shown in Figure 5-11. From the previous in-phase static cases, a sharp increase in C_L would typically correspond to a proportionally steep increase in C_D but this is not the case for the out-of-phase upstream airfoil. In Figure 5-12, at $\alpha_{ua} > +30^\circ$ where the sharp increase in C_L occurs, the average C_D values remain consistently lower than both the single static case and the in-phase upstream airfoil at positive α . It is important to analyze the vorticity contours to investigate the flow mechanism that is responsible for enhancing C_L and suppressing C_D for the out-of-phase upstream airfoil in this post-static stall region.

The out-of-phase downstream airfoil produces similarly low average C_L value at $\alpha_{da} = -10^\circ$ when comparing to the in-phase downstream airfoil at the same negative α (purple dashed line) as shown in Figure 5-11. However, as α_{da} decreases from -10° to -15° , the out-of-phase downstream airfoil generates a higher average C_L of -0.24 while the in-phase downstream airfoil maintains its average C_L close to zero. As α_{da} continues to decrease from -15° to -60° , the in-phase downstream airfoil increases its negative average C_L to its maximum of -0.37 at $\alpha_{da} = -20^\circ$ before it starts to exhibit a decreasing trend in negative C_L production. In the same α range, the out-of-phase downstream airfoil displays two trends; the first trend is from $-30^\circ < \alpha_{da} \leq -20^\circ$ where the average C_L values matches the in-phase downstream airfoil in negative α and the second trend is from $-60^\circ \leq \alpha_{da} \leq -30^\circ$ where the average C_L values matches the in-phase upstream airfoil in negative α (solid purple line) as it generates a steady increase in

negative C_L . As a consequence, the out-of-phase downstream airfoil attains its maximum negative C_L of -0.66 at $\alpha_{da} = -60^\circ$ which equates to 6 times more than the negative C_L produced by the in-phase static downstream airfoil. The two trends showcase the out-of-phase downstream airfoil's ability to generate higher negative average C_L at $\alpha_{da} = -15^\circ$ and to produce a continuous increase in negative average C_L at $\alpha_{da} < -30^\circ$ when comparing to the in-phase downstream airfoil at negative α .

The out-of-phase downstream airfoil shows a gradual linear increasing C_D trend in Figure 5-12 that corresponds well with its linear increase in negative C_L production shown in Figure 5-11. In addition, the out-of-phase downstream airfoil produces consistently higher average C_D values when comparing to the in-phase static downstream airfoils at both positive and negative α . Positively, the out-of-phase downstream airfoil produces much lower average C_D than all of the upstream airfoils. As α decreases from $\alpha_{da} = -30^\circ$ to $\alpha_{da} = -60^\circ$ where the C_L trend shows a continuous increase in negative C_L production, the out-of-phase downstream airfoil produces a small increase in C_D which differs from the other downstream airfoils where drag production typically decreases in a steep gradient as shown in Figure 5-12.

Figure 5-13 : $(C_L/C_D)_{total}$ Ratio for In-Phase and Out-of-Phase Static Tandem Airfoils at $Sd = 2$

Figure 5-13 compares the $(C_L/C_D)_{\text{total}}$ ratios for the out-of-phase static tandem airfoils with the in-phase static tandem airfoils at both positive and negative α and the single static case. This non-dimensional $(C_L/C_D)_{\text{total}}$ ratio enables the identification of the operating α for these static tandem airfoils for optimized performance.

1. As shown in Figure 5-13, the out-of-phase static tandem configuration exhibits much lower $(C_L/C_D)_{\text{total}}$ ratio than the single static case and in-phase tandem configuration at positive α throughout most of the α tested.
2. However, the out-of-phase static tandem airfoils attain its highest $(C_L/C_D)_{\text{total}}$ of 1.6 at its smallest α of $\pm 10^\circ$ which equals the single static case's C_L/C_D ratio and equates to more than double the $(C_L/C_D)_{\text{total}}$ produced by the in-phase static tandem configuration at positive α . This is primarily attributed to the out-of-phase static upstream airfoil's ability to produce the highest average C_L of 0.47 which is 17.5% higher than the single static case while suppressing average C_D to 0.176 that corresponds to 27% reduction in relation to the single static case.
3. As α increases from 15° to 60° , the out-of-phase static tandem configuration displays a stable $(C_L/C_D)_{\text{total}}$ ratio averaging at 0.42. Despite the steep increase in average C_L production at $\alpha_{\text{ua}} > +40^\circ$ by the out-of-phase upstream airfoil, its downstream counterpart's linear increase in negative average C_L decreases the combined $C_{L(\text{total})}$ for the out-of-phase tandem configuration thus resulting in the $(C_L/C_D)_{\text{total}}$ ratio plateau seen between $\alpha = 40^\circ$ to 60° in Figure 5-13.

5.3.2 Vorticity Characteristics of Out-of-Phase Static Tandem Airfoils Case at $\alpha_{ua} = +15^\circ$ and $\alpha_{da} = -15^\circ$

Figure 5-14 : Vorticity Contours for Out-of-Phase ($\phi = 180^\circ$) Static Tandem Airfoils at $\alpha_{ua} = +15^\circ$ and $\alpha_{da} = -15^\circ$

Table 5-2 : Average Force Coefficients for Out-of-Phase and In-Phase Static Tandem Airfoils at $\alpha = \pm 15^\circ$ and $\alpha = \pm 45^\circ$ with $S_d = 2$

α ($^\circ$)	Average Force Coeff.	Upstream Airfoil, Out-of-Phase, $+\alpha$	Downstream Airfoil, Out-of-Phase, $-\alpha$	Upstream Airfoil, In-Phase, $+\alpha$	Downstream Airfoil, In-Phase, $+\alpha$	Single Static Airfoil, $+\alpha$
15°	C_L	0.37	-0.24	0.16	-0.01	0.57
	C_D	0.33	0.11	0.15	-0.03	0.24
45°	C_L	1.05	-0.56	0.63	0.09	0.80
	C_D	0.81	0.24	0.84	0.06	1.19

Based on the average force measurement analysis on the out-of-phase static tandem airfoils, the out-of-phase upstream airfoil demonstrated its ability to produce high average C_L values for most of the α tested and to suppress C_D especially at larger α_{ua} of $\geq +30^\circ$. In addition, the out-of-phase static downstream airfoil showcased its ability to generate higher negative C_L than its in-phase static downstream counterpart at $\alpha_{da} = -15^\circ$ and $\alpha_{da} < -30^\circ$. In order to investigate these key observations on the out-of-phase static tandem airfoils, the present study will analyse the vorticity contours at $\alpha = 15^\circ$ and 45° . Table 5-2 tabulates the average force coefficients for the out-of-phase static tandem airfoils for these two α cases in comparison with the in-phase static tandem airfoils at positive α and the single static case.

Table 5-3 : Vortex Shedding Model Parameters on Static Cases for $\alpha_{ua} = +15^\circ$

Vortex Shedding Model on Static Cases					
Static Cases at $\alpha = +15^\circ$	Vortex	V_{vs} (m/s)	Vortex Diameter, d (m)	Shed Vortex Circulation, Γ_{vs} (m^2/s)	Combined Shed Vortex Circulation, Γ_{total_vs} (m^2/s)
Single Static Airfoil	Vortex A	0.040	0.011	0.0014	0.0019
	Vortex B	0.0132	0.012	0.0005	
In-Phase Tandem Static Upstream Airfoil	Vortex A	0.041	0.015	0.0019	0.0022
	Vortex B	0.0082	0.012	0.0003	
Out-of-Phase Tandem Static Upstream Airfoil	Vortex A	0.040	0.019	0.0024	0.0045
	Vortex B	0.055	0.012	0.0021	

The Vortex Shedding Model provides an approximation method to evaluate the ‘jet force’ vectors induced by the Von Kármán vortex street through employing the shed vortex circulation formulas provided by van den Berg and Ellington [85]. The Vortex Shedding Model methodology is elaborated in Section 5.7. In order to analyze and explain the out-of-phase upstream airfoil’s aerodynamic performance, the Vortex Shedding Model will be

employed. For this Vortex Shedding analysis, Figures 5-4 (c), 5-9 (b) (1) and 5-14 (c) are selected for the single static airfoil, in-phase static upstream airfoil and out-of-phase static upstream airfoil respectively. In these selected vorticity contours, the red and blue curved arrows illustrate the shed CCW vortex labelled as ‘A’ and the CW shed vortex labelled as ‘B’ respectively. These curved arrows are not drawn to scale and are simply for illustration. The Vortex Shedding Model parameters are computed using the same method specified in Section 5.7 and are presented in Table 5-3. In order to evaluate the overall strength of the ‘jet force’ vector which is represented by the red arrows in Figures 5-4 (c), 5-9 (b) (1) and 5-14 (c), the combined shed vortex circulation, $\Gamma_{\text{total_vs}}$ is calculated by summing the individual shed vortex circulation, Γ_{vs} from Vortex A and Vortex B.

Figures 5-4 (c), 5-9 (b) (1) and 5-14 (c) exhibit CCW shear layer of similar vortical strength between the single static airfoil and the upstream static airfoils. However, with reference to the calculated shed CCW vortex circulations presented in Table 5-3, the out-of-phase upstream static airfoil produces the highest Γ_{vs} of 0.0024 which is 71% and 26% higher than the single static case and in-phase upstream airfoil respectively. The powerful generation of CCW shed vortex by the out-of-phase upstream airfoil at $\alpha_{\text{ua}} = +15^\circ$ indicates that the initial LEV formation over the suction side is equally strong. If the LEV formation for the out-of-phase upstream airfoil is the strongest, then one would expect the average C_L to be higher than the single static airfoil but this is not the case. Another crucial factor in lift generation is the overall Δp between the airfoil sides, thus it is imperative to take into account of the CW shear layer formation over the pressure side. The out-of-phase upstream static airfoil also produces the strongest shed CW vortex with the highest Γ_{vs} of $0.0021 \text{ m}^2/\text{s}$ in relation to the single static case and in-phase upstream static airfoil as shown in Table 5-3. This stronger CW shear layer formation indicates that the flow velocity increases and pressure decreases over the pressure side, thus decreasing the overall Δp across the airfoil sides which in turn decreases the overall average C_L for the out-of-phase upstream static airfoil. The strong CCW shear layer formation may explain the high average C_L of 0.37 produced by the out-of-phase upstream static airfoil which is more than double that produced by the in-phase static upstream airfoil. However, due to the strengthening of the CW shear layer, the Δp across the airfoil sides decreases thus resulting in the 35% reduction in average C_L for the out-of-phase upstream static airfoil when comparing to the single static case.

For the average C_D evaluation, the out-of-phase static upstream airfoil’s trailing wake shown in Figure 5-14 (c) is compared to the trailing wakes generated by the single static case and in-

phase upstream static airfoil displayed in Figures 5-4 (c) and 5-9 (b) (1) respectively. Firstly, the out-of-phase static upstream airfoil exhibits a similar sized ‘dead-air’ region when comparing to the in-phase static upstream airfoil. This indicates that both in-phase and out-of-phase static upstream airfoils generate similar levels of ‘wake-drag’ forces at $\alpha_{ua} = +15^\circ$. Critically, the out-of-phase static upstream airfoil produces the strongest CCW and CW shed vortex pairings with the highest Γ_{vs} of $0.0024\text{m}^2/\text{s}$ and $0.0021\text{m}^2/\text{s}$ respectively. This also results in the highest combined shed vortex pairing circulation, $\Gamma_{total_vs} = 0.0045\text{m}^2/\text{s}$ which relates to more than double when comparing to both the in-phase static upstream airfoil and the single static airfoil as shown in Table 5-3. With the highest Γ_{total_vs} , the out-of-phase upstream static airfoil produces the strongest ‘jet force’ vector (red arrow) that points upstream as illustrated in Figure 5-14 (c) in comparison with the in-phase static upstream airfoil and single static case. As the variation between the CCW and CW shed vortex circulation is very small at $0.0003\text{m}^2/\text{s}$, the ‘jet force’ vector points upstream at a very small angle of inclination with reference to the X-axis as drawn in Figure 5-14 (c). This indicates that the ‘induced-wake’ drag component which acts in the +X-direction does not reduce significantly through the resolution of the resultant ‘jet force’ vector. Conclusively, due to the production of powerful and fast CCW and CW shed vortices coupled with the small variation between the shed vortices’ circulations, the out-of-phase upstream static airfoil produces the strongest ‘jet force’ vector that acts primarily in the +X-direction thus supporting the very high average C_D of 0.33 which equates to 37.5% and 120% higher than the single static case and the in-phase static upstream airfoil at $\alpha_{ua} = +15^\circ$ respectively.

As displayed in Figure 5-14 (a), the out-of-phase static downstream airfoil shows a large CW shed vortex interacting onto the pressure side at approximately $0.24c$ from the LE which is circled in blue. Due to the ‘like-signed’ vortex interaction between the CW shed vortex and CW shear layer over the out-of-phase static downstream airfoil, the effect is a constructive one as this interaction accelerates the localized flow velocity which decreases the pressure over the pressure side [44, 45]. This decrease in pressure over the pressure side due to the CW shed vortex interaction may explain the increase in $-\Delta p$ which give rise to the increase in negative C_L for the out-of-phase static downstream airfoil. This also supports the out-of-phase static downstream airfoil’s ability to generate a higher negative average C_L of -0.24 than the in-phase static downstream airfoil which produces a low average C_L of 0.007 at $\alpha_{da} = -15^\circ$. This ‘like-signed’ vortex interaction effect was also similarly observed in Figure 5-9 (b)

(1) for the in-phase static downstream airfoil at $\alpha_{da} = +15^\circ$ which was presented in Section 5.2.

As shown in Figure 5-14 (d), there is a medium-sized CCW shed vortex that interacts with the suction side of the out-of-phase downstream static airfoil at approximately $0.24c$ from the LE. There is no apparent CCW LEV formation over the suction side of the out-of-phase downstream static airfoil which may possibly be due to the suction side's profile at $\alpha_{da} = -15^\circ$. Importantly, due to the absence of any CCW LEV formation, this 'like-signed' vortex interaction between the CCW shed vortex and the suction side does not exhibit a huge influence in affecting the negative C_L production for the out-of-phase downstream static airfoil. This may also indicate that for a 'like-signed' vortex interaction to reinforce lift production, it depends greatly on the interaction location, the pre-existing boundary flow conditions before interaction and the proximity with other shed vortices.

However, as a result of the CW shed vortex interaction with the suction side of the out-of-phase downstream airfoil observed in Figure 5-14 (d), it strengthens and elongates the CCW shear layer which starts developing only near the TE with an initiation point measuring $0.76c$ from the LE. Due to the elongated and strengthen CCW shear layer structure, the out-of-phase downstream static airfoil is able to create a small 'dead-air' region as highlighted in Figure 5-14 (b). When comparing to the trailing wake produced by the in-phase static downstream airfoil at $\alpha_{da} = +15^\circ$ shown in Figure 5-9 (a) (1), the out-of-phase static downstream airfoil manages to produce a larger 'dead-air' region due to the elongated CCW shear layer thus indicating a higher generation of 'wake-drag' forces. This may explain the higher average C_D of 0.11 produced by the out-of-phase downstream static airfoil in relation of the in-phase static downstream airfoil at $\alpha_{da} = +15^\circ$.

5.3.3 Vorticity Characteristics of Out-of-Phase Static Tandem Case at $\alpha_{ua} = +45^\circ$ and $\alpha_{da} = -45^\circ$

Figure 5-15 : Vorticity Contours for Out-of-Phase Tandem Static Airfoils at $\alpha_{ua} = +45^\circ$ and $\alpha_{da} = -45^\circ$ with $Sd = 2$

In the previous in-phase static tandem analysis at $\alpha = +45^\circ$, the in-phase static upstream airfoil at $S_d = 2$ exhibited 21% lower average C_L than the single static airfoil due to the CW shear layer strengthening effect which decreases the overall Δp hence decreasing C_L . In addition, it also generated a 29% reduction in average C_D when comparing to the single static case due to the flatter CW shear layer profile which resulted in the decrease effect of ‘induced-wake’ drag forces. The in-phase static downstream airfoil at $S_d = 2$ and at $\alpha_{da} = +45^\circ$ generated the lowest average C_L and C_D when comparing to its counterparts which are attributed to the larger CCW shed vortex cluster interaction at its LE which promotes rapid LEV displacement thus reducing its average C_L by 88.75% in relation to the single static case. As a consequence of this CCW shed vortex cluster interaction, there is an absence of any shear layer formation in the trailing wake of the in-phase static downstream airfoil which indicates negligible ‘wake-drag’ and ‘induced-wake’ drag forces thus supporting the massive 95% average C_D reduction in relation to the single static case. With these main findings from the in-phase static tandem analysis, it will be interesting to see if the out-of-phase static tandem case at $\alpha = 45^\circ$ offers any potential aerodynamic benefits and to evaluate the differences in its down-washed vortex interactions.

Figure 5-15 (d) shows that the out-of phase static upstream airfoil produces a CW shear layer of similar vortical strength of $\zeta_{\max} \approx -0.02$ as the single static case at $\alpha = +45^\circ$ displayed in Figure 5-4 (g). Figure 5-15 (d) also demonstrates that the out-of-phase static upstream airfoil generates a stronger CCW shear layer with $\zeta_{\max} \approx +0.035$ which equates to a 40% increase in maximum vorticity when comparing with the single static case. Throughout the in-phase static tandem cases, all of the upstream airfoils exhibited stronger CW shear layers than the single static case which resulted in the reduction of Δp between the airfoil surfaces and the consequential decrease in C_L production. For this case, due to out-of-phase static upstream airfoil’s ability to produce a CW shear layer of comparable strength and a 40% stronger CCW shear layer, it is able to attain a higher Δp across the airfoil surfaces, thus producing a very high average C_L of 1.05 which relates to a 31% increase in relation to the single static case. This also makes the out-of-phase static upstream airfoil the best lift producer at $\alpha = 45^\circ$ when comparing to its static counterparts as shown in Table 5-2.

As illustrated by the white dotted arrow in Figure 5-15 (b), the out-of-phase static upstream airfoil displays a CW shear layer with a slight upward-curved profile which is similar to the single static case at $\alpha = +45^\circ$ shown in Figure 5-4 (g). On the other hand, the in-phase static

upstream airfoil exhibits a CW shear layer with a straight trajectory as shown in Figure 5-10 (a) (1). The key difference in the trailing wakes for these static upstream airfoils and single static airfoil lies in the CCW shear layer profiles. Figure 5-15 (b) clearly illustrates that the out-of-phase static upstream airfoil produces a CCW shear layer structure with a ‘diverging’ profile which is similar to its in-phase static upstream counterpart in Figure 5-10 (a) (1). This ‘diverging’ CCW shear layer is evidently different from the ‘converging’ CCW layer profile displayed by the single static case in Figure 5-4 (g). Taking into account all these variations in shear layer profiles, the ‘induced-wake’ and ‘wake-drag’ contributions will also vary between these upstream static airfoils and single static airfoil.

As the out-of-phase static upstream airfoil produces an identical CCW shear layer profile in relation to the in-phase static upstream airfoil, the slight upward-curved CW shear layer profile exhibited by the out-of-phase static upstream airfoil give rise to a marginally smaller ‘dead-air’ region and lower ‘induced-wake’ drag forces. This supports the 3.6% reduction in average C_D when comparing to the in-phase static upstream airfoil at $\alpha_{ua} = +45^\circ$. In addition, due to the ‘diverging’ CCW shear layer profile, the out-of-phase static upstream airfoil exhibits a ‘diverging’ trailing wake structure when comparing to the ‘converging’ wake structure exhibited by the single static airfoil. Importantly, this means that the out-of-phase static upstream airfoil produces a much weaker ‘jet force’ vector that acts in the ‘gap’ in between the shear layers thus producing lower ‘induced-wake’ drag forces than the single static case. The lower induction of ‘induced-wake’ drag forces due to the ‘diverging’ wake structure may explain the 29.4% average C_D reduction exhibited by the out-of-phase static upstream airfoil when comparing to the single static case.

As circled in Figure 5-15 (a), the out-of-phase static downstream airfoil displays one primary down-washed vortex interaction where a large CCW shed vortex cluster interacts near the TE of the downstream airfoil at approximately $0.25c$ from the TE. This CCW shed vortex cluster is similar to one produced by the in-phase static upstream airfoil at $\alpha_{ua} = +45^\circ$ shown in Figure 5-10 (c) (1) but instead of interacting with the downstream airfoil’s LE, the CCW shed vortex cluster interacts with the out-of-phase static downstream airfoil’s TE. For the in-phase static tandem case, this shed vortex cluster interaction was detrimental to C_L production as it promotes rapid LEV displacement. However, for this out-of-phase static tandem case, this interaction causes some sort of a ‘flow reversal’ depicted as a small CW vortex on suction surface which is circled in Figure 5-15 (b). This ‘flow reversal’ region indicates that due to

the CCW shed vortex cluster interaction, the flow over the interaction area slows down and even reverses its flow direction causing an increase in pressure in that localized interaction region thus increasing the overall $-\Delta p$ over the airfoil surfaces and consequential increase in negative C_L production for the out-of-phase static downstream airfoil at $\alpha_{da} = -45^\circ$. Importantly, the CCW shed vortex cluster interaction with the downstream airfoil acts as a lift-enhancing mechanism that sustains the increasing negative average C_L trend at $\alpha_{da} < -30^\circ$ for the out-of-phase static downstream airfoil as shown in Figure 5-11.

In Figure 5-15 (c), the out-of-phase static downstream airfoil manages to produce weakly defined shear layer formations despite the down-washed vortex interactions. Due to these shear layers; it is able to form a considerable sized ‘dead-air’ region. As the shear layers are not well-defined, the ‘wake drag’ and ‘induced-wake’ drag forces induction by the out-of-phase static downstream are not as high as its out-of-phase static upstream counterpart. The in-phase static downstream airfoil at $\alpha_{da} = +15^\circ$ as shown in Figure 5-10 (1) does not even exhibit any formation of shear layers and the presence of the ‘dead-air’ region. Hence, the fact that the out-of-phase static downstream airfoil is able to sustain its shear layer formations and produce a ‘dead-air’ region supports the higher average C_D of 0.24 which equates to triple the average C_D produced by the in-phase static downstream airfoil at $\alpha_{da} = +15^\circ$.

Conclusively, through this in-depth investigation on tandem static airfoils at various S_d of 2 and 2.5 and at various phase angles, $\phi = 0^\circ$ and 180° , the present study have validated Scharpf and Mueller [43] conclusion that the α of the individual airfoils is critical in influencing the resultant aerodynamic performance of the tandem configuration. In addition, the present study have also documented other key influencing factors such as the strength of the CCW shear layer on positive average C_L and the shear layer characteristics such as profile, structure and vortical strength in influencing the ‘wake-drag’ and ‘induced-wake’ drag force production. Most of the upstream static airfoils produced lower average C_L than the single static airfoil due to the ‘strengthening effect’ of its CW shear layer which decreases the overall Δp across the airfoil surfaces and thus decreasing average C_L production. In terms of down-washed vortex interactions, the interaction location, pre-existing boundary layer conditions and proximity to other shed vortices are key factors in determining whether that particular down-washed vortex interaction with the downstream airfoil is constructive or detrimental to its lift production.

5.4 SINGLE OSCILLATING AIRFOIL

5.4.1 Average Force Characteristics of a Single Oscillating Airfoil

One of the primary objectives of the present study is to investigate on the aerodynamic performance and the associated unsteady flow characteristics of oscillating or pitching tandem airfoils and its potential benefits over the single oscillating airfoils. Hence, the single oscillating NACA4421 airfoil is first analyzed to provide the necessary baseline and to understand the fundamental flow characteristics during an oscillating cycle.

Section 2.1.1.2 reviewed the key critical flow events and the aerodynamic performance of a single oscillating NACA0012 airfoil at $Re_c = 1.35 \times 10^5$ at both pre-static stall, near static stall and post-static stall conditions from Lee and Gerontakos [60] study. During the pre-static stall conditions, the single NACA0012 oscillating airfoil exhibited a fully attached flow throughout the cycle that was insensitive to changes in α . In addition, it also showed that the flow over the airfoil experienced two boundary-layer events; transition and relaminarization of the flow which were delayed non-linearly with the increase in α when compared to the single static case. The effects of reduced frequency, κ were also investigated and it was documented that with increasing κ , the onset of transition and relaminarization would be delayed and accelerated respectively.

During the near static stall conditions, the single NACA0012 pitching airfoil exhibited a different sequence of flow events as it demonstrated flow reversal and the subsequent turbulent breakdown near the top of oscillation which initiated a vortex-like circulation that did not have sufficient time to grow as it was quickly dissipated by the pitch-down motion. The single NACA0012 pitching airfoil then experienced a lift stall event that was primarily caused by rapid trailing-edge dynamic stall.

For the post static stall condition or the deep stall condition, the single pitching NACA0012 airfoil demonstrated the full sequence of critical flow events starting with the onset and upward spread of flow reversal, turbulent breakdown and LEV initiation. Subsequently, the LEV growth and rearward convection towards the TE were observed. The onset of dynamic or lift stall was seen before the first stroke reversal which was ensued by a full stall event right after the first stroke reversal. Consequently, the onset of secondary vortices and flow reattachment transpired.

The average force coefficients for the single oscillating airfoil at various α ranging from $\pm 10^\circ$ to $\pm 60^\circ$ at $Re = 8.6 \times 10^3$ will be analyzed in this section. As defined in Section 4.3.1, for all of the present oscillating cases, the rotational speed, ω is kept relatively constant at $93^\circ/s$ hence, the flapping frequency, f and reduced frequency, κ decreases as α increases as shown in Table 4-4. The κ values presented in Table 4-4 for the present study are substantially higher than the highest κ of 0.1 that was tested by Lee and Gerontakos [60], thus demonstrating the higher degree of flow unsteadiness and flapping frequencies, f .

Table 5-4 : Average Force Coefficients for Single Oscillating Airfoil and Single Static Airfoil

Pitch Amplitude, $\pm \alpha^\circ$	Single Oscillating Airfoil Average C_L	Single Static Airfoil Average C_L	Single Oscillating Airfoil Average C_D	Single Static Airfoil Average C_D
10	0.23	0.40	0.35	0.24
15	0.44	0.52	-0.11	0.24
20	0.47	0.56	-0.10	0.46
30	0.47	0.72	-0.09	0.63
40	0.49	0.80	-0.13	0.91
45	0.38	0.80	0.11	1.19
60	0.49	0.72	0.48	1.45

Figure 5-16 : (a) Average Lift Coefficient, C_L and (b) Average Drag Coefficient, C_D versus α for Single Oscillating NACA4421 Airfoil at $Re = 8.6 \times 10^3$

The average force coefficients, C_L and C_D for the single oscillating NACA4421 airfoil are tabulated in Table 5-4 and displayed in Figure 5-16 where the single static airfoil's results are shown for comparison.

The single oscillating airfoil generates lower average C_L across the range of α tested when comparing to the single static case as shown in Figure 5-16 (a). The single oscillating case also produces lower average C_D values across the tested α range with the exception of the smallest pitch amplitude case at $\alpha = \pm 10^\circ$. As displayed in Figure 5-16 (b), the single oscillating airfoil demonstrates its ability to produce negative drag forces or thrust forces between $\alpha = \pm 15^\circ$ to $\pm 40^\circ$ before linearly increasing in average C_D as α increases to $\pm 60^\circ$. On the other hand, the single static case was never able to generate negative drag forces or thrust forces across the tested α range as it exhibits an increasing average C_D trend with the increase in α as shown in Figure 5-16 (b).

In order to describe the average C_L and C_D characteristics of the single oscillating airfoil, the plots in Figure 5-16 can be sub-divided into 3 regions; (1) pre-static stall conditions where $\alpha < \pm 15^\circ$, (2) during static stall conditions where $\pm 15^\circ \leq \alpha < \pm 20^\circ$ and (3) post-stall conditions where $\pm 20^\circ \leq \alpha \leq \pm 60^\circ$.

In the pre-static stall conditions where $\alpha < \alpha_{ss} = +15^\circ$, the single oscillating airfoil starts off by producing considerably low average C_L of 0.23 at $\alpha = \pm 10^\circ$ which is 42.5% lower than the single static case as displayed in Figure 5-16 (a). Interestingly at $\alpha = \pm 10^\circ$, the single oscillating airfoil produced the second highest average C_D of 0.35 across the tested α range which equates to a 46% drag increment in relation to the single static case as shown in Figure 5-16 (b). At the smallest tested α of $\pm 10^\circ$, it corresponds to the highest $f = 2.33\text{Hz}$ which meant that the single oscillating airfoil may not have sufficient time to form any LEV to generate substantial lift and that the extensive flow unsteadiness may also explain the high average C_D .

As α increases to $\pm 15^\circ$, the single oscillating airfoil exhibits a 90% lift increment to an average C_L of 0.44 which is only 15% lower than its static single case. Moreover, the single oscillating airfoil starts to produce negative drag forces of $C_D = -0.11$. As defined in Section 3.5, this negative drag force production indicates that the single oscillating airfoil starts generating positive thrust forces at $\alpha = \pm 15^\circ$ which act in the opposite $-X$ direction in relation to the flow direction.

During the static stall conditions where $\pm 15^\circ \leq \alpha < \pm 20^\circ$, the single oscillating airfoil exhibits a similar average C_L trend when comparing to the single static case as observed in Figure 5-16 (a). With this small range of α , the single oscillating airfoil generates a local maximum of average C_L of 0.475 at $\alpha \approx \pm 17^\circ$ before gradually decreasing to an average C_L of 0.47 at $\alpha = \pm 20^\circ$. This peak in average C_L was previously observed for the single static airfoil where it correlates to the occurrence of static stall. Based on Lee and Gerontakos [60], they documented that during static stall conditions, a similar increase in lift production may be attributed to the partial LEV formation before it gets rapidly dissipated due to stroke reversal. Hence, the vorticity contours presented in the subsequent section will provide the necessary justification on this increase in lift during the static stall conditions. In addition, this increase in average C_L corresponds to an equally small decrease in average C_D as the single oscillating airfoil attains its local minimum of -0.115 at $\alpha \approx \pm 17^\circ$. This decrease in average C_D by the single oscillating airfoil differs from the single static case where the increase in C_L during the static stall conditions corresponds to an increase in average C_D as shown in Figure 5-16 (b).

In the post-static stall conditions where α increases from $\pm 20^\circ$, the single oscillating airfoil sustains its maximum average C_L of 0.47 and produces a consistent average C_D of -0.11 up till $\alpha = \pm 40^\circ$. This shows that the single pitching airfoil is able to sustain reasonably high lift and consistent thrust forces, thus demonstrating a viable propulsive system in this α range of between $\pm 20^\circ$ to $\pm 40^\circ$.

However, when α increases from $\pm 40^\circ$ to $\pm 45^\circ$, it disrupts the sustainment of this maximum average C_L as lift production decreases by 22% to an average C_L of 0.38 which also corresponds to a proportional increase in average C_D to 0.11. This sharp decrease in average C_L and increase in average C_D may be attributed to the oscillating airfoil experiencing dynamic stall event leading to a full stall which was highlighted by Lee and Gerontakos [60].

As α continues to increase from $\pm 45^\circ$ to the largest α of $\pm 60^\circ$, the average C_L recovers as it increases to 0.49. Despite this recovery in average C_L , the single oscillating airfoil produces a massive increase in drag production as attains its maximum average C_D of 0.48 at $\alpha = \pm 60^\circ$. This maximum average C_D produced by the single oscillating airfoil is still 3 times lower than the maximum average C_D of 1.45 attained by the single static case at $\alpha = +60^\circ$. Lee and Gerontakos [60] revealed that for an oscillating airfoil in deep stall conditions, it will experience a fully separated flow and followed by a full stall event which may explain the large increase in drag production as seen in this case as α increases from $\pm 45^\circ$ to $\pm 60^\circ$.

5.4.2 Single Oscillating Airfoil Case at $\alpha^\circ = \pm 15^\circ$

(a)

(b)

Figure 5-17 : (a) Instantaneous C_L Plot and (b) Instantaneous C_D Plot for Single Oscillating NACA4421 Airfoil at $\alpha = \pm 15^\circ$ and $Re = 8.6 \times 10^3$; Green Dotted Lines (a) to (g) illustrate the instantaneous force coefficients that correlate to the instantaneous vorticity contours in Figure 5-18

Figure 5-18 : Vorticity Contours for One Oscillating Cycle of a Single NACA4421 Airfoil at $\alpha = \pm 15^\circ$ and $Re = 8.6 \times 10^3$

Based on the earlier analysis on the average force coefficients, several key findings were documented such as the generation of thrust forces and sustainment of positive lift forces from $\alpha = \pm 15^\circ$ till $\pm 40^\circ$ and the abnormal drop in C_L and its corresponding increase in C_D at $\alpha = \pm 45^\circ$. In order to provide the necessary information behind these key findings, the instantaneous force coefficient plots and the vorticity contours at specific instantaneous time will be presented and discussed. For the brevity of the present study, two experimental cases

at $\alpha = \pm 15^\circ$ and $\alpha = \pm 45^\circ$ will be presented as these two pitching amplitudes will provide the essential coverage on the flow characteristics of the single oscillating airfoil. Figures 5-17 (a) and (b) show the instantaneous C_L and C_D plots respectively for one oscillating cycle at $\alpha = \pm 15^\circ$ where $f = 1.47\text{Hz}$ and at $Re = 8.6 \times 10^3$. Green dotted vertical lines labelled from (a) to (g) are drawn on these instantaneous force plots which represent the points in the cycle that correlate to instantaneous vorticity contours that are similarly labelled in Figures 5-18 (a) to (g).

At the start of the upstroke at $t/T = 0$, Figure 5-18 (a) at $\alpha_u = 0^\circ$ shows an attached flow over the suction side with a slight indication of onset of flow reversal which is identified by the small CCW circulation near the TE at $s/c = 0.82$ as circled in red. This observation agrees well with Lee and Gerontakos [60] where they noted that the onset of flow reversal typically starts near the TE at $s/c \approx 0.88$ as a thin layer at the bottom of the thickened turbulent boundary layer. In addition, there is an absence of any substantial Leading-Edge Vortex (LEV) formation on the suction side which supports the slow build-up in generating positive C_L at the start of the cycle as shown in Point (a) in Figure 5-17 (a).

Figure 5-18 (a) shows three shed vortices in the trailing wake consisting of two CCW shed vortices; labelled as CCW_1 and CCW_2 and one large CW shed vortex labelled as CW_1 . Both shed CCW vortices form their individual shed vortex pairing with the CW shed vortex, CW_1 . Importantly, each of these shed vortex pairings induces different ‘jet force’ vectors as depicted by the black arrows in Figure 5-18 (a). Firstly, the vortex pairing of CW_1 and CCW_1 forms a ‘jet force’ vector labelled as Vortex A which points in the downstream direction thus indicating a ‘thrust producing’ reverse Von Kármán vortex street [42, 48 and 51]. On the other hand, the second shed vortex pairing of CW_1 and CCW_2 induces a ‘jet force’ vector labelled as Vortex B that points in the upstream direction which indicates a ‘drag producing’ Von Kármán vortex street [42, 48 and 51] as illustrated in Figure 5-18 (a). Assuming that the effects of proximity of these shed vortex pairings are minimal, the differences in the maximum core vorticities of these shed vortices will have an effect on their ‘jet force’ vectors. To illustrate this point, Vector A will have a substantially higher magnitude than Vector B as the CCW_1 vortex has a higher maximum vorticity, $\zeta_{\max}$ of 0.04 which is double the maximum vorticity, $\zeta_{\max}$ of 0.02 produced by CCW_2 vortex. Consequently, this indicates that trailing wake structure will have a resultant ‘jet force’ vector

acting in the downstream direction as Vector A is stronger than Vector B, thus supporting the thrust production of $C_D = -0.4$ at Point (a) in Figure 5-17 (b).

As the single oscillating airfoil continues to pitch upwards, Figure 5-18 (g) at $\alpha_u = +6.67^\circ$ shows the initiation of a CCW vortex-like circulation over the suction side with an approximate length of $0.24c$ at $s/c = 0.67$ as circled in red. As α increases to $\alpha_u = +12.7^\circ$ as shown in Figure 5-18 (b), this CCW vortex circulation rapidly move downstream and interacts with the TEV that is already spilled over the trailing edge. The linear increase in C_L to its $C_{Lmax} \approx 6$ from $t/T = 0.10$ to 0.25 in Figure 5-17 (a) is probably attributed to the initiation and rearwards propagation of this CCW vortex-like circulation over the suction side which is similarly documented by Lee and Gerontakos [60].

In both Figures 5-18 (b) and (g), the trailing wakes exhibit a pair of well-defined alternating shed vortices that are orientated to display the ‘thrust-producing’ Reverse Von Kármán vortex streets [42, 48 and 51]. Koochesfahani [42] has extensively studied on the vortical patterns on the oscillating NACA0012 airfoil to ascertain the relationship between the oscillation frequency and amplitude on the resultant wake structure. In his study, he documented the production of similar Reverse Von Kármán vortex streets that corresponds to positive thrust generation that was computed through the normalization of force with the freestream dynamic head and airfoil chord [42]. The production of these ‘thrust-producing’ Reverse Von Kármán vortex streets will explain the low levels of thrust forces or negative drag forces during the 1st half of the upstroke from $t/T = 0$ to 0.25 where the single oscillating airfoil produces its minimum C_D of -1.8 at $t/T \approx 0.13$ as shown in Figure 5-17 (b).

With reference to the region between Point (g) at $t/T = 0.08$ and Point (b) at $t/T = 0.16$ in Figure 5-17 (b), there is a ‘turning’ point at $t/T \approx 0.13$ where there is a change in C_D gradient which indicates that at $t/T > 0.13$ to 0.3 , C_D starts to increase linearly. Both Figures 5-18 (g) and (b) show the formation and attachment of the TEV at the TE and the apparent downstream propagation of the CCW vortex-like circulation towards the TE. As the TEV exhibits very little change between Points (g) and (b), it can be assumed that as the CCW vortex-like circulation propagates downstream towards the TE, its interaction with the spilled TEV shown in Figure 5-18 (b) may explain the ‘turning’ point observed at $t/T \approx 0.13$ where positive C_D production starts to increase.

Based on Lee and Gerontakos [60], for the deep stall oscillating cases, dynamic stall or lift stall occurred near the end of the upstroke and is associated with the detachment of the LEV and a consequential reduction in C_L . After the stroke reversal as the airfoil pitches down, the LEV is spilled over the TE and the flow is completely separated as the airfoil experienced full stall [60]. During this full stall event, C_L and C_D were observed to decrease quite dramatically. However, for light stall cases such as in this case at $\alpha = \pm 15^\circ$, the flow events near the end of the upstroke are quite different from the deep stall cases. Lee and Gerontakos [60] highlighted that the lift stall is primarily caused by the rapid trailing edge dynamic stall but did not elaborate much on of the boundary layer events or on its effects on aerodynamic forces. Hence, the present study aims to provide the necessary insights to the flow events associated with the rapid trailing edge dynamic stall for this light stall case at $\alpha = \pm 15^\circ$.

In Figure 5-18 (c) where the airfoil has started to pitch down at $t/T = 0.36$ with $\alpha_d \approx 11.7^\circ$, the TEV is shed due to the rapid change in α during the stroke reversal. The behavior of the CCW vortex-like circulation in Figure 5-18 (c) will explain the difference between a typical LE dynamic stall to a rapid trailing edge dynamic stall. For this case, the CCW vortex-like circulation is analogous to a typical CCW LEV formed over the suction side. Unlike the LE dynamic stall event, the CCW LEV is unable to grow as it rapidly propagates downstream towards the TE and spills over the TE during its first stroke reversal as circled in Figure 5-18 (c). In addition, this CCW LEV remains attached over the suction side without any flow separation which differs significantly from the LE dynamic stall event. This is similar to the findings for light stall conditions by Lee and Gerontakos [60].

As the CCW LEV spills over the TE, C_L starts to rapidly decrease from its maximum of 6 to $C_L = 2$ at $t/T = 0.36$ as shown in Figure 5-17 (a). This drastic reduction in lift production is a similar effect of a typical LE dynamic stall. However, the spilled CCW LEV remains attached to the TE up till $t/T \approx 0.5$ which delays stall effects as positive lift production is sustained from $t/T = 0.36$ till 0.5. Figure 5-18 (d) shows an increase in core vorticity for the CCW LEV in relation to Figure 5-18 (c) which indicates that as the CCW LEV remains attached to the TE, it actually gains vortical strength and increases in size. Due to this CCW LEV growth after the 1st stroke reversal, the average pressure distribution across the suction side continues to decrease which may explain the sustainment of positive C_L of an average of 1.9 from $t/T = 0.3$ to $t/T = 0.5$ as observed in Figure 5-17 (a).

As the single oscillating airfoil continues to pitch into the negative α range at $t/T > 0.5$ to 0.75, the CCW LEV is finally shed into the trailing wake and forms an alternate vortex pairing with the shed CW TEV as displayed in Figures 5-18 (d) and (e). These alternate vortex pairings are orientated to produce ‘jet-like’ thrust producing Reverse Von Kármán vortex streets [42, 48 and 51] as indicated by the ‘jet-force’ vectors pointing downstream shown in Figures 5-18 (d) and (e). These Reverse Von Kármán vortex streets were made possible due to the delayed shedding of the CCW LEV after the 1st stroke reversal as the shed CCW LEV is orientated favorably in relation to the shed CW TEV. Importantly, the formation of these thrust-producing vortex streets supports the increase production in negative drag forces or thrust forces during the 2nd half of the downstroke from $t/T = 0.5$ to 0.75 as the single oscillating airfoil attains its minimum C_D of -2.7 at $t/T \approx 0.65$ before the 2nd stroke reversal as shown in Figure 5-17 (b).

During the run-up to the 2nd stroke reversal at $t/T = 0.75$, the single oscillating airfoil experiences similar flow events as the downstroke such as the initiation and rearwards convection of the CW LEV or vortex-like circulation over the pressure side instead as circled in Figures 5-18 (d) and (e) respectively. Similar to the run-up to the 1st stroke reversal, here, the initiation and rearwards propagation of the CW LEV over the pressure side give rise to the steady increase in negative lift forces as the single oscillating airfoil produces its maximum negative C_L of -4.2 at $t/T \approx 0.75$ during its 2nd stroke reversal.

As α increases to $\alpha_u = -8^\circ$ at $t/T = 0.91$ as shown in Figure 5-18 (f), the single oscillating airfoil experiences a similar rapid trailing edge dynamic stall which is supported by the attached CW LEV that displaces downstream and spills over the TE after the 2nd stroke reversal. The decrease in negative C_L after the 2nd stroke reversal is not as steep when comparing to the decrease in positive C_L after the 1st stroke reversal at $t/T = 0.25$ as displayed in Figure 5-17 (a). This indicates that the effects of the second rapid TE dynamic stall are not as intensive as the first. Given the fact that the trailing wakes in Figures 5-18 (e) and (f) consistently exhibit the ‘jet-like’ thrust producing wake pattern, the 2nd peak in C_D of 1.8 at $t/T = 0.85$ may be attributed to the effects of the 2nd rapid TE dynamic stall.

The above analysis on the single oscillating airfoil at $\alpha = \pm 15^\circ$ demonstrates the critical flow events starting with the onset of flow reversal, the initiation of the vortex-like circulation or LEV, the rearwards propagation of the LEV and interaction with the TEV in the run up to

stroke reversal, the spillage and attachment of the LEV at the TE after stroke reversal and the rapid TE dynamic stall. The generation of positive average C_L of 0.44 is attributed to the single oscillating airfoil's ability to sustain lift in the 1st half of the downstroke. This lift sustainment is attributed to the delayed shedding of the CCW LEV which delayed stall effects. The low average negative C_D of -0.11 is accredited to consistent production of 'jet-like' thrust producing Reverse Von Kármán vortex streets especially so during the 2nd half of the downstroke. The delayed CCW LEV shedding after the 1st stroke reversal give rise to the formation of these Reverse Von Kármán vortex streets as elaborated earlier.

5.4.3 Single Oscillating Airfoil Case at $\alpha^\circ = \pm 45^\circ$

(b)

Figure 5-19 : (a) Instantaneous C_L Plot and (b) Instantaneous C_D Plot for Single Oscillating Airfoil at $\alpha = \pm 45^\circ$ and $Re = 8.6 \times 10^3$; Green Dotted Lines (a) to (j) illustrate the instantaneous force coefficients that correlate to the instantaneous vorticity contours in Figure 5-20

Figure 5-20 : Vorticity Contours for One Oscillating Cycle of a Single NACA4421 Airfoil at $\alpha = \pm 45^\circ$ and $Re = 8.6 \times 10^3$

For this deep stall case, the instantaneous force coefficients and vorticity contours for the single oscillating airfoil at $\alpha = \pm 45^\circ$ as shown in Figures 5-19 and 5-20 respectively will be presented and discussed. For deep stall cases at low Reynolds number flows, the oscillating airfoil will experience the full sequence of flow events that was presented in Section 5.4.1.

In Figure 5-20 (a) at $t/T = 0.017$ where the airfoil pitches upwards to $\alpha_u = +4.77^\circ$, the flow over the suction side is fully attached without any apparent vorticity contours thus supporting the low C_L which is close to zero as shown in Point (a) in Figure 5-19 (a). The trailing wake exhibits a relatively small and weak CCW vortex positioned in the middle of two well-defined CW shed vortices as displayed in Figure 5-20 (a). These two large CW shed vortices exhibit strong core vorticities of $\zeta_{\max} > -0.045$ and were shed during the previous half of the upstroke from $t/T = 0.75$ to 1.0 in Figures 5-20 (i) and (j). The CW shed vortex labelled as CW_LEV_1 in Figure 5-20 (a) is the primary LEV formed by the previous downstroke. Typically with an oscillating airfoil at deep stall conditions such as in this case, the primary LEV shedding is succeeded by the onset and convection of secondary vortices [60]. Hence, the 2nd CW shed vortex labelled as CW_LEV_{s1} seen in Figure 5-20 (a) is the first secondary CW vortex shed. At this point (a) in Figure 5-19 (b), the single oscillating airfoil produces low levels of thrust or negative drag forces that may be attributed to the presence of this shed vortex cluster in the trailing wake. Applying with the Von Kármán vortex street momentum concept documented by Kármán and Burgers [51], this vortex cluster may increase the momentum in the fluid and consequentially increase thrust generation. Koochesfahani [42] did study the vortical patterns produced by a oscillating airfoil but did not extensively researched on the drag/thrust generation relating to cluster vortex shedding as he primarily focused on the typical alternating shed vortex pairings such as the Von Kármán vortex street.

Figure 5-21 : Smoke-Flow Visualization of Turbulent Breakdown for Single Pitching NACA0012 Airfoil [60]

As the single oscillating airfoil continues to pitch upwards to $\alpha_u = +30^\circ$ in Point (b) at $t/T = 0.11$, the flow over the suction side has undergone flow reversal which is deduced by the thickening of the turbulent CCW boundary layer over the suction side as highlighted in Figure 5-20 (b). The oscillating airfoil is experiencing turbulent breakdown where the turbulent boundary layer fluctuates rapidly both upstream and downstream [60]. In order to validate this point, Figure 5-20 (b) is compared with Figure 5-21 which details the smoke-flow visualization from Lee and Gerontakos [60] work of a single oscillating NACA0012 airfoil at $\alpha_u = +21.8^\circ$ that was experiencing turbulent breakdown event. The similarities between these two images are apparent as they exhibit comparable boundary layer thickness and turbulent nature of the boundary layer. Importantly, both Figures 5-20 (b) and 5-21 show an indication of LEV initiation though at different locations on the suction side. This LEV initiation is induced by the turbulent breakdown event [60]. The transition from flow reversal in Point (a) to turbulent breakdown and LEV initiation in Point (b) gives rise to the steady increase in C_L shown in Figure 5-19 (a). This increase in C_L also agrees well with Lee and Gerontakos [60] dynamic loop force results presented in Figure 2-9 from Points (1) to (3). The second secondary CW vortex, CW_LEV_{s2} is in the process of being shed while the trailing wake maintains its cluster of two CW shed vortices coupled with the small CCW shed vortex as displayed in Figure 5-20 (b). With this introduction of CW_LEV_{s2} , the vortex cluster provides an increase in momentum to the flow which may explain the slight increase in negative C_D at Point (b) when compared to Point (a) shown in Figure 5-19 (b). This increase in negative C_d at this turbulent breakdown and LEV initiation point is not observed in Lee and Gerontakos [60] results in Figure 2-9 as their experiments employed hot-film measurements and the integration of surface pressure measurements to obtain the aerodynamic forces. Their experimental methods were ideal for determining C_L but may not have taken account of vortex shedding effects in the trailing wake which affects the C_D production.

As the single oscillating airfoil approaches the end of its upstroke at $t/T = 0.21$ in Point (c) where $\alpha \approx +43.7^\circ$, there are a couple of important changes to the boundary layer flow structure over the suction side and in the trailing wake structure that will be discussed. Firstly, the CCW_LEV_1 over the suction side in Figure 5-20 (c) exhibits an increase in size and vortical magnitude when comparing to Figure 5-20 (b). In addition, there is an indication that the CCW_LEV_1 is propagating downstream towards the TE supported by the elongation of the CCW boundary layer downstream of the CCW_LEV_1 observed in Figure 5-20 (c). The

CCW_LEV₁ growth and downstream propagation over the suction side give rise to the increase in C_L from Point (b) to Point (c) as shown in Figure 5-19 (a) as it induces the increase in overall Δp between the airfoil surfaces [60]. Moreover, the CCW boundary layer demonstrates an early indication of flow separation as is circled in Figure 5-20 (c). As α increases, the effects of adverse pressure gradients becomes increasingly dominant causing this initiation of flow separation and a more turbulent boundary layer [77] which explains the increase in form drag and the consequential increase in C_D from Points (b) to (c) in Figure 5-19 (b). The slight dip in C_L and corresponding local peak in C_D at $t/T = 0.14$ may indicate the onset of this flow separation event. These observations on the CCW LEV growth and rearward convection coupled with early signs of LEV detachment due to the initiation of flow separation agree well with Lee and Gerontakos [60] deep stall results.

The trailing wake in Figure 5-20 (c) exhibits the dispersion of the shed vortex cluster and as the spacings between these vortices increase, its thrust enhancing property also diminishes. Despite the decrease in thrust contribution by the shed vortex cluster, it still manages to suppress the increase in the overall C_D as the single oscillating airfoil approaches its first stroke reversal from Points (b) to (c). Koochesfahani [42] documented that with cases with large α , there is a high probability of generating a more stable vortex wake structure that consists of 3 or more ‘like’ signed vortices shed per half cycle of the oscillation. Hence, the stable trailing wake structure at $t/T = 0.21$ shown in Figure 5-20 (c) which consists of three CW shed vortices (CCW_LEV₁, CCW_LEV_{s1} and CCW_LEV_{s2}) and one small CCW_TEV₁ shed vortex would agree very well with Koochesfahani’s findings [42].

From $t/T = 0.21$ in Point (3) to $t/T < 0.25$ (just before stroke reversal), the single oscillating airfoil exhibits a slight decrease in C_L which may be attributed to the full detachment of the CCW_LEV₁ as the CCW boundary layer separates. In addition, C_D increases linearly at this stage due to the increase in form drag as a result of the flow separation. Following the LEV detachment, it is then convected rearwards toward the TE. This sequence of flow events essentially encapsulates the occurrence of dynamic stall.

As a result of the rapid change in α during the 1st stroke reversal, the detached CCW_LEV₁ spills over the TE as observed in Figure 5-20 (d). Despite the change in stroke direction, the spilled CCW_LEV₁ remains attached to the TE. As documented in the single oscillating case at $\alpha = \pm 15^\circ$, this prolonged attachment to the TE provides the CCW boundary layer (that is

still moving rearwards) the opportunity to reinforce the CCW_LEV₁. As a consequence, the single oscillating airfoil continues to increase C_L and C_D production even after the first stroke reversal as shown in Figure 5-19 thus demonstrating its ability to delay full stall effects. Full stall is typically characterized by the sudden decrease in C_L which corresponds to a peak in C_D as well [60].

The single oscillating airfoil experiences full stall in the region of $t/T = 0.33$ and $t/T = 0.38$ in Point (d) when the CCW boundary layer experiences a complete flow separation from the suction side as shown in Figure 5-20 (d) which accurately corresponds to a steep decrease in C_L and a peak in C_D of 2.35 at $t/T \approx 0.36$. Figure 5-20 (d) also shows the interaction between the shed CCW_LEV₁ and CW_TEV₁ which creates a shed vortex pairing that is orientated side by side. Due to this orientation, it induces a ‘jet force’ vector that points directly upwards in the +y direction as indicated by the black arrow in Figure 5-20 (d) which signifies zero momentum deficit or excess therefore drag or thrust force generation through the vortex shedding effects would be close to zero [48 and 51]. Hence, it can be determined that the high peak in C_D of 2.35 at $t/T \approx 0.36$ after the first stroke reversal is contributed by the pressure drag induced through the aggressive nature of the flow separation during full stall rather than through vortex shedding effects.

As the airfoil pitches into its negative α range in the 2nd half of the downstroke, Figure 5-20 (e) to (g) exhibit the initiation, growth and rearwards propagation of the CW_LEV₁ which is responsible for the steady increase in negative C_L from $t/T = 0.5$ to 0.7. Subsequently, dynamic stall occurs near the end of the downstroke at $t/T \approx 0.7$ where negative C_L starts to decrease which corresponds to a sharp peak in C_D . Figure 5-20 (e) also shows the reattachment of the separated CCW boundary layer over the suction side while Figures 5-20 (f) and (g) exhibit the shedding of CCW secondary vortices. Due to the generation and shedding of more powerful CCW_LEV₁, its secondary vortices and the CW_TEV₁ in this 2nd half of this downstroke, the single oscillating airfoil produces higher average negative drag or thrust forces than during the 1st half of the upstroke.

As the single oscillating airfoil approaches its 2nd stroke reversal at $t/T = 0.75$, the CW boundary layer over the pressure side in Figure 5-20 (h) separates more in relation to Figure 5-20 (g) which may explain the sudden increase in C_D from $t/T = 0.72$ till after $t/T = 0.75$ observed in Figure 5-19 (b). After the 2nd stroke reversal where $t/T = 0.91$ and $\alpha_u \approx -24.6^\circ$ in

Figure 5-20 (i), the spilled CW_LEV_1 is still attached to the TE indicating prolonged LEV attachment that was similarly observed during the downstroke. This supports the delayed full stall effects seen during this 2nd half of the upstroke where full stall occurs only at $t/T \approx 0.8$ when the airfoil produces a local minimum C_L of -4.2 and a corresponding maximum C_D of 0.2 shown in Figures 5-19 (a) and (b) respectively. However, the main difference is that the C_D is much lower during the upstroke due to the lower flow velocity and turbulence over the pressure side during the 2nd half of the upstroke thus, indicating less aggressive full stall effects when compared to the 1st half of the cycle. As the airfoil pitches upwards towards the start of the next cycle in Figure 5-20 (j), the separated CW boundary layer over the pressure side reattaches and C_L returns to near zero due to the absence of LEVs and consequential circulation. Meanwhile, the CW_LEV_1 is finally shed into the trailing wake and the onset of its first secondary vortex shedding can be observed in Figure 5-20 (j) which adds momentum to the fluid in the trailing wake thus inducing relatively high levels of negative drag or thrust forces from Points (i) to (j) shown in Figure 5-19 (b).

The above analysis has shown all of the critical flow events for deep stall conditions. It demonstrated the single oscillating airfoil's ability in delaying full stall effects by prolonging the shedding of the detached LEV which sustained positive C_L during the 1st half of the downstroke. It was ascertained that the increase in average C_D at $\alpha = \pm 45^\circ$ was attributed to the aggressive flow separation event during full stall in the 1st half of the downstroke. Vortex shedding effects do aid in generating thrust forces but its contribution in suppressing positive drag forces during full stall is minimal.

5.5 IN-PHASE OSCILLATING TANDEM AIRFOILS

Table 5-5 : Average Force Coefficients for In-Phase Tandem Oscillating Airfoils at $S_d = 2$ and 2.5 for $\alpha = \pm 15^\circ$ and $\pm 45^\circ$

α (°)	Average Force Coeff.	Upstream Airfoil at $S_d = 2$	Downstream Airfoil at $S_d = 2$	Upstream Airfoil at $S_d = 2.5$	Downstream Airfoil at $S_d = 2.5$	Single Oscillating Airfoil
$\pm 15^\circ$	C_L	0.21	0.44	0.31	0.24	0.44
	C_D	0.14	0.19	0.27	0.35	-0.11
$\pm 45^\circ$	C_L	0.37	0.72	0.45	0.50	0.38
	C_D	-0.21	0.68	0.16	0.58	0.11

The next two sections in this chapter aims to comprehensively study the effects of stagger distances, S_d and gap distances, G for the in-phase, $\phi = 0^\circ$ tandem oscillating airfoils. The out-of-phase, $\phi = 180^\circ$ tandem oscillating airfoils will be covered in the final results section. The average force coefficients results will be presented first, followed by the correlation of the vorticity contours to its instantaneous force results at $\alpha = \pm 15^\circ$ and $\pm 45^\circ$. The average force coefficients results for these two cases at $\alpha = \pm 15^\circ$ and $\pm 45^\circ$ are tabulated in Table 5-5.

Lee [18] had experimentally studied tandem NACA0012 airfoils undergoing sinusoidal oscillations at various S_d of 1.3, 1.6 and 2.6 and at $\phi = 0^\circ$ and 180° . Both of the tandem airfoils were oscillated with $\alpha(t) = 8^\circ + 5^\circ \sin(\omega t)$, at $Re_c = 8.5 \times 10^4$ and at $\kappa = 0.05$. Lee [18] has provided the basis of comparison for the present study, however, it is important to note that the oscillating experiments in the present study are carried out at a higher κ than 0.05 with the lowest κ at 0.093 for the $\alpha = \pm 60^\circ$ case and the tandem airfoils were oscillated with a different sinusoidal kinematic motion, $\alpha(t) = \alpha^\circ \sin(\omega t)$. This meant that the tandem airfoils in Lee's study oscillated at a far lower f and did not oscillate into the negative α range with a maximum α of only $+13^\circ$.

Nevertheless, Lee [18] study have demonstrated the effects of stagger distance, S_d and phase angles, ϕ in tandem oscillating configuration in determining the strength of undesirable interference effects on the downstream airfoils' aerodynamic performance. The boundary layer events on the downstream airfoil were also consistently different when compared to the baseline oscillating airfoil. The downwash generated by the upstream airfoil disrupted the LEV formation of the downstream airfoil thus decreasing its C_L production. For in-phase, $\phi = 0^\circ$ tandem oscillating cases, the downstream airfoil's C_L improved with the increase in S_d and the opposite trend was exhibited with the out-of-phase, $\phi = 180^\circ$ tandem oscillating cases which also outperformed its in-phase counterpart.

The numerical studies on the effect of stagger distance on tandem wing aerodynamics by Broering and Lian [44, 45] were reviewed in Section 2.2.1 where the tandem wings were oscillated with a $\phi = 0^\circ$ in a coupled heaving and pitching motion at a Re of 5000 and at various S_d of 2.0, 1.5, 1.25 and 1.1. Broering and Lian [44, 45] main focus was to investigate on any correlations between vortex interactions between the tandem airfoils and the force histories. Even though, the kinematic motion is different from the present study, they have provided a solid foundation in understanding down-washed vortex interactions and its effects on aerodynamic performance of the downstream airfoil.

5.5.1 Average Force Measurements for In-Phase Oscillating Tandem Airfoils at Various Stagger Distances

Figure 5-22 : In-Phase Tandem Oscillating Airfoils at $S_d = 2$ and 2.5 ; (a) Average C_L Plot and (b) Average C_D Plot

Figures 5-22 (a) and (b) show the average C_L and C_D plots respectively for in-phase, $\phi = 0^\circ$ tandem oscillating NACA4421 airfoils at $S_d = 2$ and 2.5 which are being compared to single oscillating case. Based on Figure 5-22 (a), the in-phase upstream airfoil at $S_d = 2$ exhibits consistently lower average C_L when comparing to the single oscillating case across the range of α tested. The in-phase upstream airfoil at $S_d = 2$ shows a similar average C_L trend to the single oscillating case from $\alpha = \pm 10^\circ$ to $\pm 45^\circ$ and produces identical average C_L values as the single oscillating case at $\alpha = \pm 40^\circ$ and $\alpha = \pm 45^\circ$. The upstream airfoil at $S_d = 2$ attains its maximum C_L of 0.47 at $\alpha = \pm 40^\circ$ before it decrease linearly as α increases to $\pm 60^\circ$.

In Figure 5-22 (b), the in-phase upstream airfoil at $S_d = 2$ displays a similar ‘U-shaped’ average C_D trend when comparing to the single oscillating airfoil. It also generates the highest C_D of 0.51 at the smallest α of $\pm 10^\circ$ in relation to the rest of the cases. Similar to the single oscillating case, the upstream airfoil at $S_d = 2$ exhibits a steady linear decrease in average C_D as α increases from $\alpha = \pm 10^\circ$ to $\pm 45^\circ$. Despite this reduction in average C_D , at α between $\pm 15^\circ$ to $\pm 20^\circ$, the upstream airfoil at $S_d = 2$ still produces positive C_D values whereas the single oscillating airfoil has started producing negative drag or positive thrust forces in this pre-static stall α range. As α increases from $\pm 30^\circ$ to $\pm 60^\circ$, the upstream airfoil at $S_d = 2$ is able to generate the lowest C_D values when comparing to the rest. In addition, it demonstrates its ability to produce thrust forces at $\pm 30^\circ \leq \alpha < \pm 50^\circ$ averaging at C_D of -0.15 and is the only airfoil to exhibit thrust apart from the single oscillating case.

The in-phase downstream airfoil at $S_d = 2$ exhibits the best lift performance in relation to the rest of the oscillating cases as shown in Figure 5-22 (a). It outperforms the single oscillating case by producing higher C_L values across the tested α range. However, it generates approximately the same average C_L of 0.44 at $\alpha = \pm 15^\circ$ and exhibits the same ‘stabilized’ range as the single oscillating case. The ‘stabilized’ range is between $\alpha = \pm 20^\circ$ to $\alpha = \pm 40^\circ$ where C_L production is more or less constant. In this ‘stabilized’ range, the in-phase downstream airfoil produces an average C_L of 0.60 which is approximately 28% higher than the average C_L of 0.47 achieved by the single oscillating airfoil. As α increases from $\pm 40^\circ$ to $\pm 45^\circ$, the in-phase downstream airfoil continues to produce 15.5% higher C_L as it attains its $C_{L_{max}}$ of 0.72 whereas the single oscillating case exhibits a 22% drop in C_L which corresponds to a proportional increase in average C_D . Subsequently, the in-phase downstream airfoil was able to sustain its $C_{L_{max}}$ as α increases to $\pm 60^\circ$.

The downstream airfoil at $S_d = 2$ exhibits a poor drag performance as it generates consistently higher C_D values when comparing to the other tandem oscillating airfoils as shown in Figure 5-22 (b). It also demonstrates a different average C_D trend in relation to its upstream counterpart and the single oscillating airfoil as it shows a relatively linear increasing trend rather than the ‘U-shaped trend with the increase in α . However, the in-phase downstream airfoil manages to generate the lowest average C_D of 0.07 at the smallest $\alpha = \pm 10^\circ$ when comparing to the rest. Another observation is the downstream airfoil exhibits a 13% decrease in C_D at $\alpha = \pm 45^\circ$ which corresponds to a 15.5% increase in average C_L that might provide an indication of one of the down-washed vortex interactions effects on the downstream airfoil.

As S_d increases from 2 to 2.5, the in-phase upstream airfoil exhibits similar average C_L trend as its upstream counterpart at $S_d = 2$ where it attains its maximum C_L of 0.5 at $\alpha = \pm 40^\circ$ which equates to a 6.4% C_L increment in relation to its upstream counterpart. The in-phase upstream airfoil at $S_d = 2.5$ also produces consistently higher average C_L values than its upstream counterpart at $S_d = 2$ at pre-stall conditions at $\alpha < \pm 20^\circ$ and at $\alpha > \pm 40^\circ$. Despite these lift improvements at a larger S_d , the upstream airfoil at $S_d = 2.5$ still generates lower average C_L values than the single oscillating airfoil throughout the tested α range. This detrimental effect on average C_L is similarly observed by the upstream counterpart at $S_d = 2$ but reduces in its effect as S_d increases from 2 to 2.5.

The in-phase upstream airfoil at $S_d = 2.5$ produces relatively higher C_D when comparing to both the single oscillating case and its upstream counterpart at $S_d = 2$. However, it generates consistently lower C_D than all of the downstream airfoils. In terms of trending, the in-phase upstream airfoil at $S_d = 2.5$ exhibits a distinct trend. In the pre-stall region where $\alpha \leq \pm 20^\circ$, the upstream airfoil at $S_d = 2.5$ starts by producing one of the lowest average C_D of 0.12 at $\alpha = \pm 10^\circ$ before increasing steadily as α increases to $\alpha = \pm 20^\circ$. On the other hand, the in-phase airfoil at $S_d = 2$ starts with generating the highest average C_D of 0.51 at $\alpha = \pm 10^\circ$ before decreasing as α increases. As the upstream airfoil at $S_d = 2.5$ oscillates to $\alpha > \pm 20^\circ$, it displays a similar average C_D trend as its upstream counterpart at $S_d = 2$ and attains a minimum of 0.07 at $\alpha = \pm 40^\circ$ which is approximately 150% higher the C_{Dmin} produced by the upstream airfoil at $S_d = 2$. Despite not generating any thrust or negative drag forces at deeper stall conditions with $\alpha > \pm 30^\circ$, the upstream airfoil at $S_d = 2.5$ does manage to produce comparable average C_D values to the single oscillating airfoil at $\alpha \geq \pm 45^\circ$.

For the downstream airfoil at $Sd = 2$, it is apparent that the presence of its upstream counterpart and the down-washed vortex interactions result in an increase in average C_L and C_D across most of α cases in relation to the single oscillating case and its upstream counterpart at $Sd = 2$. Hence, with the increase in Sd from 2 to 2.5, the downstream airfoil is expected to exhibit similar increase in average C_L and C_D but to a lesser extent. Based on the average C_L and C_D plots in Figures 5-22 (a) and (b), this speculation is true to a certain degree. The downstream airfoil at $Sd = 2.5$ shows C_L improvements but only for deep stall cases at $\alpha \geq \pm 30^\circ$ where it achieves its maximum of 0.62 which relates to a 14% decrease in average C_L when comparing to its downstream counterpart at $Sd = 2$. Another observation for these deep stall cases is that the downstream airfoil at $Sd = 2.5$ generated a 13.2% drop in average C_L as α increases from $\pm 40^\circ$ to $\pm 45^\circ$ which also corresponds to a 8.4% increase in average C_D . This sudden drop in C_L and corresponding increase in C_D was also observed in the single oscillating airfoil case at the same α range. Based on the previous analysis at $\alpha = \pm 45^\circ$, these changes in force production were attributed to the aggressive nature of the stall events which led to intense flow separation and a more turbulent boundary layer structure. It will be worth investigating whether these same causes are responsible for these changes in force production for the downstream airfoil at $Sd = 2.5$.

As Sd increases from 2 to 2.5, the downstream airfoil produces lower average C_D values than its downstream counterpart at $Sd = 2.5$ especially so for $\alpha > \pm 20^\circ$. However, at pre-stall condition where $\alpha < \pm 20^\circ$, the downstream airfoil at $Sd = 2.5$ generates consistently higher C_D than its downstream counterpart at $Sd = 2$. At $\alpha = \pm 10^\circ$, the downstream airfoil at $Sd = 2.5$ generates an average C_D of 0.35 which is nearly 4 times more than its downstream counterpart at $Sd = 2$. Based on Broering and Lian [44, 45], the increase in Sd typically results in a decrease in intensity of the down-washed vortex interactions that decreases positive drag. Hence this large increase in average C_D exhibited by the downstream airfoil at $\alpha < \pm 20^\circ$ as Sd increases from 2 to 2.5 is abnormal. This will justify an in-depth investigation on its flow structure and the effects of down-washed vortex interactions on its critical flow events associated with pre-stall conditions.

Conclusively, these in-phase tandem oscillating airfoils that can be categorized and summarized into two conditions; (1) the pre-stall conditions at $\alpha \leq \pm 20^\circ$ and (2) the deep-stall conditions at $\alpha \geq \pm 30^\circ$.

At pre-stall conditions where $\alpha \leq \pm 20^\circ$

1. The upstream airfoil at both Sd exhibits similar average C_L and C_D trends but generates lower average C_L and higher C_D than the single oscillating airfoil. As Sd increases from 2 to 2.5, the upstream airfoil produces consistently higher average C_L of up to a maximum of 237% increment at the smallest $\alpha = \pm 10^\circ$. In addition, the upstream airfoil at the larger Sd of 2.5 generates higher average C_D than its upstream counterpart at Sd = 2 only with the exception of the $\alpha = \pm 10^\circ$ case.
2. The downstream airfoil exhibits similar increasing average C_L trends with comparable C_L values as the single oscillating case at Sd = 2. However, as Sd increases from 2 to 2.5, it produces lower C_L values throughout the pre-stall α range. The downstream airfoil at both Sd generates higher C_D than the single oscillating airfoil except for at $\alpha = \pm 10^\circ$ where it attains the lowest C_D of 0.07 at Sd = 2. As Sd increases from 2 to 2.5, it displays consistently high C_D averaging at 0.47 across this pre-stall α range.
3. Generally, the downstream airfoil tend to produce higher average C_D and is relatively unaffected by changes in α as Sd increases. This also demonstrates that the influence of stagger distances on the downstream airfoil's aerodynamic performance in these pre-stall conditions which agrees well with Broering and Lian [44, 45] findings on the effects of axial spacings for these in-phase tandem oscillating airfoils.

At deep stall conditions where $\alpha \geq \pm 30^\circ$

4. The upstream airfoil at both Sd exhibits similar average C_L and C_D trends as the single oscillating case with the exception of at $\alpha \geq \pm 40^\circ$ where it shows a decreasing average C_L trend rather than the increasing trend exhibited by the single oscillating airfoil. As Sd decreases to 2, the upstream airfoil is able to generate consistently lower drag forces than the single oscillating airfoil in these deep stall conditions.
5. The downstream airfoil at both Sd generates consistently higher average C_L and C_D than the single oscillating case and its upstream counterparts throughout this deep stall α range. As Sd decreases to 2, it generates an increase in average C_L and a corresponding increase in average C_D in this deep stall conditions. At $\alpha = \pm 45^\circ$, the downstream airfoil exhibits different changes in force production at the various Sd thus, justifying an in-depth investigation which may provide new insights to vortex interaction effects.

5.5.2 In-Phase Oscillating Tandem Airfoils Case at $S_d = 2$ and 2.5 for $\alpha = \pm 15^\circ$

(a)

(b)

Figure 5-23 : (a) Instantaneous C_L Plot and (b) Instantaneous C_D Plot for In-Phase Oscillating Tandem Airfoils at $S_d = 2$ and 2.5 at $Re = 8.6 \times 10^3$ for $\alpha = \pm 15^\circ$; Green Dotted Lines from (a) to (g) illustrate the instantaneous force coefficients that correlate to the instantaneous vorticity contours in Figure 5-24

Figure 5-24 : Vorticity Contours for One Oscillation Cycle for In-Phase Oscillating Tandem Airfoils at $Sd = 2$ and 2.5 for $\alpha = \pm 15^\circ$

The flow events during the 1st half of the upstroke from $t/T = 0$ to 0.25 are characterized by referencing the vorticity contours in Figures 5-24 (a), (b) and (g) for the tandem oscillating airfoils which are compared with the vorticity contours in Figures 5-22 (a), (b) and (g) for the single oscillating case at $\alpha = \pm 15^\circ$. The upstream airfoil at both Sd exhibit similar CCW boundary layer structures in terms of vortical strength, elongation and turbulent boundary layer thickness at the TE throughout the 1st half of the upstroke as shown in Figures 5-24 (a), (b) and (g). As the α increases from $\alpha_{u,a,u} = 0^\circ$ to $\pm 12.7^\circ$ for Points (a) to (b), the upstream airfoil at $Sd = 2$ and 2.5 form partial CCW LEVs that propagate downstream towards the TE as illustrated by the thickening of the CCW boundary layers near the TE in Figure 5-24 (b). The upstream airfoil at both Sd exhibit comparable formation and rearwards propagation of the partial CCW LEVs which supports the similar rate of increase in C_L from Points (a) to (b) in Figure 5-23 (a). However, it is observed that the single oscillating airfoil's partial CCW LEV propagates at a faster rate towards the TE as illustrated by the faster development and thickening of the CCW boundary layer as shown in Figures 5-18 (a) to (b) when comparing to Figures 5-24 (a) to (b). This may explain the steeper rate of increase in C_L from Points (a) to (b) exhibited by the single oscillating airfoil in relation to the upstream airfoil at both Sd as shown in Figure 5-23 (a).

Table 5-6: Vortex Shedding Model Parameters at $t/T = 0.16$ for $\alpha = \pm 15^\circ$

Cases	Shed Vortex	Vortex Diameter, d (m)	Vortex Shedding Axial Velocity, V_{vs} (m/s)	Vortex Shedding Circulation, Γ_{vs} (m^2/s)
Single Oscillating Airfoil	CW_1	0.015	0.164	0.008
	CCW_1	0.013	0.138	0.006
	CW_2	0.014	0.069	0.003
Upstream Airfoil at $Sd = 2$	CW_1	0.012	0.054	0.002
	CCW_1	0.015	0.077	0.004
	CW_2	0.013	0.077	0.003
Upstream Airfoil at $Sd = 2.5$	CW_1	0.012	0.115	0.004
	CCW_1	0.008	0.115	0.003
	CW_2	0.012	0.077	0.003

Table 5-7 : 'Jet-Force' Vector Characteristics at $t/T = 0.16$ for $\alpha = \pm 15^\circ$

Cases	Shed Vortex	Vector ₁ Magnitude	Vector ₁ Direction	Vector ₂ Magnitude	Vector ₂ Direction	Resultant 'Jet Vector' Magnitude, V_{JF}	Instantaneous C_D	Drag to 'Jet-Force' Vector Ratio
Single Oscillating Airfoil	CW_1	-0.013	Downstream (Thrust Producing Wake)			-0.022	-1.5	68
	CCW_1			-0.009	Downstream (Thrust Producing Wake)			
	CW_2							
Upstream Airfoil at $Sd = 2$	CW_1	0.006	Downwards (Neutral Wake)			-0.007	-0.5	74
	CCW_1			-0.007	Downstream (Thrust Producing Wake)			
	CW_2							
Upstream Airfoil at $Sd = 2.5$	CW_1	0.007	Downwards (Neutral Wake)			-0.006	-0.4	69
	CCW_1			-0.006	Downstream (Thrust Producing Wake)			
	CW_2							

As the upstream airfoil at both Sd pitch upwards from Points (a) to (b), they produce well-defined trailing wakes consisting of two CW shed vortices labelled as CW_1 and CW_2 and a CCW shed vortex labelled as CCW_1 as shown in Figure 5-24 (b). The single oscillating airfoil at Point (b) also exhibits a similar trailing wake structure shown in Figure 5-18 (b). These shed vortices induces two 'jet-force' vectors which can be illustrated in Figures 5-18 (b) and 5-24 (b) as the black arrows where the direction in which they act depends on the orientation of the shed vortex pairings. The first shed vortex pairing consists of CW_1 and

CCW₁ which induces a ‘jet-force’ vector, Vector₁ and the second shed vortex pairing consisting of CCW1 and CW2 induces Vector₂.

Using the Vortex Shedding Model methodology presented in Section 5.7, the vortex shedding model parameters based on the vorticity contours in Figure 5-18 (b) and Figure 5-24 (b) are computed and presented in Table 5-6. Table 5-7 shows the ‘Jet-Force’ vectors characteristics such as magnitude, direction and the resultant ‘jet-force’ vector magnitudes, V_{JF} for the respective trailing wakes. The vector magnitudes in Table 5-7 are calculated by summing the associated Γ_{vs} from Table 5-6 that induce the respective ‘Jet-Force’ vectors. For example, Vector₁ magnitude is simply the summation of CW₁ and CCW₁ vortex shedding circulations, Γ_{vs} . The direction of ‘jet-force’ vectors determines the sign in front of the vector magnitudes. If the orientation of the shed vortex pairing induces a ‘jet-force’ vector that points downstream in the +x direction, it shows a ‘thrust-producing’ wake structure [48, 51] then the vector magnitude will be denoted with the (-) sign. If the shed vortex pairings induces a vector that points upstream (-x direction) which is characteristic of a ‘drag-producing’ wake [48, 51], then the vector magnitude will be denoted with the (+) sign. If the shed vortex pairings are oriented side by side which induces a vector that points either up (+y direction) or down (-y direction) which is characteristic of a ‘neutral’ wake, then the vector magnitude will not be denoted with any sign. These signs for the vector magnitudes signify the ‘jet-force’ vector contribution of the resultant vector magnitude, V_{JF} produced by the trailing wake structure. V_{JF} is simply computed by summing the individual vector magnitudes along with their denoted signs. Hence, for a shed vortex pairing that exhibits a ‘neutral’ wake structure, without any sign denoted for its vector magnitude, it indicates that the particular ‘jet-force’ vector does not contribute to V_{JF} .

$$C_D = 70 \times V_{JF} \quad (\text{Eq. 5.4})$$

Table 5-7 shows that the single oscillating airfoil produces the highest V_{JF} of -0.022 which is 68% and 73% lower than the upstream airfoil at $S_d = 2$ and 2.5 respectively. The negative signs denoted for all the V_{JF} in Table 5-7 indicate that all of these cases produce trailing wake structure that generate thrust which supports the instantaneous negative drag or thrust forces generated at $t/T = 0.16$. Based on the instantaneous C_D values produced by these cases, the single oscillating airfoil produces the lowest C_D of -1.5 which equates to 67% and 73% lower than the upstream airfoil at $S_d = 2$ and 2.5. Importantly, these C_D decrement percentages

equals to that computed based on the V_{JF} values in Table 5-7 which indicate a clear correlation between the instantaneous C_D and V_{JF} . By dividing the instantaneous C_D with V_{JF} , these cases display similar ratios averaging at 70 thus establishing a direct correlation that can be expressed in Equation 5-4. By applying Equation 5.4 to compute the instantaneous C_D using the V_{JF} values in Table 5-7, the instantaneous C_D values can be replicated with a maximum error of $\pm 5\%$.

The single oscillating airfoil is able to generate higher thrust forces than the upstream airfoil at both S_d due to the stronger production of the (thrust-producing) reverse Von Kármán vortex street. It produces two shed vortex pairings that are orientated favorably to generate thrust thus; $Vector_1$ and $Vector_2$ magnitudes reinforce the resultant ‘jet-force’ vector magnitude, V_{JF} resulting in higher thrust than the upstream airfoil at both S_d . On the other hand, the upstream airfoil at both S_d exhibit no V_{JF} contribution from $Vector_1$ due to its ‘neutral’ wake orientation. As a consequence, the V_{JF} is solely contributed by $Vector_2$ magnitude which explains the lower V_{JF} and negative drag or thrust by the upstream airfoil. This also indicates that due to the presence of the downstream airfoil, it affects the orientation of the CW_1 and CCW_1 shed vortex pairing which induces a ‘neutral’ wake structure. As S_d increases from 2 to 2.5, the upstream airfoil exhibits a 14% reduction in $Vector_2$ magnitude which may explain the 20% lower negative C_D produced by the upstream airfoil as S_d increases from 2 to 2.5 at $t/T = 0.16$ in Point (b).

In this 1st half of the upstroke from Points (a) to (b), the downstream airfoil at both S_d exhibit similar critical flow events as their upstream counterparts such as partial CCW LEV initiation, rearwards propagation and thickening of the CCW turbulent boundary layer over the suction side as shown in Figures 5-24 (a), (b) and (g). These similar flow events over the suction side exhibited by the downstream airfoils support the identical steady increase in C_L as shown in Figure 5-23 (a) from Points (a) to (b).

Despite the similarities in flow events over the suction side from Points (a) to (b), Figures 5-24 (a) and (b) show that the downstream airfoil at both S_d experience down-washed vortex interactions over the pressure side at different intensities. The downstream airfoil at $S_d = 2$ exhibits interaction with a small CCW shed vortex near the TE and the shed vortex pairing of CCW_1 and CW_1 near its LE as shown in Figure 5-24 (b). As S_d increases to 2.5, it experiences lesser down-washed vortex interactions which consists of a very small CW shed vortex interacting near its TE and a similarly small CCW shed vortex near its LE. As the

downstream airfoil at both S_d exhibit similar ‘neutral’ wake structures in Figure 5-24 (b), C_D induced by vortex shedding effects is minimal. This demonstrates that the consistently higher C_D as the downstream airfoil pitches from Points (a) to (b) during the first half of the upstroke can be attributed to these down-washed vortex interactions rather than due to vortex shedding effects. As S_d increases from 2 to 2.5, the down-washed vortices shed by the upstream airfoil diminish in strength and size by the time they interact with the downstream airfoil, thus explaining the reduction in down-washed vortex interaction intensity and the consequential 80% reduction in C_D at Point (b) for the downstream airfoil at the larger S_d of 2.5. This also validates Lee [18]’s finding on the detrimental effects on C_D associated with smaller S_d due to increase in down-washed vortex interactions.

As these tandem oscillating airfoils experience the first stroke reversal at $t/T = 0.25$, it can be observed that the partial CCW LEVs for all the upstream airfoil at both S_d and the single oscillating airfoil have propagated rearwards towards the TE as it spills over the TE due to the rapid change in α as shown in Figure 5-24 (c) and Figure 5-18 (c) respectively. Despite experiencing similar flow events over the suction side, the upstream airfoils at both S_d and the single oscillating airfoil exhibit different rates of CCW LEV rearwards propagation and spillage over its TE at $t/T = 0.36$ after the stroke changes direction. The single oscillating airfoil exhibits the largest partial CCW LEV which has already spilled over the TE as displayed in Figure 5-18 (c) whereas the upstream airfoil at $S_d = 2$ exhibits the second smallest partial CCW LEV that has only started spilling over its TE as shown in Figure 5-24 (c). As S_d increases to 2.5, the upstream airfoil produces the smallest partial CCW LEV that exhibits a slight lag in terms of spillage when comparing to its upstream counterpart at $S_d = 2$. These variations in terms of partial CCW LEV size and rate of spillage may give an indication of the flow velocity over the suction surface of these airfoils leading up to its first stroke reversal at $t/T = 0.25$. These observations indicates that the single oscillating airfoil exhibits the highest flow velocity over its suction side when comparing to the upstream airfoil at both S_d which is supported by its fastest partial CCW LEV spillage and largest CCW LEV size. As S_d increases from 2 to 2.5, the flow velocity over the suction side of the upstream airfoil also decreases resulting in the slight lag in the partial CCW LEV spillage at $t/T = 0.36$ in Point (c). With the reduction in flow velocity over the suction side, pressure across the suction side increases which decreases the overall Δp across the airfoil sides, thus lift decreases [77]. The reduction in flow velocity over the upstream airfoil’s suction side at Point

(c) may explain the 7.4% and 55.6% reduction in peak C_L during the first stroke reversal at $t/T = 0.25$ for $Sd = 2$ and 2.5 respectively when comparing to the single oscillating airfoil.

As the upstream airfoil at both Sd continue with their downstroke from $t/T = 0.25$ to 0.75 , the spilled partial CCW LEVs begin to shed and propagate downstream in their trailing wakes as shown in Figures 5-24 (c) to (e). However, due to the reduction in CCW boundary layer flow velocity and the consequential lag in CCW LEV spillage documented in the previous paragraph, these upstream airfoils exhibit an apparent lag in their partial CCW LEV shedding in relation to the single oscillating airfoil shown in Figures 5-18 (c) to (e). The single oscillating airfoil exhibits the shedding of its partial CCW LEV between Points (c) to (d) which corresponds between $t/T = 0.36$ to 0.57 as demonstrated in Figures 5-18 (c) to (d). On the other hand, the upstream airfoil at $Sd = 2$ shed its partial CCW LEV at a later time just before its 2nd stroke reversal between Points (d) to (e) which relates to $t/T = 0.57$ to 0.75 as shown in Figures 5-24 (d) to (e). As Sd increases to 2.5 , the upstream airfoil exhibits the largest lag in its partial CCW LEV shedding which occurs only after the 2nd stroke reversal between Points (e) to (f) which corresponds to $t/T = 0.75$ to 0.91 .

As documented in the single oscillating airfoil analysis at $\alpha = \pm 15^\circ$, the prolonged attachment of the partial CCW LEV enable it to sustain positive C_L during the first half of its downstroke from $t/T = 0.25$ to 0.5 . Similar effects on C_L can also be observed for the upstream airfoil at $Sd = 2.5$ where the partial CCW LEV remains attached to the airfoil throughout its downstroke which allows the flow across the suction side to reinforce the attached CCW LEV thus enabling it to grow and gain strength. This effect is illustrated in Figure 5-24 (d) to (e) from $t/T = 0.57$ to 0.75 where the upstream airfoil at $Sd = 2.5$ exhibits an increase in size of its attached CCW LEV. Typically, as α decreases during the downstroke, the flow velocity over the suction side decreases and pressure increases. However, with the reinforcement of the attached CCW LEV, the increase in pressure over the suction side during the downstroke is somewhat ‘suppressed’ thus sustaining the Δp across the airfoil surfaces and the positive C_L . As shown in Figure 5-23 (a), these effects of the prolonged CCW LEV attachment may explain the increase in sustainment of positive C_L from $t/T = 0.25$ to 0.56 and the lower negative C_L throughout the 2nd half of the downstroke for the upstream airfoil as Sd increases from 2 to 2.5 . As a result of this positive lift sustainment during the downstroke, the upstream airfoil is able to produce 47.7% higher average C_L as Sd increases from 2 to 2.5 despite producing a higher C_L peak during the 1st half of the cycle at $Sd = 2$.

Another consequence of the different timing of the partial CCW LEV shedding is that the upstream airfoil at $Sd = 2$ and 2.5 forms its alternating shed vortex pairings in the trailing wakes at different times in the cycle. The upstream airfoil at $Sd = 2$ forms its shed vortex pairing with the shed CW vortex much earlier as shown at Point (e) in Figure 5-24 (e) whereas at $Sd = 2.5$, it forms later as shown at Point (g) in Figure 5-24 (g). At both Sd , the upstream airfoil exhibit ‘thrust-producing’ reverse Von Kármán vortex streets as illustrated by the ‘jet-force’ vectors pointing downstream in Figures 5-24 (e) and (g). Thus, as Sd increases from 2 to 2.5 for the upstream airfoil, the delay in forming its ‘thrust-producing’ vortex street supports the apparent phase lag in negative drag or thrust production during the 2nd half of the cycle as shown in Figure 5-23 (b). In addition, the upstream airfoil at $Sd = 2.5$ generates a weaker shed vortex pairing shown in Figure 5-24 (g) which induces a weaker ‘jet-force’ vector and lower induced thrust forces through vortex shedding effects in relation to its upstream counterpart at $Sd = 2$ shown in Figure 5-24 (e). Importantly, this may explain the 31% reduction in peak negative C_D during the 2nd half of the cycle for the upstream airfoil as Sd increases from 2 to 2.5.

Table 5-8 : Interacting Shed Vortex Characteristics for the Downstream Airfoil at $Sd = 2$ for $\alpha_{da, u} \approx +11.7^\circ$ at $t/T = 0.36$

Cases	Interacting Shed Vortex	Vortex Diameter, d (m)	Vortex Shedding Axial Velocity, V_{vs} (m/s)	Vortex Shedding Circulation, Γ_{vs} (m ² /s)	Vortex Interaction Location	Vortex Interaction Type
Downstream Airfoil at $Sd = 2$	CCW ₁	0.0118	0.058	0.0022	Pressure Side	Unlike
	CW ₁	0.0109	0.051	0.0017	Pressure Side	Like
	CCW ₂	0.0079	0.102	0.0025	Pressure Side	Unlike

During the first half of the downstroke in Figures 5-24 (c) and (d), the downstream airfoil at $Sd = 2$ is bombarded with more down-washed vortices than its downstream counterpart at $Sd = 2.5$. The increase in intensity of down-washed vortices at $Sd = 2$ relates back to the fact that the upstream airfoil at $Sd = 2$ was able to form shed vortex pairings earlier in its downstroke thus resulting in a higher vortex shedding frequency. The downstream airfoil at $Sd = 2$ experiences three down-washed vortex interactions onto its pressure side that consists of a pair of alternating shed vortex near the LE labelled as CW₁ and CCW₂ and a small CCW shed vortex near its TE labelled as CCW₂ as illustrated in Figure 5-24 (c) at $t/T = 0.36$. As Sd increases to 2.5, the downstream airfoil does not experience any significant down-washed vortex interactions over its pressure side.

By applying the Vortex Shedding Model methodology in Section 5.7, the vortex shedding model parameters for these interacting shed vortices are computed and tabulated in Table 5-8 in order to evaluate its effects on the aerodynamic performance of the downstream airfoil at $Sd = 2$. Table 5-8 also tabulates the interaction location and the vortex interaction type on whether it's a 'like-signed' or 'unlike-signed' interaction. Since all of the shed interacting vortices interact with the pressure side where the boundary layer flow is predominantly in the CW direction (blue), the interacting shed vortex, CW_1 is deemed as a 'like-signed' vortex interaction which is 'constructive' in nature as it adds momentum to the CW boundary layer over the pressure side [44, 45]. On the other hand, both CCW_1 and CCW_2 vortices are categorized as 'unlike-signed' vortex interactions which are deemed to be 'destructive' as they impede the build-up of circulation across the pressure side. In order to evaluate the resultant vortex interaction effect on the pressure side, all of the vortex shedding circulations, Γ_{vs} need to be considered. By denoting a (-) sign to the T_{vs} term for the 'unlike-signed' CCW_1 and CCW_2 shed vortices and a (+) sign for the Γ_{vs} term for the 'like-signed' CW_1 shed vortex to represent its 'destructive' and 'constructive' nature respectively, the resultant vortex shedding circulation over the pressure side can be obtained by summing the individual circulations. The resultant vortex shedding circulation calculated is $-0.003\text{m}^2/\text{s}$ which indicates that the resultant effect of these down-washed vortex interactions is destructive to the circulation over the pressure side. With this quantifiable proof that the circulation over the pressure side reduces due to these shed vortex interactions, pressure over the pressure side increases which increases the overall Δp across the downstream airfoil sides thus increasing positive lift.

These down-washed vortex interactions effects over the pressure side of the downstream airfoil at $Sd = 2$ may explain the large increase in peak C_L of 9 after the first stroke reversal at $t/T = 0.31$ as shown in Figure 5-23 (a) which equates to a 44.4% increase in C_L when comparing to the peak C_L of 5 attained by its downstream counterpart at $Sd = 2.5$. Importantly, this large C_L increment exhibited by the downstream airfoil at $Sd = 2$ during its first half of its downstroke is responsible for the 45% higher average C_L of 0.44 when comparing to its downstream airfoil at $Sd = 2.5$. As the downstream airfoil at $Sd = 2.5$ does not experience these down-washed vortex interactions over its pressure side, its C_L production remains similar to the upstream airfoil at both Sd but without any positive lift sustainment during its downstroke which may explain the slight decrease in average C_L when comparing to its upstream counterpart at both Sd .

As the downstream airfoil at both S_d continue to pitch downwards into the negative α range from $t/T = 0.57$ to 0.75 in Points (d) to (e), they exhibit two different types of down-washed vortex interactions at the TE; a ‘like-signed’ and an ‘unlike-signed’ vortex interactions that are circled in Figure 5-24 (e). The ‘like-signed’ vortex interaction exhibited by the downstream airfoil at $S_d = 2$ consists of the small CCW shed vortex interacting with the shed partial CCW LEV at the TE which is constructive as it reinforces the shed CCW LEV. Due to this reinforcement, the downstream airfoil at $S_d = 2$ is able to produce stronger alternate shed vortex pairing when comparing to its downstream counterpart at $S_d = 2.5$. The ‘unlike-signed’ vortex interaction at $S_d = 2.5$ consists of a small CW shed vortex interacting with the spilled partial CCW LEV which causes the shedding of weaker and smaller CCW LEV as shown in Figure 5-24 (e). Despite these differences in vortex interactions at the TE, the downstream airfoil at both S_d still manage to produce ‘thrust-producing’ reverse Von Kármán vortex streets from Points (d) to (e) during the 2nd half the downstroke as indicated by the ‘jet-force’ vectors pointing downstream in Figures Figure 5-24 (e). Hence, due to stronger alternate vortex shedding, the downstream airfoil at $S_d = 2$ is able to produce stronger ‘thrust-producing’ vortex street which supports the 42% higher peak in negative drag or thrust from Points (d) to (e) when comparing to its downstream counterpart at $S_d = 2.5$. This reduction in thrust production during the 2nd half of downstroke may have a contribution to the 84% increase in average C_D produced by the downstream airfoil as S_d increases from 2 to 2.5 in this pre-stall condition at $\alpha = \pm 15^\circ$.

5.5.3 In-Phase Oscillating Tandem Airfoils Case at $S_d = 2$ and 2.5 for $\alpha = \pm 45^\circ$

(a)

(b)

Figure 5-25 : (a) Instantaneous C_L Plot and (b) Instantaneous C_D Plot for In-Phase Oscillating Tandem Airfoils at $S_d = 2$ and 2.5 for $\alpha = \pm 45^\circ$; Green Dotted Lines from (a) to (j) illustrate the instantaneous force coefficients that correlate to the instantaneous vorticity contours in Figure 5-26

Figure 5-26 : Vorticity Contours for One Oscillation Cycle for In-Phase Oscillating Tandem Airfoils at $Sd = 2$ and 2.5 for $\alpha = \pm 45^\circ$

Upstream Airfoil at $Sd = 2$ and 2.5

As the upstream airfoil at both Sd start their oscillating cycle at Point (a) at $t/T = 0.017$ in Point (a), they produce their respective shed vortex clusters in the trailing wakes as shown in Figure 5-26 (a). However, these shed vortex clusters exhibit some differences in their composition and structure at the various Sd and in relation to the single oscillating airfoil which is shown in Figure 5-20 (a). Based on the single oscillating airfoil analysis at $\alpha = \pm 45^\circ$ in Section 5.4.3, this shed vortex cluster produced during the 1st half of the upstroke may be responsible for adding momentum to the fluid in the trailing wake thus, generating higher thrust forces for the single oscillating airfoil.

At $t/T = 0.017$, the upstream airfoil at $Sd = 2$ produces a shed vortex cluster as shown in Figure 5-26 (a) that consists of an 8.5% smaller primary CCW shed LEV, CCW_LEV_1 but a smaller and more stretched out or elongated secondary vortex structure with an approximately 10% higher ζ_{max} when comparing to the single oscillating airfoil's shed vortex cluster. Its CW_TEV_1 is also smaller in size when comparing to the CW_TEV_1 produced by the single oscillating airfoil shown in Figure 5-20 (a). As Sd increases from 2 to 2.5, the upstream airfoil produces a shed vortex cluster with a similar secondary vortex structure but with an 11% smaller CCW_LEV_1 than its counterpart at $Sd = 2$ as exhibited in Figure 5-26 (a). Based on the instantaneous C_L plot in Figure 5-25 (a), the upstream airfoil at $Sd = 2$ produces a higher thrust force of $C_D = -1.25$ which is approximately 5 times higher than the single oscillating airfoil. As Sd increases to 2.5, the upstream airfoil still manages to generate a C_D of -0.31 which relates to a 87.5% higher negative drag or thrust produced by the single oscillating airfoil. From these variations in the shed vortex clusters, the higher thrust generation during the start of the cycle for the upstream airfoil at both Sd in relation to the single oscillating airfoil may be attributed to the more elongated and stronger secondary CW vortex structure. Applying the Vortex Shedding Model concept, a more elongated secondary vortex structure may indicate a faster vortex shedding velocity, V_{vs} hence inducing a stronger vortex shedding circulation, Γ_{vs} which adds more momentum to the fluid thus increasing thrust in this case. In addition, as Sd increases from 2 to 2.5, the upstream airfoil produces a 75% reduction in negative C_D or thrust which may be attributed to the 11% smaller CCW_LEV_1 given that the core vorticities of their shed vortex clusters and secondary vortex structures are near identical as shown in Figure 5-25 (b).

As α increases to $\alpha_{ua, u} = +30^\circ$ to Point (b) at $t/T = 0.11$, the shed vortex cluster produced by the upstream airfoil at $Sd = 2$ elongates further as it propagates downstream while maintaining a continuous secondary vortex structure but its CCW_TEV_1 has dissipated. Meanwhile, the upstream airfoil at $Sd = 2.5$ exhibits a broken up vortex cluster without much continuity and elongation and its CW_TEV_1 has also dissipated. On the other hand, the single oscillating airfoil demonstrates its ability to conserve its secondary vortex structure and its CCW_TEV_1 as evidently shown in Figure 5-20 (b). The instantaneous C_D plot in Figure 5-25 (b) shows that the upstream airfoil at $Sd = 2$ exhibits a steady increase in C_D from Points (a) to (b) during the first half of the upstroke whereas at $Sd = 2.5$, it shows a similar increase in C_D to a peak of 0.44 which is 76% higher than its counterpart at $Sd = 2$. The single oscillating airfoil maintains its production of thrust or negative C_D force exhibiting a low C_D of -0.44 at Point (b) which is 43% lower than the upstream airfoil at $Sd = 2$. Through these changes in the shed vortex clusters, the increase in C_D between Points (a) to (b) exhibited by the upstream airfoil at both Sd may be attributed to its inability to conserve its CCW_TEV_1 as well as the single oscillating airfoil, thus the overall addition of momentum to the fluid at the trailing wake reduces resulting in the reduction in thrust. As Sd increases from 2 to 2.5, the upstream airfoil produces consistently higher C_D from Points (a) to (b) due to its inability to maintain its secondary vortex structure coupled with its smaller CCW_LEV_1 as shown in Figures 5-26 (a) and (b).

In Point (c) at $t/T = 0.21$ as the upstream airfoil at both Sd approaches its first stroke reversal as shown in Figure 5-26 (c), their CCW boundary layers over the suction side have already started separating due to the increase effect of adverse pressure gradients associated with the increase in α [77]. The upstream airfoil at both Sd exhibit similar extent of flow separation with minimal difference in their CCW LEVs' core vorticities as shown in Figure 5-26 (c) which supports the near identical C_L of 3.5 and C_D of 0.25 at Point (c) in Figures 5-25 (a) and (b) respectively. The single oscillating airfoil produces a more defined CCW LEV formation at Point (c) as shown in Figure 5-20 (c) which supports the 28.6% higher C_L when comparing to the upstream airfoil at both Sd . Despite generating higher C_L as it approaches its first stroke reversal, the single oscillating airfoil exhibits the same degree of flow separation and production of form drag as the upstream airfoil at both Sd as illustrated by the identical thickening of the CCW boundary layer near the TE in Figures 5-26 (c) and 5-20 (c). This will justify the identical C_D of 0.25 produced by the upstream airfoil at both Sd and the single oscillating airfoil at Point (c).

After the first stroke reversal at $t/T = 0.36$ in Point (d) where $\alpha_{u,a,u} \approx +31.4^\circ$, the detached CCW LEVs formed by the upstream airfoil at both S_d have already propagated downstream and started to spill over the TE as shown in Figure 5-26 (d). These flow events were similarly observed in the single oscillating case depicted in Figure 5-20 (d). At this phase in the cycle, the CCW boundary layer flow has fully separated as shown in Figure 5-26 (d) and full stall has taken effect supported by the decreasing C_L trend exhibited in Figure 5-25 (a). When comparing the spilled (CCW_LEV₁)s and the CCW boundary layers across the suction sides at $S_d = 2$ and 2.5 shown in Figure 5-26 (d), it can be observed that both spilled (CCW LEV₁)s exhibit approximately the same $\zeta_{\max}$ of +0.03. However at $S_d = 2$, the upstream airfoil displayed a stronger CCW boundary layer formation with a 33% higher $\zeta_{\max}$ than its upstream counterpart at $S_d = 2.5$. This indicates that the overall circulation over the suction side of the upstream airfoil is lower as S_d increases from 2 to 2.5 which give rise to the 33% reduction in C_L at Point (d) shown in Figure 5-25 (a).

As the upstream airfoil at both S_d continue with their downstroke from Points (e) to (f) and pitch into the negative α range, they begin shedding their (CCW LEV₁)s and secondary CCW vortices as shown in Figures 5-26 (e) to (f). The upstream airfoil at both S_d exhibit near identical phases of CCW_LEV₁ shedding with marginal differences in their sizes. In Figures 5-20 (e) to (f), the single oscillating airfoil also exhibit a similar phase in its CCW_LEV₁ shedding as the upstream airfoil which may explain the similar C_L and C_D decreasing trends shown in Figure 5-25 (a) from Points (e) to (f).

During the 2nd half of the downstroke from Points (f) to (h), the CW boundary layer across the pressure side starts to gain strength and momentum as α decreases as shown in Figures 5-26 (f) to (h). The upstream airfoil at both S_d exhibit similar CW_LEV₁ initiations as shown in Figure 5-26 (g) but as α decreases to Point ((h), the upstream airfoil at $S_d = 2.5$ produces a smaller and less defined CW_LEV₁ formation than its counterpart at $S_d = 2$ as highlighted in Figure 5-26 (h). The weaker CW_LEV₁ formation over the pressure side which may explain the consistently lower C_L values from Points (f) to (h) and the 15.4% reduction in C_L at Point (h) in Figure 5-25 (a) as S_d increases from 2 to 2.5. Through analyzing all the points in the cycle, at $S_d = 2.5$, the upstream airfoil's ability to increase its average C_L by 21% in relation to its upstream counterpart at $S_d = 2$ and the single oscillating airfoil may be attributed to its ability to generate consistently lower negative C_L during the 2nd half of the cycle due to the weaker formation of the CW_LEV₁ over its pressure side.

Table 5-9 : Shed Vortex Cluster Characteristics for Upstream Airfoil at $S_d = 2$ and 2.5 and Single Oscillating Airfoil for $\alpha = \pm 45^\circ$; (a) At $t/T = 0.58$ and (b) At $t/T = 0.67$

At $t/T = 0.58$ in Point (f) at $\alpha_d \approx -23.5^\circ$				
Cases	Shed Vortex	Vortex Diameter, d (m) / Vortex Length, L_{vs} (m)	Absolute Max Core Vorticity, $\zeta_{\max}$ (s^{-1})	Instantaneous C_D
Upstream Airfoil at $S_d = 2$	CCW_LEV ₁	0.017	0.05	-1.1
	CCW_LEV _{s1}	0.032	0.035	
	CW_TEV ₁	0.010	-0.03	
Upstream Airfoil at $S_d = 2.5$	CCW_LEV ₁	0.016	0.05	-0.3
	CCW_LEV _{s1}	0.022	0.035	
	CW_TEV ₁	0.010	-0.02	
Single Oscillating Airfoil	CCW_LEV ₁	0.022	0.03	-0.875
	CCW_LEV _{s1}	0.041	0.03	
	CW_TEV ₁	0.010	-0.015	

(a)

At $t/T = 0.67$ in Point (g) at $\alpha_d \approx -39.6^\circ$				
Cases	Shed Vortex	Vortex Diameter, d (m) / Vortex Length, L_{vs} (m)	Max Core Vorticity, $\zeta_{\max}$ (s^{-1})	Instantaneous C_D
Upstream Airfoil at $S_d = 2$	CCW_LEV ₁	0.022	0.05	-0.75
	CCW_LEV _{s1}	0.041	0.045	
	CW_TEV ₁	0.009	-0.025	
Upstream Airfoil at $S_d = 2.5$	CCW_LEV ₁	0.018	0.05	-0.875
	CCW_LEV _{s1}	0.024	0.045	
	CW_TEV ₁	0.008	-0.015	
Single Oscillating Airfoil	CCW_LEV ₁	0.020	0.03	-0.25
	CCW_LEV _{s1}	0.028	0.04	
	CW_TEV ₁	0.003	-0.01	

(b)

In the 2nd half of the downstroke from Points (f) to (h), the upstream airfoil at both S_d produce similar shed vortex clusters as the single oscillating airfoil which consists of the primary shed CCW LEV, CCW_LEV₁, the secondary CCW vortex structure, CW_LEV_{s1} and the shed CW TEV, CW_TEV₁ as shown in Figures 5-26 (f) to (h). However, these shed vortex clusters have different characteristics such vortex diameters, secondary vortex structure and maximum core vorticities that will have an effect on the overall momentum added to the fluid which affects thrust production typically associated with these shed vortex

clusters. Tables 5-9 (a) and (b) tabulates these various shed vortex cluster characteristics at $t/T = 0.58$ in Point (f) and at $t/T = 0.67$ in Point (g) respectively. For the CCW secondary vortex structure, CCW_LEV_{s1} observed in Figures 5-26 (f) and (g) and in Figure 5-20 (f) and (g) for the single oscillating airfoil, due to its continuous structure, the vortex diameter, d cannot be effectively used as its characteristic dimension hence, the length of the secondary vortex structure, L_{vs} is used for this comparative analysis. L_{vs} is measured from the airfoil's TE to the end of its secondary vortex structure.

Figure 5-26 (f) at $t/T = 0.58$ shows that the upstream at $Sd = 2$ produces a relatively stronger shed vortex cluster in comparison to the single oscillating airfoil in Figure 5-20 (f). Based on Table 5-9 (a), the upstream airfoil at $Sd = 2$ produces a 22% smaller CCW_LEV_1 which is compensated by the 66.7% higher ζ_{max} when comparing to the single oscillating airfoil. Even though the single oscillating airfoil produces a 28% longer CCW_LEV_{s1} , the upstream airfoil at $Sd = 2$ generates a similar sized CW_TEV_1 with a 100% higher ζ_{max} . Based on these differences, the upstream airfoil at $Sd = 2$ produces moderately smaller vortex structures but is compensated by stronger CCW_LEV_1 and CW_TEV_1 core vorticities which supports the 25% higher thrust or negative C_D produced at Point (f) when comparing to the single oscillating airfoil.

At Point (f), as Sd increases from 2 to 2.5, the upstream airfoil produces a 5.88% reduction in CCW_LEV_1 size but maintaining similar ζ_{max} as displayed in Table 5-9 (a). However, it exhibits an approximately 31% reduction in CCW_LEV_{s1} length with a similar ζ_{max} . The upstream airfoil at $Sd = 2.5$ also shows a similar sized CW_TEV_1 but with a 33.3% lower ζ_{max} . These 5.88% and 31% reductions in CCW_LEV_1 size and CCW_LEV_{s1} length respectively and the 33.3% weaker CW_TEV_1 supports the 72.7% reduction in negative C_D or thrust for the upstream airfoil at Point (f) as Sd increases from 2 to 2.5.

As the single oscillating airfoil and upstream airfoil at both Sd continue their downstroke from Points (f) to (g), the CW LEVs grow and exhibit an early indication of CW boundary layer flow separation especially so for the upstream airfoil at $Sd = 2$ and the single oscillating airfoil shown in Figures 5-26 (g) and 5-20 (g) respectively. The upstream airfoil at $Sd = 2.5$ exhibit an attached CW boundary layer structure as displayed in Figure 5-26 (g). A more separated and thicker boundary layer structure typically indicates a higher induction of form drag forces.

As the upstream airfoil at $Sd = 2$ pitches down from Points (f) to (g), the CCW_LEV_1 grows by 29.4% while maintaining its ζ and its CCW_LEV_{s1} grows by 28% coupled with a 28% increase in ζ_{max} . The CW_TEV_1 reduces its size by 10% and ζ_{max} by 16.7%. Despite its steady 28% to 29% CCW_LEV_1 and CCW_LEV_{s1} growths and ζ_{max} sustainment, the upstream airfoil exhibits a 32% reduction in thrust as it pitches from Point (f) to (g). Hence, this strengthening of its shed vortex cluster contradicts the increase in positive C_D in this cycle transition which strongly indicates another contributing factor in producing C_D .

Based on Table 5-9 (b), as Sd increases to 2.5, the upstream airfoil at Point (g) exhibits an 18.2% smaller CCW_LEV_1 with similar ζ_{max} and a 41% shorter CCW_LEV_{s1} structure with similar ζ_{max} . It also produces a 11.11% smaller CW_TEV_1 coupled with a 40% reduction in ζ_{max} . Despite producing smaller shed vortices and weaker CW_TEV_1 , the upstream airfoil at $Sd = 2.5$ manages to produce 9.4% higher negative C_D at Point (g) when comparing to its counterpart at $Sd = 2$. When comparing the CW boundary layer structure across the pressure side of the upstream airfoil at both Sd shown in Figure 5-26 (g), it can be observed that the upstream airfoil at $Sd = 2$ exhibits a thicker and more separated CW boundary layer structure than its counterpart at $Sd = 2.5$. Hence, it can be deduced that the upstream airfoil at $Sd = 2$ experiences a higher extent of flow separation than its counterpart at $Sd = 2.5$, thus giving rise to higher form drag forces which explains the 32% increase in C_D as it pitches from Points (f) to (g). Despite the reduction in shed vortex cluster size, the upstream airfoil at $Sd = 2.5$ manages to double its negative C_D from Points (f) to (g) by delaying flow separation. However, as the upstream airfoil at $Sd = 2.5$ pitches from Points (g) to (h) just before the 2nd stroke reversal at $t/T = 0.75$, the CW boundary layer across the pressure side exhibits a similar separated flow structure to its counterpart at $Sd = 2$ as shown in Figure 5-26 (h). This indicates similar form drag production by the upstream airfoil at both Sd at Point (h), thus nullifying the C_D benefit by delaying flow separation at Point (g) for the upstream airfoil at $Sd = 2.5$. Due to the stronger generation of shed vortex cluster by the upstream airfoil at $Sd = 2$ at Point (h) as shown in Figure 5-26 (h), it produces 33% higher negative C_D when comparing to its counterpart at $Sd = 2.5$.

In summary, the upstream airfoil at $Sd = 2$ is able to generate comparable average C_L of 0.37 as the single oscillating airfoil primarily due to its ability to generate consistently lower negative C_L values of up to 22.9% during the 2nd half of the cycle attributed to the smaller CW_LEV_1 formation and CW boundary layer structure over its pressure side when

comparing to the single oscillating airfoil. This generation of lower negative C_L compensates for the consistently lower positive C_L of up to 28.6% at Point (c) due to the less defined CCW_LEV₁ formation during the first half of the cycle. Despite the upstream airfoil at both Sd exhibiting similar flow events such as primary LEV initiation, LEV spillage and extent of flow separation as the single oscillating airfoil, the primary LEV formations are consistently weaker and less defined in comparison to the single oscillating airfoil. This observation is more apparent with the increase in Sd from 2 to 2.5. The upstream airfoil at Sd = 2.5 generates consistently lower negative C_L throughout the 2nd half of the cycle and up to 15.4% at Point (h) when comparing to its upstream counterpart at Sd = 2. This reduction in negative C_L is attributed to the weaker and smaller CW_LEV₁ formation over its pressure side which may explain the 21% higher average C_L of 0.47 produced by the upstream airfoil at Sd = 2.5 in relation to its upstream counterpart at Sd = 2 and the single oscillating airfoil.

Conclusively, the upstream airfoil at Sd = 2 is able to generate a high average negative C_D of -0.21 by generating shed vortex cluster with higher $\zeta_{\max}$ during its 2nd half of the downstroke and with stronger secondary vortex structure during the 1st half of the upstroke. This enables the upstream airfoil at Sd = 2 to generate consistently higher thrust forces or negative C_D during these phases in the cycle when comparing to its upstream counterpart at Sd = 2.5 and the single oscillating airfoil. As Sd increases from 2 to 2.5, the upstream airfoil exhibits a 45% increase in average C_D when comparing to the single oscillating airfoil. This increase in average C_D is attributed to the weaker shed vortex cluster that is unable to retain its secondary vortex structure during the 1st half of the upstroke and despite delaying flow separation before the 2nd stroke reversal; its smaller vortex shed cluster reduces its negative C_D or thrust production during the 2nd half of the cycle.

Downstream Airfoil at Sd = 2 and 2.5

As the downstream airfoil at both Sd pitch up from Points (a) to (c) during the 1st half of the upstroke, they experience different down-washed vortex interactions as shown in Figures 5-26 (a) to (c). It can be observed that the downstream airfoil at Sd = 2 experiences a singular shed CCW_LEV_{s1} that passes over the suction side which was generated in the previous 2nd half of the cycle. Due to the ‘like-signed’ vortex interaction between this shed CCW_LEV_{s1} and the CCW boundary layer over the suction side, the circulation and flow velocity over the suction side will most likely increase, thus lowering the pressure over the suction side which consequentially increases Δp and positive lift. As the downstream airfoil at Sd = 2 pitches up

from Point (a) to (c) during its upstroke, it can be observed that this shed CCW_LEV_{s1} propagates steadily over the downstream airfoil's suction side which corresponds to the steep increase in C_L and enabling it to attain a peak C_L of 4 at $t/T = 0.14$. This peak in C_L of 4 equates to a 67% increment when comparing to the single oscillating airfoil at $t/T = 0.14$, thus demonstrating the 'constructive' effect of this shed CCW_LEV_{s1} interaction on C_L for the downstream airfoil at $Sd = 2$.

As the downstream airfoil at $Sd = 2$ pitches up from Point (b) to (c) shown in Figures 5-26 (b) to (c), it experiences another down-washed vortex interaction at its LE over the suction side with a strong and large shed CW_LEV_1 as shown in Figure 5-26 (c). This interacting CW_LEV_1 is the primary CW LEV shed by the upstream airfoil during the previous 2nd half of the upstroke shown in Figure 5-25 (i) and (j). In Figure 5-26 (c), it can be observed that due to this interaction near the LE of the downstream airfoil at $Sd = 2$, it disrupts the CCW LEV formation over suction side and causes a rapid displacement of the CCW LEV. As documented by Broering and Lian [44, 45], this typical rapid LEV displacement due to down-washed vortex interaction causes a reduction in C_L as it disrupts the circulation build-up by the LEV that is required to produce positive lift forces. Hence, this rapid CCW LEV displacement that occurs between $t/T = 0.11$ in Point (b) and $t/T = 0.21$ in Point (c) due to the interaction of the strong shed CW_LEV_1 at the LE may explain of the sudden drop in C_L after $t/T = 0.14$ as shown in Figure 5-25 (a).

At the same phase in the upstroke from Points (a) to (c), the downstream airfoil at $Sd = 2.5$ experiences two down-washed vortex interactions as displayed in Figures 5-26 (a) to (c). It encounters a similar CCW_LEV_{s1} interaction over its suction side but with an apparent lag due to the larger Sd when comparing to its downstream counterpart at $Sd = 2$. The second vortex interaction consists of an early-detached CW_LEV_1 over the pressure side as indicated in Figures 5-26 (a) to (c). This early detachment of its CW_LEV_1 was due to the interaction of the shed CCW_LEV_1 that occurred during its previous 2nd half of the upstroke from $t/T = 0.75$ to 1.0 as highlighted in Figure 5-26 (i). Despite being prematurely detached from the pressure side of the downstream airfoil at $Sd = 2.5$, the CCW_LEV_1 still hovers over the airfoil surface which increases flow velocity and decreases the pressure over the pressure side. Since the early detached CW_LEV_1 is much larger and stronger than the 'lift enhancing' shed CCW_LEV_{s1} propagating over the suction side as shown in Figure 5-26 (b), the influence of the CW_LEV_1 is greater which results in an overall decrease in Δp and C_L for the downstream airfoil by approximately 65% as Sd increases from 2 to 2.5. This 'destructive'

effect on C_L due to the early detached CW_LEV_1 may also explain the consistently lowest C_L values from Points (a) to (c) produced by the downstream airfoil at $Sd = 2.5$ when comparing to the rest of the cases as shown in Figure 5-25 (a).

During the 1st half of the upstroke from Points (a) to (c), it is also apparent that the presence of the upstream airfoil and the generation of down-washed vortices have an impact on the trailing wake structures produced by the downstream airfoil at both Sd . The downstream airfoil at both Sd produce highly unstructured shed vortex clusters which supports the consistently higher C_D from Points (a) to (c) in relation to its upstream counterparts and single oscillating airfoil as shown in Figure 5-25 (b). This also validates Broering and Lian [44, 45] finding where they documented that thrust was detrimentally affected by the production of ‘unstructured’ counter-rotating shed vortices in the trailing wakes which were associated with the increase intensity of down-washed vortex interactions relating to $Sd < 1.25$.

At Point (a), as Sd increases from 2 to 2.5, the downstream airfoil exhibits a more unstructured shed vortex cluster in its trailing which supports the 44.4% higher C_D at Point (a) as shown in Figure 2-25 (b). As the downstream airfoil at both Sd pitch to Point (b) where $\alpha_{da}, u \approx +30^\circ$, it can be observed in Figure 5-26 (b) that the early detached CW_LEV_1 produced by the downstream airfoil at $Sd = 2.5$ has propagated downstream to the TE and exhibits a longer CW secondary vortex structure with a ζ_{max} of -0.03 which is twice the ζ_{max} exhibited by its counterpart at $Sd = 2$. This observation may indicate the strengthening effect of the ‘like-signed’ interaction between the early detached CW_LEV_1 and the CW secondary vortex structure at the TE in Point (b). As a result, the downstream airfoil at $Sd = 2.5$ is able to produce a stronger shed vortex cluster which adds more momentum to the fluid in the trailing wake, thus increasing thrust or negative C_D . This supports the 48% lower C_D in Point (b) produced by the downstream airfoil at $Sd = 2.5$ in relation to its counterpart at $Sd = 2$ despite having a more unstructured shed vortex cluster formation. As the downstream airfoil at both Sd pitch up from Points (b) to (c), the shed vortex clusters have broken apart as it begin to dissipate into their respective trailing wakes. For the downstream airfoil at $Sd = 2.5$, the early detached CW_LEV_1 propagates further downstream and away from the CW secondary vortex structure as shown in Figure 5-26 (c) which reduces its ‘constructive’ effect on thrust production. This also supports the steady increase in positive C_D for the downstream airfoil at $Sd = 2.5$ from Points (b) to (c) in Figure 5-25 (b) as it produces comparable C_D of 1.38 at Point (c) in relation to its downstream counterpart at $Sd = 2$.

As the downstream airfoil both S_d change their stroke direction after $t/T = 0.25$, they exhibit completely different CCW boundary layer structures due to the down-washed vortex interactions in relation to its upstream counterparts and the single oscillating airfoil as shown in Figure 5-26 (d) thus indicating different full stall events associated with this phase of the cycle. The downstream airfoil also experiences different flow events at the various S_d tested due to the different timing of its down-washed vortex interactions as shown in Figure 5-26 (d). At Point (d) after the first stroke reversal, the downstream airfoil at $S_d = 2$ exhibits a significant enlargement of the early detached CCW_LEV₁ as it grows by approximately 150% while maintaining a high $\zeta_{\max}$ of +0.05 as displayed in Figure 5-26 (d). This enlargement of the detached CCW_LEV₁ may be due to the increase in flow momentum over the suction side as a result of the change in stroke direction at $t/T = 0.25$. This is the same effect that is responsible for the CCW LEV spillage over the TE which was observed in the upstream cases and the single oscillating case evidently shown in Figure 5-26 (d) and Figure 5-20 (d) respectively. At this Point (d) where $\alpha_{da,d} \approx +31.4^\circ$, this enlarged detached CCW_LEV₁ still remains close to the suction surface of the downstream airfoil at $S_d = 2$ as shown in Figure 5-26 (d). As this detached CCW_LEV₁ grows and hovers close to the suction side as the downstream airfoil at $S_d = 2$ pitches from Points (c) to (d), the circulation over suction side increases as pressure decreases which results in the increase in Δp across the airfoil sides thus positive C_L increases. This ‘constructive’ effect on C_L due to the enlargement of early detached CCW_LEV₁ may explain the downstream airfoil’s ability to attain a higher $C_{L\max}$ of 5.3 at $t/T = 0.28$ which is 17.8% and 34% higher than its upstream counterpart at $S_d = 2$ and the downstream airfoil at $S_d = 2.5$.

As the downstream airfoil at $S_d = 2$ pitches from Points (d) to (e) which relates to the mid-point in the cycle at $t/T = 0.5$, the enlarged detached CCW_LEV₁ is being shed and can be observe propagating above the suction side as exhibited in Figure 5-26 (e). Despite increase in distance from the suction side due to shedding, the enlarged CCW_LEV₁ is still able to reinforce the flow velocity over the suction side which increases positive C_L at Point (e) for the downstream airfoil at $S_d = 2$ which supports the C_L of 0.83 that is higher than the upstream and single oscillating airfoil cases as shown in Figure 5-25 (a).

On the other hand, as S_d increases from 2 to 2.5, the downstream airfoil experiences similar down-washed vortex interactions as its counterpart at $S_d = 2$ but with an apparent lag as it pitches during its first half of the downstroke from Points (d) to (e). In Figure 5-26 (d), the downstream airfoil at $S_d = 2.5$ experiences its interaction with the shed CW_LEV₁ which

displaces the CCW_LEV₁ over the suction side similar to as its counterpart at $S_d = 2$. Due to the lag in this interaction with the shed CW_LEV₁ and consequential displacement of its CCW_LEV₁, the downstream airfoil at $S_d = 2.5$ is able to delay the effects of full stall as it attains its $C_{L_{max}}$ of 4.2 at $t/T = 0.31$ whereas at $S_d = 2$, the downstream airfoil attained its $C_{L_{max}}$ of 5.3 at $t/T = 0.28$ which equates to an approximate lag time of 0.058s between the two S_d cases. At this Point (d), due to this lag interaction, the downstream airfoil at $S_d = 2.5$ exhibits a considerably smaller detached CCW_LEV₁ than the enlarged CCW_LEV₁ produced at $S_d = 2$. Assuming that the trailing wakes does not play a part in drag production at this Point (d), it can be deduced that this enlarged detached CCW_LEV₁ is a major contributor to positive drag forces thus supporting the 66.5% increase in C_D at Point (d) in relation to its downstream counterpart at $S_d = 2.5$. Thus, demonstrating the trade-off of this enlargement in the detached CCW_LEV₁ where it enhances lift but increases positive drag as well.

As the downstream airfoil at $S_d = 2.5$ continues its downstroke from Points (d) to (e), its detached CCW_LEV₁ grows in size by 50% to an average vortex diameter, d of 19.8mm as shown in Figure 5-26 (e). This 50% CCW_LEV₁ enlargement pales in comparison to the 150% growth displayed by its counterpart at $S_d = 2$ in Figure 5-26 (d). As the detachment of the CCW_LEV₁ occurs before the first stroke reversal for the downstream airfoil at $S_d = 2$, this variation in CCW_LEV₁ growth verifies that the increase in flow momentum induced by the change in stroke direction do play an instrumental part in increasing the detached CCW_LEV₁ growth by up to 100%. In Figure 5-26 (e) shows that the enlarged detached CCW_LEV₁ still remains close to the suction surface of the downstream airfoil at $S_d = 2.5$ which similarly enhances the positive C_L at Point (e) which explains its ability to produce the highest C_L of 1.67 at $t/T = 0.5$ in relation to the rest of the cases as shown in Figure 5-25 (a).

As the downstream airfoil at $S_d = 2$ pitches from Points (f) to (g) where $\alpha_{da, d} \approx -39.6^\circ$, its enlarged CCW_LEV₁ was shed and is not able to influence C_L and does not have any apparent down-washed vortex interactions that can affect the boundary layer structures across the airfoil sides as shown in Figures 5-26 (f) to (g), hence supporting the similar C_L trends and values as its upstream counterpart at $S_d = 2$ from Points (f) to (g) in Figure 5-25 (a). However, at $S_d = 2.5$, it can be observed in Figure 5-26 (g) that the enlarged CCW_LEV₁ still ‘sticks’ to the suction side as it propagates downstream towards the TE. Due to this prolonged attachment to the suction side exhibited by the downstream airfoil at $S_d = 2.5$, the

enlarged CCW_LEV₁ further sustains positive C_L which may explain the prolonged positive C_L sustainment until $t/T = 0.58$ and the 46% reduction in negative C_L when comparing to its downstream counterpart at $Sd = 2$ at Point (g).

However, the downstream airfoil at both Sd produce really different shed vortex clusters when comparing to its upstream counterpart at both Sd and the single oscillating airfoil as they pitch from Points (f) to (g). As documented earlier, in this 2nd half of the oscillating cycle is where the upstream airfoil at both Sd produce majority of their thrust or negative C_D which correlated to the generation of structured and strong shed vortex clusters. At Points (f) and (g), the downstream airfoil at $Sd = 2$ exhibits a shed vortex cluster that consists of significant weaker and smaller shed CCW vortices albeit maintaining a weak structure which may explain consistently lower negative C_D or thrust when comparing to its upstream counterpart at $Sd = 2$.

At the same time, as Sd increases to 2.5, the downstream airfoil exhibits a considerably larger shed vortex cluster which consists of both CW and CCW shed vortices as shown in Figures 5-26 (f) and (g). Moreover, its CCW shed vortices have at least twice the $\zeta_{\max}$ when comparing to the CCW shed vortices generated by its counterpart at $Sd = 2$. However, the shed vortex cluster produced by the downstream airfoil at $Sd = 2.5$ is highly unstructured in relation to the rest. Despite the unstructured shed vortex cluster, due to its stronger shed vortices and larger cluster, the downstream airfoil is still able to generate consistently higher negative C_D or thrust up to 130% at Point (f) as Sd increases from 2 to 2.5.

As the downstream airfoil at $Sd = 2$ approaches its 2nd stroke reversal at $t/T = 0.75$ in Point (h), it encounters a larger and stronger shed CCW_LEV₁ that prematurely displaces its CW_LEV₁ over its pressure side as shown in Figure 5-26 (h) which reduces its negative C_L between Points (h) to (g) as it produces a negative C_L of -2.67 which is 15% lower than its counterpart at $Sd = 2.5$. After the 2nd stroke reversal at Point (i) in Figure 5-26 (i), this early detached CCW_LEV₁ enlarges due by the increase in flow momentum associated with the change in stroke direction. This enlargement of its CW_LEV₁ over its pressure side may be responsible for the increase in negative C_L after reversing its stroke direction as it attains a peak in C_L of 4.7 at $t/T = 0.81$ as shown in Figure 5-25 (a). This enlarged CW_LEV₁ is only shed between Points (i) and (j) with reference to Figure 5-26 (i) and (j), thus giving rise to the steady increase in C_D and consistently higher positive C_D values throughout the 2nd half of the

upstroke when comparing to the upstream and single oscillating cases as shown in Figure 5-25 (b).

Due to the larger S_d , the downstream airfoil at $S_d = 2.5$ did not exhibit any interaction with the shed CCW_LEV_1 at Point (h), thus its CW_LEV formation over the pressure side is allowed to gain strength and momentum similar to the upstream and single oscillating cases as it pitches from Points (g) to (h). The downstream airfoil at $S_d = 2.5$ only encounters the shed CCW_LEV_1 after the 2nd stroke reversal between Points (h) and (i) as shown in Figure 5-26 (i) which may explain the sharp decrease in negative C_L soon after $t/T = 0.75$. As it pitches up towards Point (j), the enlargement of its detached CW_LEV_1 over the pressure side increases the negative lift as it produces comparable C_L values in relation its upstream counterpart at $S_d = 2.5$ as shown in Figure 5-25 (a). This enlargement of the CW_LEV_1 during the 2nd half of the upstroke is also responsible for the consistently higher C_D values than the upstream and single oscillating cases which are comparable to its downstream counterpart at $S_d = 2$.

Conclusively, through documenting the flow events and the effects of the down-washed vortex interactions experienced by the downstream airfoil at both S_d , the present study has provided an in-depth understanding on the effects of varying S_d which alter the timing of these down-washed vortex interactions and its consequential effects on C_L and C_D during the cycle. The down-washed interactions between the shed primary LEVs produced by the upstream airfoil and the downstream airfoil demonstrate its effect in enhancing and sustaining positive C_L and increasing positive C_D through the enlargement and subsequent prolonged ‘attachment’ of the detached primary LEVs.

At the smaller S_d of 2, the downstream airfoil experiences its interaction with the shed primary LEVs before each stroke reversal which allows the increase in flow momentum associated with stroke reversal to further enlarge its early detached LEVs. This larger detached LEVs produced by the downstream airfoil at $S_d = 2$ may be responsible for its ability to generate the highest average C_L of 0.72 which is 94.6% higher than its upstream counterpart at $S_d = 2$ and 44% higher than the downstream airfoil at $S_d = 2.5$ as displayed in Table 5-5. Having these larger detached LEVs due to the interaction with shed primary LEVs before stroke reversal have its trade-off as the downstream airfoil at $S_d = 2$ also generates the highest average C_D of 0.68 which is 5 times more than single oscillating airfoil and 17% higher than its downstream counterpart at $S_d = 2.5$. Another cause for this high average C_D

may be attributed to the downstream airfoil's ability to produce smaller and weaker shed vortex cluster at $Sd = 2$ during its 2nd half of the downstroke thus decreasing its thrust or negative C_D production in relation to its downstream counterpart at $Sd = 2.5$ and the single oscillating airfoil.

At the larger Sd of 2.5, the downstream airfoil experiences its interaction with the shed primary LEVs only after each stroke reversal, thus its detached LEVs is considerable smaller by up to 100% reduction in growth when compared to the enlarged detached LEVs produced by its counterpart at $Sd = 2$. Due to the increase in Sd from 2 to 2.5, the lag in interaction prolongs the sustainment of positive C_L during the downstroke up to $t/T = 0.58$ which enables the downstream airfoil to generate a high average C_L of 0.58 that is more than 4 times and 2 times higher than the single oscillating airfoil and its upstream counterpart at $Sd = 2.5$ respectively. Due to smaller detached primary LEVs coupled with the production of larger and stronger shed vortex cluster during the 2nd half of the downstroke, the downstream airfoil at $Sd = 2.5$ is able to generate an average C_D of 0.58 which is 14.7% lower than its downstream counterpart at $Sd = 2$ as shown in Table 5-5.

5.5.4 Average Force Characteristics of In-Phase Oscillating Tandem Airfoils at Various Gap Distances

(a)

(b)

Figure 5-27 : In-Phase Upstream Oscillating Airfoils at Various Gap Distances at $Sd = 2$; (a) Average C_L Plot and (b) Average C_D Plot

(a)

(b)

Figure 5-28 : In-Phase Downstream Oscillating Airfoils at Various Gap Distances at $S_d = 2$; (a) Average C_L Plot and (b) Average C_D Plot

Upstream Airfoils at Various Gap Distances

Based on literature on tandem airfoils aerodynamic studies such as by Sharpf and Mueller [43] and Broering and Lian [44, 45], the effects of gap distances were not investigated. Hence, it will be important for the present study to analyze on the effects of various G on the average C_L and C_D for the tandem oscillating airfoils. For the brevity of the present study, the focus will be on in-phase oscillating tandem airfoils with $Sd = 2$ at various G of ± 0.133 and ± 0.266 which equates to vertical spacings of $\pm 10\text{mm}$ and $\pm 20\text{mm}$ respectively. G of $+0.133$ would translate to the downstream airfoil's aerodynamic center positioned at $+10\text{mm}$ vertically apart from the upstream airfoil's aerodynamic center. The average C_L and C_D plots will be segregated according to the respective oscillating airfoils. Figures 5-27 and 5-28 show the average force coefficients plots for the upstream and downstream airfoils respectively.

Based on the average C_L plot in Figure 5-27 (a), as α increases from $\pm 10^\circ$ to $\pm 40^\circ$, the upstream airfoil at $G = +0.133$ produces consistently lower average C_L values when comparing to the single oscillating airfoil as it attains its maximum of 0.366 at $\alpha = \pm 30^\circ$ that is approximately 21% lower than the single oscillating airfoil. Between $\alpha = \pm 40^\circ$ to $\pm 60^\circ$, the upstream airfoil at $G = +0.133$ exhibits a decreasing average C_L trend albeit a subtle 4% increment from $\alpha = \pm 40^\circ$ to $\pm 45^\circ$ where it produces the lowest average C_L of 0.164 at $\alpha = \pm 60^\circ$ which is 66.5% lower than the single oscillating airfoil at the largest α of $\pm 60^\circ$. As the positive G increases from $+0.133$ to $+0.266$, the upstream airfoil exhibits similar average C_L trends and values with the exception at $\alpha = \pm 20^\circ$ and between $\alpha = \pm 40^\circ$ to $\pm 45^\circ$ where it produces 44% and 24% higher average C_L respectively when comparing to its counterpart at $G = +0.133$ as shown in Figure 5-27 (a).

As the upstream airfoil changes polarity to negative G , it produces higher C_L peak values at $\alpha = \pm 30^\circ$ to $\pm 45^\circ$ when comparing to the single oscillating airfoil and at positive G shown in Figure 5-27 (a). However, α increases from $\pm 45^\circ$ to $\pm 60^\circ$, the upstream airfoil at $G = -0.133$ and -0.266 exhibit a steep drop in C_L and produce 45% higher average C_L than the single oscillating airfoil at $\alpha = \pm 60^\circ$. As the upstream airfoil increases its negative G from -0.133 to -0.266 , the changes in average C_L values for $\alpha < \pm 30^\circ$ cases are minimal, but it is able to produce higher average C_L peaks of up to 15.4% between $\alpha = \pm 30^\circ$ to $\pm 40^\circ$. The upstream airfoil at $G = -0.266$ also produces the highest average C_L peak of 0.6 at $\alpha = \pm 40^\circ$ which is 23% higher than the C_L peak produced by the single oscillating airfoil.

Figure 5-27 (b) presents the average C_D results for the upstream airfoils at the various G in comparison with the single oscillating airfoil. The upstream airfoils at both positive G exhibit a 'U' shaped average C_D trend similar to the single oscillating airfoil where they produce the highest average C_D at the smallest $\alpha = \pm 10^\circ$ and reducing its drag production as α increases to approximately $\pm 40^\circ$ before exhibiting a steady increase in average C_D as α increases to $\pm 60^\circ$. At $\alpha = \pm 10^\circ$, the upstream airfoil at $G = +0.133$ produces an average C_D of 0.5 which is 44% higher than the single oscillating airfoil and comparable to the upstream airfoil at $G = 0$ as shown in Figure 5-27 (b). As α increases to $\pm 15^\circ$, it exhibits a steep decrease in average C_D to -0.1 which is comparable to the single oscillating airfoil. However, as α increases from $\pm 15^\circ$ to $\pm 30^\circ$, it exhibits a gradual increase in average C_D producing low levels of positive average C_D which differs from the single oscillating airfoil that is able to sustain its average negative C_D or thrust production from $\pm 15^\circ$ to $\pm 40^\circ$. As α increases from $\pm 30^\circ$ to $\pm 40^\circ$, the upstream airfoil at $G = +0.133$ exhibits a large reduction in C_D as it produces an average C_D of -0.14 which is comparable to both the single oscillating airfoil and the upstream airfoil at $G = 0$. The upstream airfoil at $G = +0.133$ then produces consistently lower C_D values than the single oscillating airfoil at the larger α of between $\pm 45^\circ$ to $\pm 60^\circ$ where it exhibits the largest C_D reduction of 65% at $\alpha = \pm 60^\circ$ in relation to the single oscillating airfoil.

As positive G increases from +0.133 to +0.266, the upstream airfoil produces consistently higher average C_D for $\alpha = \pm 10^\circ$ to $\pm 30^\circ$ cases with an average increment of +0.12. As α increases from $\pm 20^\circ$ to $\pm 30^\circ$, the upstream airfoil at $G = +0.266$ exhibits a sharp decrease in average C_D to -0.153 as it outperforms both the single oscillating airfoil by nearly 150% and its counterpart at $G = +0.133$. However, as α increases to $\pm 45^\circ$, it exhibits an increase in average C_D to -0.035 which is 75% higher than the single oscillating airfoil and its counterpart at $G = +0.133$ and at $G = 0$. At larger α of between $\pm 45^\circ$ to $\pm 60^\circ$, the upstream airfoil at both positive G produce similar average C_D values which are consistently lower than the single oscillating airfoil.

At $\alpha = \pm 10^\circ$, the upstream airfoil at $G = -0.133$ is able to produce 44% lower average C_D of 0.5 when comparing to the single oscillating airfoil which is also comparable to the upstream cases at positive G as shown in Figure 5-27 (b). As α increases to $\pm 15^\circ$, the upstream airfoil at $G = -0.133$ exhibit a 74% reduction in average C_D to 0.127 which is still higher than the upstream airfoil at positive G but comparable to the upstream airfoil at $G = 0$. As α increases from $\pm 15^\circ$ to $\pm 20^\circ$, it exhibits a sharp increase in average C_D to 0.22 which is the highest average

C_D produced at this α as shown in Figure 5-27 (b). Despite exhibiting a steady decrease in average C_D trend as α decreases from $\pm 15^\circ$ to $\pm 40^\circ$, the upstream airfoil at $G = -0.133$ still produces higher average C_D values than the rest of the upstream cases and the single oscillating airfoil. However, as α increases from $\pm 40^\circ$ to $\pm 60^\circ$, it continues to decrease its average C_D production as it attains its lowest average C_D of -0.20 at $\alpha = \pm 60^\circ$. This decrease in average C_D as α increases from $\pm 40^\circ$ to $\pm 60^\circ$ differs from the increasing average C_D trend exhibited by the upstream cases at positive gap distances and the single oscillating airfoil as shown in Figure 5-27 (b).

As negative G increases from -0.133 to -0.266 , the upstream airfoil exhibit a similar average C_D trend but the variations in average C_D values between the two negative gap distances are higher than between the two positive gap distances as shown in Figure 5-27 (b). The upstream airfoil at $G = -0.266$ produces a comparable average C_L of 0.5 at $\alpha = \pm 10^\circ$ to its counterpart at $G = -0.133$. However, as α increases from $\pm 10^\circ$ to $\pm 60^\circ$, the upstream airfoil at $G = -0.266$ displays the best thrust performance as it exhibits consistently lower average C_D values than the single oscillating airfoil. It attains its lowest average C_D or highest thrust of -0.3 at $\alpha = \pm 30^\circ$ which is more than double the highest thrust produced by the single oscillating airfoil. At larger α of between $\pm 45^\circ$ to $\pm 60^\circ$, the upstream airfoil at $G = -0.266$ exhibit a similar decreasing average C_D trend and values as its upstream counterpart at $G = -0.133$ which demonstrates the upstream airfoil's ability to sustain thrust production at both negative G tested even at these larger α range.

Conclusively, the upstream airfoil at both positive G has a detrimental effect on average C_L as it produce consistently lower average C_L values than the single oscillating throughout the α range. However, with the change from positive to negative G , the upstream airfoil at both negative G are able to generate consistently higher average C_L values of up to 37% when comparing to the single oscillating airfoil between $\alpha = \pm 30^\circ$ to $\pm 45^\circ$. The increase in negative G from -0.133 to -0.266 also give rise to higher average C_L of up to 15.4% at $\alpha = \pm 40^\circ$. On the downside, at all G tested, the upstream airfoil exhibit a steep average C_L reduction as α increases from $\pm 45^\circ$ to $\pm 60^\circ$ producing the lowest average C_L of 0.164 which equates to the largest reduction of 66% when comparing to the single oscillating airfoil.

In summary, the upstream airfoil at both positive G tested exhibit consistently higher average C_D values at $\alpha < \pm 30^\circ$ but lower values at larger α of between $\pm 45^\circ$ to $\pm 60^\circ$ when comparing

to the single oscillating airfoil. Despite producing one of the lowest average C_D of -0.153 at $\alpha = \pm 30^\circ$, the upstream airfoil at $G = +0.266$ produces higher average C_D values at $\alpha < \pm 30^\circ$ and at $\pm 45^\circ$ when comparing to its counterpart at $G = +0.133$. The upstream airfoil at both negative G exhibit similar average C_D trends but the variations in the C_D values between the two negative G cases are much larger than between the two positive G cases. This clearly shows that the change in magnitudes for the negative G is more influential in affecting C_D for the upstream airfoil than the change in magnitudes in the positive G . The upstream airfoil at $G = -0.133$ produces consistently higher average C_D than the upstream cases at positive G and the single oscillating airfoil for most of the cases $< \pm 40^\circ$. However, as negative G increases from -0.133 to -0.266, the upstream airfoil exhibits the best thrust performance by generating more than double the maximum negative C_D or thrust produced by the single oscillating airfoil at $\alpha = \pm 30^\circ$. In addition, at both negative G , the upstream airfoil is able to sustain thrust production at larger α of $\pm 45^\circ$ and $\pm 60^\circ$ where the upstream cases at positive G and the single oscillating airfoil failed to do so.

Downstream Airfoils at Various Gap Distances

Figure 5-28 (a) shows the average C_L results for the in-phase downstream oscillating airfoil at various G tested in comparison to the single oscillating airfoil. When comparing to the average C_L results produced by the upstream airfoil at various G in Figure 5-27 (a), it can be observed that the downstream airfoil exhibit an increasing average C_L trend rather than the ‘n’ shaped trends exhibited by the upstream cases at various G . This indicate that the downstream airfoil at various G is able to continue generating an increase in average C_L even at larger $\alpha \geq \pm 45^\circ$ when comparing to its upstream counterpart at various G .

The downstream airfoil at $G = +0.133$ produces an average C_L of 0.26 at $\alpha = \pm 10^\circ$ which is approximately 18% higher but 39.7% lower than the single oscillating airfoil and the downstream airfoil at $G = 0$ respectively as shown in Figure 5-28 (a). As α increases from $\pm 10^\circ$ to $\alpha < \pm 20^\circ$, it exhibits a steady increase in average C_L but at a slower rate than the single oscillating airfoil as it produces 29% lower average C_L at $\alpha = \pm 15^\circ$ than the single oscillating airfoil and the downstream airfoil at $G = 0$. However, at $\alpha = \pm 20^\circ$, the downstream airfoil at $G = +0.133$ manages to increase its average C_L to 0.50 which is 6.4% higher than the single oscillating airfoil but still 13.9% lower than the downstream airfoil at $G = 0$. As α increases from $\pm 20^\circ$ to $\pm 60^\circ$, the downstream airfoil at $G = +0.133$ exhibits a similar

increasing average C_L trend and comparable values as its counterpart at $G = 0$ where produces consistently higher values than the single oscillating airfoil. It also produces its maximum average C_L of 0.73 at $\alpha = \pm 45^\circ$ which corresponds to the largest increment of 92% when comparing to the single oscillating airfoil.

As positive G increases from +0.133 to +0.266, the downstream airfoil exhibit comparable average C_L values as the single oscillating airfoil in the pre-stall α range of $\leq \pm 20^\circ$ and a similar 29% higher average C_L than its counterpart at $G = +0.133$ at $\alpha = \pm 15^\circ$. As α increases from $\pm 20^\circ$ to $\pm 30^\circ$, the downstream airfoil at $G = +0.266$ exhibits a steeper increase in average C_L than its counterpart at $G = +0.133$ as it attains its maximum of 0.72 which is approximately 13% and 55% higher than the downstream airfoil at $G = +0.133$ and the single oscillating airfoil respectively. Subsequently, it generates consistently lower average C_L values at larger $\alpha \geq \pm 40^\circ$ when comparing to the downstream cases at $G = 0$ and +0.133 with maximum reduction of approximately 15% at $\alpha = \pm 45^\circ$ as shown in Figure 5-28 (a). Despite this reduction in C_L at the larger positive G , the downstream airfoil still produces consistently higher values than the single oscillating airfoil at $\alpha \geq \pm 40^\circ$ cases.

As the gap distances change polarity, the downstream airfoil at both negative G of -0.133 and -0.266 exhibit very different average C_L trends when comparing to the rest of the cases presented in Figure 5-28 (a). At these negative G cases, the downstream airfoil consistently produce lower average C_L values than the downstream cases at positive G and $G = 0$ for most of the α cases with exception of the smallest $\alpha = \pm 10^\circ$ where it produces an average C_L of 0.4 which nearly double that produced by the downstream cases at positive G and the single oscillating airfoil.

At $G = -0.133$, the downstream airfoil exhibits marginal lift improvements for most of the α range tested in relation to the single oscillating airfoil with the exception of $\pm 12.5^\circ \leq \alpha \leq \pm 25^\circ$ where it produces its minimum average C_L of 0.235 at $\alpha = \pm 15^\circ$ as shown in Figure 5-28 (a). Despite this poor C_L performance, the downstream airfoil at $G = -0.133$ generates 25% higher average C_L at $\alpha = \pm 30^\circ$ and 30% higher values at $\alpha = \pm 45^\circ$ and $\pm 60^\circ$ when comparing to the single oscillating airfoil. The downstream airfoil at $G = -0.266$ produces similar average C_L values in the pre-stall α range of $\leq \pm 20^\circ$ but it is able produce 77% higher average C_L at $\alpha = \pm 15^\circ$ in relation to its counterpart at $G = -0.133$. However, as α increases from $\pm 20^\circ$ to $\pm 40^\circ$, it generates consistently the lowest average C_L values when comparing to the rest of the cases

shown in Figure 5-28 (a). It attains the lowest average C_L of 0.28 in between $\alpha = \pm 20^\circ$ to $\pm 30^\circ$ which equates to approximately a 40% and 53% reduction when comparing to the single oscillating and the downstream airfoil at $G = 0$ cases respectively. As α increases from $\pm 40^\circ$ to $\pm 60^\circ$, the downstream airfoil at $G = -0.266$ exhibits a steep linear increase as it surpasses its counterpart at $G = -0.133$ at $\alpha = \pm 60^\circ$ by producing 13.4% higher average C_L of 0.72 which is comparable to the downstream airfoil at $G = 0$ and $+0.133$.

At $G = +0.133$, the downstream airfoil exhibits relatively similar drag production as the downstream airfoil at $G = 0$ where low positive drag forces were produced at $\alpha = \pm 10^\circ$ before steadily increasing as α increases. At $G = +0.266$, the downstream airfoil exhibits a different average C_D trend at $\alpha < \pm 40^\circ$ when comparing to its counterpart at $G = +0.133$. The downstream airfoil at $G = +0.266$ starts at a high average C_D of 0.46 at $\alpha = \pm 10^\circ$ and subsequently reduces to a local minimum C_D of 0.26 at $\alpha = \pm 30^\circ$ which relates to a reduction of up to 63% when comparing to the rest of the downstream cases at various G . At $\alpha = \pm 40^\circ$ to $\pm 60^\circ$, the downstream airfoil at $G = +0.266$ produces comparable average C_L values as the downstream airfoil at $G = +0.133$ and $G = 0$ as shown in Figure 5-28 (b).

The downstream airfoil at $G = -0.133$ starts off by producing the highest average C_D of 0.51 at $\alpha = \pm 10^\circ$ which is 47% higher than the single oscillating airfoil before decreasing to its local minimum average C_D of 0.19 as it increases to $\alpha = \pm 15^\circ$. It then produces a sharp average C_L increment of 61.5% as it increases to $\alpha = \pm 20^\circ$. Its average C_D behavior or trend in this pre stall α range of $\leq \pm 20^\circ$ is similar to that exhibited by the upstream airfoil at the various G cases and differs from its downstream counterparts at positive G cases. However, as increases from $\pm 20^\circ$ to $\pm 60^\circ$, the downstream airfoil at $G = -0.133$ exhibit identical increasing average C_D trend but consistently higher values when comparing to the downstream cases at positive G and $G = 0$. As negative G increases from -0.133 to -0.266 , the downstream airfoil produces 37% lower average C_D of 0.32 at $\alpha = \pm 10^\circ$ than its counterpart at $G = -0.266$ which is comparable to the single oscillating airfoil. With the increase in α from $\pm 10^\circ$ to $\pm 20^\circ$, it exhibits a plateau in average C_D at 0.32 which differs from its counterpart at $G = -0.133$. As α increases from $\pm 20^\circ$ to $\pm 60^\circ$, the downstream airfoil at $G = -0.266$ exhibit a linear steady increase in average C_D and for most α cases, it produces lower values than its counterpart at $G = -0.133$. The only exception is that it produces 30% higher average C_D at $\alpha = \pm 45^\circ$ as G increases from -0.133 to -0.266 as shown in Figure 5-28 (b).

Conclusively, the downstream airfoil at $G = +0.133$ consistently generates higher average C_L values than the single oscillating airfoil up to 92% increment at $\alpha > \pm 20^\circ$. As positive G increases from $+0.133$ to $+0.266$, for $\alpha < \pm 40^\circ$ cases, the downstream airfoil produces comparable C_L production but is able to attain 13% higher average C_L at $\alpha = \pm 30^\circ$. However, at $\alpha \geq \pm 40^\circ$ cases, it exhibits reductions in average C_L of up to 15% as G increases from $+0.133$ to $+0.266$. The downstream airfoil at both negative G exhibit poorer average C_L performance than at the positive G cases throughout most of the α range except for at $\alpha = \pm 10^\circ$ where it produces a 50% C_L increment. At $G = -0.133$, the downstream airfoil shows marginal lift improvements of up to 30% at larger α of $\geq \pm 45^\circ$ when comparing to the single oscillating airfoil. However, as negative G increases to -0.266 , it produces the lowest average C_L values at α from $\pm 20^\circ$ to $\pm 40^\circ$ which equates to a maximum reduction of 40% and 53% reduction when comparing to the single oscillating and the downstream airfoil at $G = 0$ cases respectively. Despite producing the lowest average C_L , the downstream airfoil at $G = -0.266$ manages to outperform its downstream counterpart at $G = -0.133$ at the larger α of $\geq \pm 45^\circ$. The variation in the average C_L values between the various G cases indicates that the change in magnitude for the negative G cases is more influential in affecting the average C_L than the change in magnitude for the positive G cases. This is also similarly observed for the upstream airfoil as documented earlier.

In summary, the downstream airfoil at both positive G exhibit similar average C_D trends as the downstream airfoil at $G = 0$ with subtle differences in the pre-stall α range of $\leq \pm 20^\circ$. The effect of increasing positive G magnitude has minimal influence on the average C_D for most α cases tested. As gap distance polarity changes, the downstream airfoil at both negative G of -0.133 and -0.266 produce consistently higher average C_D values at $\alpha > \pm 20^\circ$ in relation to the single oscillating airfoil and downstream cases at positive G as shown in Figure 5-28 (b). At pre stall conditions where $\alpha \leq \pm 20^\circ$, the downstream airfoil at both negative G exhibit mixed average C_D results where it depends on the α . Overall, the downstream airfoil at all the G tested exhibit an increasing average C_D trend which differs from the ‘U’ shaped trend exhibited by the upstream cases and the single oscillating airfoil. All of the downstream cases produce consistently higher average C_D throughout the α range tested with exception at $\alpha = \pm 10^\circ$ in relation to the single oscillating airfoil. None of these downstream cases at various G is able to produce negative C_D or thrust unlike some of the upstream cases and the single oscillating airfoil.

$$C_{L,combined} = \frac{F_{y,ua} + F_{y,da}}{\frac{1}{2}\rho (2A_w)U_\infty^2} = \frac{C_{L,ua} + C_{L,da}}{2} \quad (\text{Eq. 5.5})$$

$$C_{D,combined} = \frac{F_{x,ua} + F_{x,da}}{\frac{1}{2}\rho (2A_w)U_\infty^2} = \frac{C_{D,ua} + C_{D,da}}{2} \quad (\text{Eq. 5.6})$$

$$\left(\frac{C_L}{C_D}\right)_{combined} = \frac{C_{L,combined}}{C_{D,combined}} \quad (\text{Eq. 5.7})$$

Figure 5-29 : Normalized Combined C_L / C_D Ratio for In-Phase Tandem Oscillating Airfoil at $S_d = 2$ at Various Gap Distances

In order to effectively evaluate the oscillating tandem airfoils' aerodynamic performance at the various G tested, it is important to ascertain the normalized combined C_L to C_D ratios which can be calculated using Equations 5.5 to 5.7. Both combined average C_L and combined average C_D are calculated by summing the average force coefficients produced by the upstream and downstream airfoils and normalizing by their total wing areas, A_w using Equations 5.5 and 5.6 respectively as documented by Broering and Lian [45].

Based on the normalized combined C_L / C_D ratios presented in Figure 5-29, it is apparent that the in-phase oscillating tandem configurations at all the G tested produce positive C_L / C_D ratios which clearly indicates these various G cases are most ideal to deliver positive lift

forces without any negative drag or thrust production. In the context of flight in the unsteady flow regime, these in-phase tandem oscillating cases at various G are more applicable for hovering flight rather than forward flight where thrust is required. However, this differs from the single oscillating airfoil which is able to generate thrust forces for α between $\pm 15^\circ$ to $\pm 40^\circ$ depicted by the negative C_L / C_D ratios averaging at -5.5.

For pre-stall α range of $\leq \pm 20^\circ$, the tandem oscillating airfoils exhibit similar ratios that start really low at $\alpha = \pm 10^\circ$ similar to the single oscillating airfoil. In this pre-stall α range, the tandem configurations at both positive G cases also produce identical ratios as the tandem configuration at $G = 0$ which indicates that the change in magnitudes in the positive G range has very little influence in affecting average C_L and C_D production. For the negative G cases, the oscillating tandem configurations exhibit similar C_L / C_D trend but at $G = -0.266$, it is able to attain a peak in C_L / C_D ratio of 3.5 at $\alpha = \pm 15^\circ$ due to the upstream airfoil's ability to produce really low average C_D of -0.08 and reasonably high average C_L of 0.35.

At the larger α range of $> \pm 20^\circ$ to $\pm 60^\circ$, the tandem oscillating configuration at the various G cases exhibit similar C_L / C_D trends with subtle differences across most of the α cases with the exception of $\alpha = \pm 30^\circ$. At $\alpha = \pm 30^\circ$, with $G = -0.266$, the in-phase tandem oscillating configuration is able to attain its highest ratio of 10 which is primarily attributed to lowest positive $C_{D,combined}$ of 0.055 that is contributed by the low average C_D produced by both its upstream and downstream airfoils despite neither of them producing thrust. The in-phase tandem oscillating configurations also produce consistently higher ratios than the single oscillating airfoil with the exception of $\alpha = \pm 45^\circ$. At $\alpha = \pm 45^\circ$, the single oscillating airfoil's ratio increases to the positive range due to the step increase in average C_D as it produces positive drag instead of thrust forces coupled with the 24.6% reduction in average C_L as α increases from $\pm 40^\circ$ to $\pm 45^\circ$. Based on the single oscillating airfoil analysis at $\alpha = \pm 45^\circ$ in Section 5.4.3, the increase in average C_D is attributed to the aggressive full stall effects during the 1st half of the oscillating cycle. At the largest $\alpha = \pm 60^\circ$, the in-phase tandem oscillating configurations produce similar C_L / C_D ratios averaging at 0.9 which is comparable to the single oscillating airfoil. Despite the upstream airfoil at the various G producing consistently higher average C_L and lower average C_D than the single oscillating airfoil, its downstream counterparts reduce the combined C_L / C_D ratios by producing consistently lower average C_L and higher average C_D than the single oscillating airfoil, thus nullifying the aerodynamic benefits provided by its upstream counterparts.

5.6 OUT-OF-PHASE OSCILLATING TANDEM AIRFOILS

Table 5-10 : Average Force Coefficients for Out-of-Phase Tandem Oscillating Airfoils at $S_d = 2$ and 2.5 for $\alpha = \pm 45^\circ$

$\alpha = \pm 45^\circ$	Upstream Airfoil at $S_d = 2$	Downstream Airfoil at $S_d = 2$	Upstream Airfoil at $S_d = 2.5$	Downstream Airfoil at $S_d = 2.5$	Single Oscillating Airfoil
C_L	0.74	1.32	0.73	1.04	0.38
C_D	0.08	1.26	0.24	0.21	0.11

This section will document on the investigation on the out-of-phase, $\phi = 180^\circ$ oscillating tandem airfoils at $S_d = 2$ and 2.5 over the same α range as the in-phase oscillating cases. Similar to the in-phase oscillating cases, the average force coefficient results will be presented first. However, for the brevity of the present study, the vorticity contours and instantaneous force coefficient results at only $\alpha = \pm 45^\circ$ will be discussed and presented in this section. The average force coefficients results at $\alpha = \pm 45^\circ$ are presented in Table 5-10.

5.6.1 Average Force Characteristics of Out-of-Phase Oscillating Tandem Airfoils at $S_d = 2$ and 2.5

Figure 5-30 : Out-of-Phase Tandem Oscillating Airfoils at $S_d = 2$ and 2.5 ; (a) Average C_L Plot and (b) Average C_D Plot

The out-of-phase upstream airfoil at $S_d = 2$ exhibits similar average C_L trend as the single oscillating airfoil as shown in Figure 5-30 (a). The upstream airfoil at $S_d = 2$ starts by producing a low average C_L of 0.12 at $\alpha = \pm 10^\circ$ which is 48% lower than the single oscillating airfoil. At $\alpha \pm 15^\circ$, it produces similar average C_L of 0.43 as the single oscillating airfoil. Between $\alpha = \pm 20^\circ$ to $\pm 60^\circ$, it is able to generate higher average C_L values than the single oscillating airfoil where it attains its maximum average C_L of 0.81 at $\alpha = \pm 40^\circ$ which is 65.6% higher than the single oscillating airfoil. The upstream airfoil at $S_d = 2$ exhibit the largest difference of 0.35 at $\alpha = \pm 45^\circ$ which equates to its largest C_L increment of 94% in relation to the single oscillating airfoil as shown in Figure 5-30 (a).

The upstream airfoil at $S_d = 2$ exhibits a slightly different average C_D trend than the single oscillating airfoil but produces values not exceeding the limits set by the single oscillating airfoil as shown in Figure 5-30 (b). In the pre-stall $\alpha \leq \pm 20^\circ$, the upstream airfoil at $S_d = 2$ produces consistently lower average C_D values than the single oscillating airfoil with the largest difference of 0.36 at $\alpha = \pm 10^\circ$ as shown in Figure 5-30 (b). It is able to maintain a constant low level of negative average C_D or thrust throughout this pre stall α range averaging at -0.16. However, as α increases from $\pm 20^\circ$ to $\pm 30^\circ$, the upstream airfoil at $S_d = 2$ exhibits an increase in average C_D to 0.15 which is then sustained until $\alpha = \pm 45^\circ$. On the other hand, the

single oscillating airfoil exhibit its ability to sustain its thrust production averaging at -0.12 across the α range between $\pm 30^\circ$ to $\pm 40^\circ$. As α increases from $\pm 45^\circ$ to $\pm 60^\circ$, the upstream airfoil at $S_d = 2$ exhibit a similar increment rate in average C_D and similar values when comparing to the single oscillating airfoil where it attains its maximum average C_D of 0.59 at $\alpha = \pm 60^\circ$.

As displayed in Figure 5-30 (a), the downstream airfoil at $S_d = 2$ shows similar average C_L trend as its upstream counterpart at $S_d = 2$ where it peaks at $\alpha = \pm 40^\circ$ but is able to produce much higher values than the upstream cases and the single oscillating airfoil for $\alpha \geq \pm 20^\circ$ cases. At $\alpha = \pm 10^\circ$, it produces the lowest average C_L of 0.038 which is 83.5% and 68% lower than the single oscillating airfoil and its upstream counterpart at $S_d = 2$ respectively. As α increases to $\pm 15^\circ$, average C_L increases to 0.43 which is comparable to its upstream counterpart at $S_d = 2$ and the single oscillating airfoil. However, as α increases from $\pm 20^\circ$ to $\pm 60^\circ$, it produces very high average C_L values as it attains its maximum average C_L of 1.5 at $\alpha = \pm 40^\circ$ which relates to a 85% and 200% increment when comparing to its upstream counterpart at $S_d = 2$ and the single oscillating airfoil respectively.

In the in-phase oscillating tandem cases in Section 5.4, an increase in average C_L is typically associated with a corresponding increase in average C_D . For example, the in-phase downstream oscillating airfoil at $S_d = 2$ at $\alpha = \pm 45^\circ$ exhibits a 94% average C_L improvement when comparing to the single oscillating airfoil due to the effects of down-washed vortex interactions with the shed primary CCW LEV before its 1st stroke reversal. However, there is a trade off as it also produces an average C_D that is nearly 5 times more than the single oscillating airfoil due to the same down-washed vortex interaction. However, in this case, the out-of-phase oscillating downstream airfoil at $S_d = 2$ exhibits a different relationship between its average C_L and C_D production.

At pre-stall α range of $< \pm 20^\circ$ where the downstream airfoil at $S_d = 2$ generates consistently lower average C_L than the single oscillating airfoil, it produces the highest average C_D values when comparing to the rest of the cases as shown in Figure 5-30 (b). It attains the highest average C_D of 4.88 at $\alpha = \pm 10^\circ$ which also corresponds to the lowest average C_L of 0.038 thus, demonstrating a different relationship when comparing to its in-phase downstream counterpart which indicates different down-washed vortex interaction effects. This large average C_D increment during this pre-stall α range persists until $\alpha = \pm 20^\circ$. As α increases

from $\pm 20^\circ$ to $\pm 30^\circ$, the downstream airfoil at $Sd = 2$ exhibits a steep reduction in average C_D of about 79% as it attains its minimum average C_D of 0.83 which is still 4 times more than its upstream counterpart at $Sd = 2$ as shown in Figure 5-30 (b). As α increases from $\pm 40^\circ$ to $\pm 60^\circ$, it exhibits a similar increment in average C_D as its upstream counterpart at $Sd = 2$ which also corresponds to the steady linear decrease in average C_L . In this α range of $\pm 40^\circ$ to $\pm 60^\circ$, the downstream airfoil at $Sd = 2$ still produces consistently higher average C_D values than both the single oscillating airfoil and its upstream counterpart at $Sd = 2$.

As Sd increases from 2 to 2.5, the upstream airfoil retains a similar 'n' shaped average C_L trend as its upstream counterpart at $Sd = 2$ as it also attains its maximum average C_L at $\alpha = \pm 40^\circ$ as shown in Figure 5-30 (a). This 'U' shaped average C_L trends were also exhibited by the in-phase upstream oscillating airfoils presented in Section 5.5.1. However, the upstream airfoil at $Sd = 2.5$ is able to generate consistently higher average C_L values than the single oscillating airfoil across the entire α range tested as shown in Figure 5-30 (a) even at pre-stall α range of $< \pm 20^\circ$ which the upstream airfoil at $Sd = 2$ was unable to accomplish. At both $\alpha = \pm 10^\circ$ and $\pm 15^\circ$ cases, it generates average C_L values that are approximately 0.13 higher than its upstream counterpart at $Sd = 2$. Conversely, as α increases from $\pm 20^\circ$ to $\pm 60^\circ$, the upstream airfoil at $Sd = 2.5$ produces consistently lower average C_L values than its counterpart at $Sd = 2$. In this α range of between $\pm 20^\circ$ to $\pm 60^\circ$, it attains its maximum of 0.73 at $\alpha = \pm 40^\circ$ which is approximately 10% lower than its counterpart at $Sd = 2$ but is still 50% higher than the single oscillating airfoil. As Sd increases from 2 to 2.5, the upstream airfoil exhibits its largest decrease in average C_L of 0.14 at $\alpha = \pm 60^\circ$ which equates to a 21% reduction.

The effects of varying Sd on the out-of-phase upstream airfoil's average C_L performance differs from the in-phase upstream airfoil. The out-of-phase upstream airfoil at the larger Sd of 2.5 generates consistently lower average C_L values for all the cases at $\alpha > \pm 20^\circ$ than at $Sd = 2$ despite showing some lift improvements at the pre-stall conditions. On the other hand, the in-phase upstream airfoil at $Sd = 2.5$ produces consistently higher average C_L at most of the α cases than at the smaller Sd of 2. Thus, demonstrating that the effects of varying Sd on lift performance of these upstream oscillating airfoils are influenced by phase angles.

Unlike its upstream counterpart at $Sd = 2$ that produces low average thrust forces in the pre-stall α range of $\leq \pm 20^\circ$, the upstream airfoil at $Sd = 2.5$ exhibits a constant low average C_D averaging at 0.02 from $\alpha = \pm 10^\circ$ till $\alpha = \pm 40^\circ$. As the α increases from $\alpha = \pm 45^\circ$ to $\alpha = \pm 60^\circ$, the upstream airfoil at $Sd = 2.5$ exhibits a typical increasing C_D trend as the single oscillating

airfoil and upstream counterpart at $S_d = 2$ but it attains a higher maximum average C_D of 0.68 at $\alpha = \pm 60^\circ$ which relates to a 14.6% and 41% drag increment when compared to its upstream counterpart at 2 and the single oscillating case respectively.

Similar to its downstream counterpart at $S_d = 2$, the downstream airfoil at $S_d = 2.5$ also exhibits large improvements in average C_L across the entire α range tested when comparing its upstream counterparts and the single oscillating airfoil. However, it demonstrates a rather erratic C_L trend across the α range but in terms of lift enhancement ability, the downstream airfoil at $S_d = 2.5$ is more consistent than its counterpart at $S_d = 2$ as it manages to generate higher values at pre-stall conditions. At the pre-stall conditions of $\alpha \leq \pm 20^\circ$, the downstream airfoil at $S_d = 2.5$ outperforms its counterpart at $S_d = 2$ and the rest by attaining its first peak of 1.1 at $\alpha = \pm 15^\circ$. In the deep stall conditions at $\alpha \geq \pm 30^\circ$, the downstream airfoil at $S_d = 2.5$ exhibits a different average C_L trend when comparing to its counterpart at $S_d = 2$ as it shows an increasing trend with a 17% reduction in C_L as α increases from $\pm 40^\circ$ to $\pm 45^\circ$. It generates consistently lower average C_L values throughout the deep stall α range of $\geq \pm 30^\circ$ when comparing to its counterpart at $S_d = 2$ with the largest reduction of 21% at $\alpha = \pm 45^\circ$ as shown in Figure 5-30 (a). However, the downstream airfoil at $S_d = 2.5$ is the best lift producer at the largest α of $\pm 60^\circ$ as it attains the highest average C_L of 1.35 which also corresponds to a 31.7% increment when comparing to its downstream counterpart at $S_d = 2$.

As S_d increases from 2 to 2.5, the downstream airfoil produces consistently lower average C_D values throughout the entire α range tested with the exception at $\alpha = \pm 30^\circ$ when comparing to its downstream counterpart at $S_d = 2$. Similar to its counterpart at $S_d = 2$, the downstream airfoil at $S_d = 2.5$ also generates consistently higher average C_D values throughout the α range than the upstream counterparts and the single oscillating airfoil. The downstream airfoil at $S_d = 2.5$ exhibits a decreasing average C_D trend where it attains its maximum of 2.2 at $\alpha = \pm 15^\circ$ before decreasing steadily to its minimum of 0.2 at $\alpha = \pm 45^\circ$ which is 83% lower than its counterpart at $S_d = 2$.

In summary, at $S_d = 2$, the upstream airfoil produces consistently lower average C_L but higher negative average C_D in the pre-stall α range of $\alpha < \pm 20^\circ$ than the single oscillating airfoil. However, at $\alpha \geq \pm 20^\circ$, it produces consistently higher average C_L values of up to 94% while exhibiting subtle increments in average C_D at $\alpha = \pm 30^\circ$ to $\pm 40^\circ$ in relation to the single oscillating airfoil. As S_d increases from 2 to 2.5, the upstream airfoil generates higher

average C_L values than the single oscillating airfoil throughout the α range and is able to outperform its upstream counterpart at $Sd = 2$ at pre-stall α range of $< \pm 20^\circ$. However, at $\alpha > \pm 20^\circ$, the upstream airfoil at $Sd = 2.5$ generates consistently lower average C_L than its counterpart at $Sd = 2$ with a maximum reduction of 21% at $\alpha = \pm 60^\circ$. The effects of varying Sd on average C_L differs between the in-phase and out-of-phase upstream cases thus demonstrating the influence of phase angles on the upstream oscillating airfoil's lift performance. In terms of drag performance, the upstream airfoil at $Sd = 2.5$ performs poorly in terms of producing any thrust forces at pre-stall condition and generates consistently higher average C_D at $\alpha \geq \pm 45^\circ$ than its upstream counterpart at $Sd = 2$ and the single oscillating airfoil.

The downstream airfoil at $Sd = 2$ produces higher average C_D values of up to 85% increment at pre-stall α range of $< \pm 20^\circ$ despite producing correspondingly lower average C_L values when comparing to the single oscillating airfoil. This demonstrates a different relationship between C_L and C_D at pre-stall conditions for the out-of-phase downstream airfoil at $Sd = 2$ to its in-phase downstream counterparts which may indicate different down-washed vortex interaction effects. In the larger α range between $\pm 40^\circ$ to $\pm 60^\circ$, the downstream airfoil at $Sd = 2$ produces consistently higher average C_D values than both its upstream counterpart at $Sd = 2$ and the single oscillating airfoil in spite of exhibiting similar average C_D trends. As Sd increases from 2 to 2.5, despite its rather erratic average C_L trend, the downstream airfoil at $Sd = 2.5$ exhibits more consistent lift improvements over the entire α range tested than its counterpart at $Sd = 2$ due to its ability to generate the highest average C_L values even at pre-stall conditions. However, at deep stall conditions, the downstream airfoil at $Sd = 2.5$ produces consistently lower average C_L values of up to 21% with the exception at $\alpha = \pm 60^\circ$ where it is the best lift producer as it attains a 31.7% increment in relation to its downstream counterpart at $Sd = 2$. Conclusively, the downstream airfoil at both Sd exhibit consistently higher average C_D when comparing to its upstream counterparts and the single oscillating airfoil. This demonstrates the influence of down-washed vortex interactions in inducing higher positive drag forces for the downstream airfoil but as Sd increases, the detrimental effects on drag performance also reduces. In terms of achieving optimal aerodynamic performance, the downstream airfoil at $Sd = 2.5$ is the best choice due to (1) its consistency in generating high positive lift forces even at pre stall conditions while still producing lower drag forces than its downstream counterpart at $Sd = 2$ and (2) for its ability to sustain high positive lift forces at deep stall conditions while lowering its drag production.

5.6.2 Out-of-Phase Oscillating Tandem Airfoils Case at $Sd = 2$ and 2.5 for $\alpha = \pm 45^\circ$

(a)

(b)

Figure 5-31 : (a) Instantaneous C_L Plot and (b) Instantaneous C_D Plot for Out-of-Phase Tandem Oscillating Airfoils at $Sd = 2$ and 2.5 for $\alpha = \pm 45^\circ$; Green Dotted Lines from (a) to (i) illustrate the instantaneous force coefficients that correlate to the instantaneous vorticity contours in Figure 5-32

Figure 5-32 : Vorticity Contours for One Oscillation Cycle for Out-of-Phase Oscillating Tandem Airfoils at $Sd = 2$ and 2.5 for $\alpha = \pm 45^\circ$

Upstream Airfoil at Various Sd

In Figures 5-32 (a) to (b), where the out-of-phase oscillating upstream airfoil at both Sd start their upstrokes from Points (a) to (b), the CCW boundary layers have started gaining momentum and strength and their primary CCW LEVs formation have been initiated. When comparing the primary CCW LEV formations in Figure 5-32 (b), the upstream airfoil at $Sd = 2$ exhibits a CCW LEV formation with a $\zeta_{\max}$ of +0.04 and with a relatively thick boundary layer structure whereas its counterpart at $Sd = 2.5$ exhibits a CCW LEV formation with an approximately 12% lower $\zeta_{\max}$ of +0.035 and with a flatter and elongated CCW boundary layer structure. The thicker CCW boundary layer structure exhibited by the upstream airfoil at $Sd = 2$ at Point (b) may indicate a more aggressive turbulent breakdown event prior to its CCW LEV formation which induces a more thicker and turbulent boundary layer structure. This more turbulent CCW boundary layer structure may also be detrimental to the overall circulation over the suction side which reduces overall positive C_L and increase form drag forces for the upstream airfoil at $Sd = 2$ which may explain 20% lower C_L of 3.2 at Point (b) when comparing to its counterpart at $Sd = 2.5$ as shown in Figure 5-31 (a). To support the

detrimental effect on C_D due to this more turbulent CCW boundary layer structure, the upstream airfoil at $Sd = 2$ also produces a positive C_D of 0.4 at Point (b) which is 3 times more than the C_D of -0.02 produced by its upstream counterpart at $Sd = 2$. At Point (b), the single oscillating airfoil produces a smaller and weaker CCW LEV structure with a $\zeta_{\max}$ of about +0.02 and without any evidence of any excessive thickening of the CCW boundary layer structure as shown in Figure 5-20 (b) when comparing to the upstream airfoil at both Sd in Figure 5-32 (b). This smaller and 50% weaker CCW LEV formation by the single oscillating airfoil may explain its 37.5% and 22% reduction in C_L when comparing to the upstream airfoil at $Sd = 2$ and 2.5 respectively as it pitches from Points (a) to (b) during the 1st half of the upstroke.

At Point (a), the upstream airfoil at both Sd exhibit near identical shed vortex clusters in their trailing wakes as shown in Figure 5-32 (a). When comparing to the shed vortex cluster generated by the single oscillating airfoil at Point (a) in Figure 5-20 (a), it can be observed that whilst the CW_LEV_1 and CW_LEV_{s1} are near identical in terms of size and $\zeta_{\max}$, the single oscillating airfoil produces an approximately 30% shorter CW secondary vortex structure than the upstream airfoil at both Sd which may explain the lower thrust or negative C_D values from $t/T = 0$ to 0.1 when comparing to the upstream airfoil at both Sd as depicted in Figure 5-31 (b). In Figure 5-32 (b) at Point (b), the upstream airfoil at both Sd maintain a longer CW secondary vortex structure and exhibit identical primary and secondary shed CW vortices' growth rates when comparing to the single oscillating airfoil. Despite the identical progression and growth of their shed vortex clusters from Points (a) to (b), the upstream airfoil at $Sd = 2$ exhibit an sharp increase in C_D as it produces a positive C_D of 0.4 at point (b) which is nearly 3 times higher than its upstream counterpart at $Sd = 2.5$ and the single oscillating airfoil that are still producing thrust forces. This sharp increase in C_D from $t/T = 0.1$ to $t/T = 0.135$ in Point (b) exhibited by the upstream airfoil at $Sd = 2$ may be attributed to the more aggressive turbulent breakdown prior to the CCW LEV formation of the suction side which was explained earlier.

As the upstream airfoil at both Sd continues their upstroke from Points (b) to (c) as they approach their 1st stroke reversal, they experience similar critical flow events as the single oscillating airfoil where the CCW LEVs grow and propagate rearwards towards the TE as illustrated by the increase in their CCW LEV sizes over the suction side shown in Figures 5-32 (b) to (c). As α increases, the effect of adverse pressure gradients also increases resulting in the CCW LEV detachments shown in Figure 5-32 (c). Thereafter, dynamic stall occurs

which is characterized by the reduction in positive C_L before the 1st stroke reversal. For the upstream airfoil at $Sd = 2.5$, this reduction in C_L due to dynamic stall occurs at approximately $t/T = 0.19$ just before Point (c). However, for the upstream airfoil at $Sd = 2$, it exhibits a smooth C_L trend throughout the cycle as shown in Figure 5-31 (a) which may seem to indicate that the effects of dynamic stall (pre stroke reversals) and full stall events (post stroke reversals) on C_L are lesser or rather suppressed in relation to its upstream counterpart at $Sd = 2$ and the single oscillating airfoil. The CCW LEV and CCW boundary layer structures produced by the upstream airfoil at both Sd are similar to the single oscillating airfoil in Figure 5-20 (c) with identical early indication of flow separation at the TE as well. These similarities evidently support the similar C_L and C_D values at Point (c) produced by the upstream airfoil at both Sd and the single oscillating airfoil in Figures 5-31 (a) and (b) respectively.

As the upstream airfoil at both Sd experience their first stroke reversal at $t/T = 0.25$, their detached CCW LEVs are spilled over the TE due to the rapid change in α as shown in Figure 5-32 (d) at Point (d) which was similarly observed for the single oscillating airfoil at Figure 5-20 (d). The upstream airfoil at $Sd = 2$ produces a spilled CCW_LEV₁ of similar $\zeta_{\max}$ of +0.035 shown in Figure 5-32 (d) which supports the identical C_L trend and values from Points (c) to (d) in relation to the single oscillating airfoil. However, as Sd increases from 2 to 2.5, the upstream airfoil produces a stronger spilled CCW_LEV₁ with a higher $\zeta_{\max}$ of +0.04 which is around 15% higher than the spilled (CCW_LEV₁)s produced by its counterpart at $Sd = 2$ and the single oscillating airfoil as depicted in Figure 5-32 (d). This stronger spilled CCW_LEV₁ produced by the upstream airfoil at $Sd = 2.5$ may be due to slightly faster CCW boundary layer flow over the suction side which enable it to generate 20% higher C_L of 6 at Point (d) when comparing to its upstream counterpart at $Sd = 2$ and the single oscillating airfoil.

One of the major trade-off associated with faster boundary layer flow over the suction side is the likelihood of increased adverse pressure gradients effects leading to a more intensive flow separation event. At Point (d) where $\alpha_{ua, d} \approx +33.8^\circ$, similar to the single oscillating airfoil, the upstream airfoil at both Sd exhibit fully separated CCW boundary layer structures as depicted by the layer of CW (blue) contours over the suction side. This fully separated flow is typically associated with full stall taking effect after the first stroke reversal as documented by Lee and Gerontakos [60]. However, the upstream airfoil at $Sd = 2.5$ exhibit a thicker layer of CW contours over its suction side in Figure 5-32 (d) which indicates a higher degree of

CCW boundary layer flow separation than its upstream counterpart at $Sd = 2$ and the single oscillating airfoil shown in Figure 5-20 (d). This higher degree of flow separation agrees well with the fact that the flow over the suction side of the upstream airfoil at $Sd = 2.5$ is faster than its counterpart at $Sd = 2$ and the single oscillating airfoil as previously documented. Importantly, this more intensive flow separation experienced by the upstream airfoil at $Sd = 2.5$ give rise to higher form drag [77] which may explain the steep increase in C_D after the first stroke reversal as it attains its maximum C_D of 3.9 at $t/T = 0.36$ at Point (d) that is nearly 150% higher than both the upstream airfoil at $Sd = 2$ and the single oscillating airfoil as shown in Figure 5-31 (b).

As the upstream airfoil at both Sd pitch down towards the mid-way point in the cycle at $t/T = 0.5$ at Point (e), the spilled (CCW_LEV₁)s are all in the process of shedding into their trailing wakes as shown in Figure 5-32 (e) similar to the single oscillating airfoil in Figure 5-20 (e). However, the upstream airfoil at both Sd exhibit a slight lag in its CCW_LEV₁ shedding when comparing to the single oscillating airfoil as depicted by their shorter CCW_LEV₁ shedding structures shown in Figure 5-32 (e). The delay in shedding prolong the attachments of their CCW_LEV₁ for the upstream airfoil both Sd as they pitch down to Point (e) which may explain the longer sustainment of positive C_L till $t/T = 0.55$ whereas the single oscillating airfoil is only able to sustain positive C_L till $t/T = 0.51$ as shown in Figure 5-31 (a).

As the upstream airfoil at both Sd continue to pitch down into the negative α range from Points (e) to (f), they begin to shed their CCW secondary vortices and form their shed vortex clusters as shown in Figure 5-32 (f). However, the upstream airfoil at $Sd = 2.5$ exhibit slightly larger and stronger shed CCW_LEV₁ than its counterpart at $Sd = 2$ and the single oscillating airfoil shown in Figure 5-20 (f) which is attributed to the faster CCW boundary layer flow and 15% stronger CCW_LEV₁ formation documented earlier during the first half of the cycle. This marginally stronger and bigger shed CCW_LEV₁ exhibited by the upstream airfoil at $Sd = 2.5$ may explain the higher momentum supplement to the fluid at the trailing wake which supports the consistently higher negative average C_D or thrust values produced when comparing to its counterpart at $Sd = 2$ and the single oscillating airfoil from Points (e) to (f) as shown in Figure 5-31 (b).

In addition, the upstream airfoil at both S_d exhibit similar CW boundary layer structures over the pressure side with identical $\zeta_{\max}$ values as shown in Figure 5-32 (f) when comparing to the single oscillating airfoil shown in Figure 5-20 (f). However, the upstream airfoil at both S_d exhibit similar CCW boundary layer structures that stretch out from the center of the suction side as shown in Figure 5-32 (f) whereas the single oscillating does not exhibit any CCW boundary layer structure. These ‘residual’ CCW boundary layer structures are present at Point (f) due to the delay in the shedding of their CCW_LEV₁ and CCW secondary vortices which was documented earlier at Point (e). These ‘residual’ CCW boundary layers induce lower pressure over the suction sides which lower the negative Δp across the airfoil sides thus decreasing negative C_L production which may explain the consistently lower negative C_L values produced by the upstream airfoil at both S_d from Points (e) to (g) during the 2nd half of the downstroke when comparing to the single oscillating airfoil. In Figure 5-32 (f), the upstream airfoil at $S_d = 2$ exhibits a stronger ‘residual’ CCW boundary layer with a $\zeta_{\max}$ of +0.025 that is 40% higher than its counterpart at $S_d = 2.5$. Thus, enabling the upstream airfoil at $S_d = 2$ to produce a negative C_L of -2.3 at Point (f) which is 30% and 36% lower than its counterpart at $S_d = 2.5$ and the single oscillating airfoil respectively as shown in Figure 5-31 (a).

As the upstream airfoil at both S_d experience their 2nd stroke reversal at $t/T = 0.75$ in Point (g), they exhibit different extent of CW boundary layer flow separation over the pressure side with the upstream airfoil $S_d = 2.5$ exhibiting a higher degree of flow separation than its counterpart at $S_d = 2$ as shown in Figure 5-32 (g). This indicates that the upstream airfoil at $S_d = 2.5$ experiences more aggressive full stall effects after the 2nd stroke reversal which may explain the consistently higher positive C_D values throughout most of the 2nd half of its upstroke. On the other hand, as the upstream airfoil at $S_d = 2$ pitches from Points (g) to (h), it can be observed that its reattached CCW boundary layer structure is much stronger and defined than its counterpart at $S_d = 2.5$ which can be attributed to the stronger ‘residual’ CCW boundary layer observed in Point (h) due to the delayed shedding of its CCW vortices. With a stronger ‘residual’ CCW boundary layer, as it pitches to Point (h), the flow reattachment phase over the suction side is ‘aided’ thus producing stronger reattached CCW boundary layer structure at the smaller S_d of 2. With the development of stronger CCW boundary layer structure observed in Figures 5-32 (h) to (i), the upstream airfoil at $S_d = 2$ is able to decrease the negative Δp across the airfoil sides, thus lowering its negative C_L production which supports the consistently lower negative C_L values throughout the 2nd half

of its upstroke as shown in Figure 5-31 (a) than both its counterpart at $Sd = 2.5$ and the single oscillating airfoil.

Based on the average C_L results in Table 5-10, the out-of-phase oscillating upstream airfoil at both Sd are able to produce an average C_L of 0.74 which equates to a 94.7% increment in relation to the single oscillating airfoil. Despite producing similar average C_L values at $\alpha = \pm 45^\circ$, it can be concluded through this analysis that the primary causes for average C_L enhancement for the upstream airfoil at $Sd = 2$ and 2.5 are different. For the upstream airfoil at $Sd = 2$, its lift enhancement is primarily attributed to the production of consistently lower negative C_L values after the 2nd stroke reversal due to the stronger CCW boundary layer formation over its suction side. This strengthening of its CCW boundary layer structure is due to a more enhanced CCW boundary layer flow reattachment phase after the 2nd stroke reversal which is aided by the stronger ‘residual’ CCW boundary layer. As Sd increases from 2 to 2.5, the upstream airfoil is able to increase its average C_L due to its ability to generate 15% stronger CCW_LEV₁ formation during the first half of the cycle and to sustain positive C_L longer than the single oscillating airfoil through prolonging the shedding of its CCW_LEV₁ during its downstroke.

In terms of average C_D production, the upstream airfoil at $Sd = 2$ produces an average C_D of 0.08 which is comparable to the average C_D of 0.11 produced by the single oscillating airfoil. This similar average C_D is due to its similar extent of flow separations throughout the cycle as the single oscillating airfoil and also similar shed vortex cluster formations. However, as Sd increases to 2.5, the upstream airfoil produces a higher average C_D of 0.24 which is more than double of that produced by the single oscillating airfoil. Despite the stronger CCW_LEV₁ formation and higher C_L production during the first half of the cycle, it has its drawbacks as the faster CCW boundary layer flow give rise to more intensive flow separation effects and the consequential increase in form drag forces which supports the large C_D peak of 3.9 at $t/T = 0.36$ which equates to an approximately 150% increment when comparing to its counterpart at $Sd = 2$ and the single oscillating airfoil.

Downstream Airfoil at Various Sd

As the out-of-phase oscillating downstream airfoil at both Sd pitch down at the start of the cycle at Point (a), they are in the process of shedding their CCW secondary vortices and it can be observed that they are experiencing different down-washed vortex interactions as shown in Figure 5-32 (a). The downstream airfoil at Sd = 2 encounters 2 clusters of CCW down-washed vortices; the first cluster consists of the shed CCW_LEV₁ which interacts onto its pressure side and the second cluster consists of 2 shed CCW secondary vortices onto its suction side. Both of these clusters of interacting shed CCW vortices are of similar vortical strengths and sizes as shown in Figure 5-32 (a). In addition, these clusters are interacting at the same axial location along the chord where the downstream airfoil at Sd = 2 is at $\alpha_{da,d} = 0^\circ$. The ‘like-signed’ vortex interaction between the CCW boundary layer over the suction side and the shed CCW secondary vortices will increase the flow velocity and decrease pressure over the suction side whereas the ‘unlike-signed’ vortex interaction between the shed CCW_LEV₁ and the CW boundary layer flow over the pressure side will have the opposite effect. However, due to these similarities in vortex characteristic and interaction locations, these effects on the boundary layer flows over the airfoil sides will be cancelled each other out. This may explain the low C_L of near zero at Point (a) produced by the downstream airfoil at Sd = 2 despite these down-washed vortex interactions.

For the downstream airfoil at Sd = 2.5, it experiences a different down-washed vortex interaction as it encounters a shed CCW secondary vortex, CCW_LEV_{s1} which measures approximately 14.4mm in diameter with a ζ_{max} of + 0.025 and it interacts directly onto the LE as shown in Figure 5-32 (a). When comparing to the interacting shed vortices observed at Sd = 2, this interacting CCW_LEV_{s1} is smaller and weaker. Due to its direct interaction with the LE of the downstream airfoil at Sd = 2.5, this medium-sized interacting CCW secondary vortex does not affect the boundary layer flows over either of the airfoil sides. Thus similar to its counterpart at Sd = 2, due to the low impact of the down-washed vortex interaction, it supports the near zero C_L at Point (a) produced by the downstream airfoil at Sd = 2.5 as shown in Figure 5-31 (a).

Table 5-11 : Shed Vortex Cluster Characteristics at $\alpha_d = 0^\circ$ for Out-of-Phase Oscillating Upstream and Downstream Airfoil at $S_d = 2$ and 2.5

Cases	Instantaneous Cycle Point	Shed Vortex	Vortex Diameter, d (m) / Vortex Length, L _{vs} (m)	Absolute Max Core Vorticity, $\zeta_{\max}$ (°/s)	Instantaneous C _D
Upstream Airfoil at Sd = 2	At Point (e), t/T = 0.5 where $\alpha_{ua, d} = 0^\circ$	CCW_LEV ₁	23.08	0.035	0.27
		CCW Secondary Vortex Structure	11.54	0.02	
		CW_TEV ₁	8.65	0.03	
Upstream Airfoil at Sd = 2.5		CCW_LEV ₁	22.22	0.045	-0.8
		CCW Secondary Vortex Structure	25	0.025	
		CW_TEV ₁	19.44	0.015	
Downstream Airfoil at Sd = 2	At Point (a), t/T = 0 where $\alpha_{da, d} = 0^\circ$	CCW_LEV ₁	28.85	0.045	-1.13
		CCW Secondary Vortex Structure	28.85	0.035	
		CW_TEV ₁	None		
Downstream Airfoil at Sd = 2.5		CCW_LEV ₁	25	0.045	-3.87
		CCW Secondary Vortex Structure	33.78	0.035	
		CW_TEV ₁	None		

Similar to its upstream counterparts, the downstream airfoil at both S_d produce similar shed vortex clusters in their trailing wakes at Point (a) which comprise of CCW_LEV₁ and CCW secondary vortex structures as shown in Figure 5-32 (a). These shed vortex clusters produced by the downstream airfoil at both S_d do not exhibit any shed CW_TEV₁. In order to effectively evaluate these shed vortex clusters and the effects of down-washed vortex interactions on the downstream airfoil, the shed vortex clusters in Figure 5-32 (a) at $t/T = 0$ in Point (a) where $\alpha_{d, da} = 0^\circ$ will be compared to the shed vortex clusters produced by the upstream airfoil at both S_d at the same α and stroke at $t/T = 0.5$ in Point (e) where $\alpha_{d, ua} = 0^\circ$ as shown in Figure 5-32 (e). Table 5-11 tabulates these shed vortex clusters characteristics for the downstream and upstream airfoil at both S_d at $\alpha_d = 0^\circ$ during their respective downstroke. Due to the elongated CCW secondary structure, the approximate length of the structure is measured instead of the vortex diameter, d which is more applicable to the shed CCW_LEV₁ and CW_TEV₁. The instantaneous C_D values obtained from Figure 5-31 (b) are also tabulated in Table 5-11 to facilitate this comparative analysis. For the previous in-phase oscillating tandem case at $\alpha = \pm 45^\circ$ and the single oscillating airfoil case, a stronger and larger primary shed vortex coupled with a longer and more defined secondary vortex structure and the ability to preserve the shed TEV are all the desirable characteristics of a shed vortex cluster that is responsible for supplementing more momentum to the fluid at the trailing wake and consequently, producing more thrust.

Based on Table 5-11, the upstream airfoil at $S_d = 2.5$ in Figure 5-32 (e) produces an approximately 92.5% longer and 25% stronger CCW secondary vortex structure with a 125% larger but 50% weaker shed CW-TEV₁ when comparing to its upstream counterpart at $S_d = 2$. Despite producing a similar sized CCW-LEV₁, the upstream airfoil at $S_d = 2$ produces a positive C_D of 0.27 whereas its counterpart at $S_d = 2.5$ generates thrust or negative C_D of -0.8. From this comparison, it highlights the importance of generating a longer secondary vortex structure as well as having a larger TEV in terms of producing higher instantaneous thrust. The downstream airfoil at $S_d = 2.5$ in Figure 5-32 (a) produces an approximately 13.3% smaller CCW-LEV₁ but a 17% longer CCW secondary vortex structure when comparing to its counterpart at $S_d = 2$ as shown in Table 5-11. The downstream airfoil at both S_d produce shed vortices of equal strength with no shed TEVs. Despite the downstream airfoil at $S_d = 2$ producing a larger CCW-LEV₁, it generates a C_D of -1.13 which is more than 3 times lesser than its counterpart at $S_d = 2.5$ which produced a C_D of -3.87 thus indicating higher thrust.

At Point (e), the upstream airfoil at $S_d = 2.5$ is able to produce C_D difference of 1.07 due to its ability to produce a secondary vortex structure that is twice as long and a TEV that is doubled in size when comparing to its counterpart at $S_d = 2$. On the other hand, at Point (a), the downstream airfoil at $S_d = 2.5$ is able to produce a much larger C_D difference of 2.74 but the only difference in the downstream airfoils' shed vortex clusters is the 17% longer CCW secondary vortex structure. Hence, it can be deduced that the 17% longer CCW secondary vortex structure exhibited by the downstream airfoil at $S_d = 2.5$ do not contribute entirely to total increase in thrust. The higher intensity of down-washed vortex interactions experienced by the downstream airfoil at $S_d = 2$ when comparing to its counterpart at $S_d = 2.5$ as shown in Figure 5-32 (a) may give rise to an increase in positive C_D or a reduction in thrust for the downstream airfoil at $S_d = 2$, thus supporting the larger C_D difference of 2.74 observed at Point (a) between the downstream cases. This correlation between higher intensity of down-washed vortex interactions due to smaller S_d and the production of higher drag forces was also previously documented by Broering and Lian [44, 45].

As the downstream airfoil at both S_d continue their downstroke from Points (a) to (b), they continue shedding their CCW secondary vortices. The two interacting shed CCW vortex clusters over the suction and pressure sides of the downstream airfoil at $S_d = 2$ have propagated downstream towards mid-chord while hovering over the airfoil surfaces as shown in Figure 5-32 (b). From in-phase oscillating tandem cases in Section 5.5, the present study

discussed on the effects of ‘like-signed’ and ‘unlike-signed’ vortex interactions where they add and reduce energy of the interacted boundary layer flow respectively and can be termed as ‘direct’ vortex interactions. However, for this case at Point (b) in Figure 5-32 (b), these interacting shed CCW vortices hover over the surfaces instead of directly interacting onto them, thus they can be termed as ‘indirect’ vortex interactions. With these ‘indirect’ vortex interactions, the effects on the boundary layer flows are opposite to the ‘direct’ vortex interactions. To illustrate this point, take for example, the ‘indirect’ vortex interaction between the interacting CCW_LEV₁ and the CW boundary layer over the pressure side of the downstream airfoil at $Sd = 2$ in Figure 5-32 (b). As the interacting CCW_LEV₁ hovers over the CW boundary layer, the small gap in between induces a ‘jet’ flow region where the flow accelerates due to the counter-rotating directions which is illustrated by the black arrow in Figure 5-32 (b). Due to this accelerated ‘jet’ flow, it reinforces the CW boundary layer flow over the pressure side which will explain the 60% higher $\zeta_{\max}$ of -0.04 exhibited by the downstream airfoil at $Sd = 2$ when comparing to its counterpart at $Sd = 2.5$ which do not exhibit any down-washed vortex interactions as shown in Figure 5-32 (b). On the other hand, the 2nd ‘indirect’ vortex interaction between the interacting CCW_LEV_{s1} and the CCW boundary layer over the suction side induces a ‘treadmill’ flow region as flow decelerates due to the same rotational directions. As a result, the CCW boundary layer flow reduces velocity and pressure increases over the suction side. In summary, due to these ‘indirect’ vortex interactions, the flow over the pressure side accelerates and decelerates over the suction side, thus increasing the negative Δp across the sides which increases negative C_L at Point (b) for the downstream airfoil at $Sd = 2$. This supports the ability of the downstream airfoil to produce consistently higher C_L values of up to 50% as these hovering CCW vortices propagates downstream from Points (a) to (c) as Sd increases from 2 to 2.5.

One major drawback of these ‘indirect’ vortex interactions experienced by the downstream airfoil at $Sd = 2$ is that as these shed vortices propagates downstream and hover over the airfoil surfaces, they induce higher positive drag forces which may explain the consistently higher C_D values from Points (a) to (c) during the 1st half of the downstroke in relation to its counterpart at $Sd = 2.5$ as shown in Figure 5-31 (b). Meanwhile, as the downstream airfoil at $Sd = 2.5$ pitches down from Points (a) to (c), it is able to maintain a strong shed vortex cluster without much down-washed vortex interactions thus supporting the consistently higher thrust or negative C_D values throughout its first half of the downstroke.

As the downstream airfoil at both S_d experience their first stroke reversal and pitch up from Points (c) to (d), they spilled their CW_LEV_1 over the TE as shown in Figure 5-32 (d) which are of similar sizes and exhibit identical ζ_{max} of -0.04. This indicates that the strength of their CW boundary layers over the pressure sides which are responsible for development these spilled CW_LEV_1 are of equal strength. The downstream airfoil at $S_d = 2$ also exhibit the start of the ‘indirect’ vortex interaction with the interacting shed CW_LEV_1 and CW_LEV_{s1} near its LE over the suction side. As this interaction is still in its infancy stage, the effect on the CCW boundary layer is minimal at Point (d). Hence, due to the similar CW boundary layers and spilled CW_LEV_1 formations, it supports the similar negative C_L values from Points (c) to (d) produced by the downstream airfoil at both S_d as shown in Figure 5-31 (a).

In Figure 5-32 (d), the interacting shed CCW_LEV_{s1} over the downstream airfoil at $S_d = 2$ propagates downstream to the TE where it interacts with the spilled CW_LEV_1 which resembles a von Kármán alternating shed vortex pairing. Thus, applying the Von Kármán vortex street concept [48, 51], a ‘jet-force’ vector can be drawn pointing upstream in between the spilled CW_LEV_1 and the interacting CCW_LEV_{s1} as illustrated in Figure 5-32 (d) which indicates a ‘drag-producing’ Von Kármán vortex street. At this Point (d), both the shed vortex clusters formed by the downstream airfoil at both S_d have dissipated as they propagate downstream, thus their contribution to thrust production are minimal. It can then be deduced that due to the interaction of the shed CCW_LEV_{s1} and the spilled CW_LEV_1 at the TE, the induction of a ‘drag-producing’ shed vortex pairing may explain the high C_D of 3 by the downstream airfoil at $S_d = 2$ at Point (d) which is 3 times higher than its counterpart at $S_d = 2.5$ as shown in Figure 5-31 (b).

As the downstream airfoil at $S_d = 2$ pitches up from Points (d) to (e) towards the mid-point of the cycle, the interacting CCW_LEV_{s1} propagates further downstream together with the shed CW_LEV_1 to form part of the shed vortex cluster as shown in Figure 5-32 (e). In this process of shedding this alternating vortex pairing, positive C_D decreases thus, supporting the reduction in positive C_D from $t/T = 0.36$ in Point (d) to $t/T = 0.40$ as shown in Figure 5-31 (b). It also encounters a pair of CW shed vortices, CW_LEV_1 and CW_LEV_{s1} over its suction side as shown Figure 5-32 (e). Initially, at Point (d), these interacting shed CCW vortices are orientated on top of each other but it pitches down, the bottom shed CW_LEV_{s1} is being ‘pushed’ forwards which results in the ‘side by side’ orientation that is observed in Figure 5-32 (e). As these shed interacting CW vortices hover over the suction side of the downstream

airfoil at $Sd = 2$ in this orientation, these ‘indirect’ vortex interactions induce an accelerated ‘jet’ flow region as illustrated by the black arrow in Figure 5-32 (e). This ‘jet’ flow region increases the CCW boundary layer flow velocity over the suction side and in turn increasing positive C_L which supports the steady increase in C_L as the downstream airfoil at $Sd = 2$ pitches up from Points (d) to (e). One drawback of having this accelerated CCW boundary layer is that form drag forces [77] also increase which may explain the sharp increase in positive C_D from $t/T = 0.4$ to 0.45 as the downstream airfoil at $Sd = 2$ attains its maximum of 5 which is about 67% more than its counterpart at $Sd = 2.5$. In the same phase of the downstroke from Points (d) to (e), the downstream airfoil at the larger $Sd = 2.5$ experiences the same two interacting shed CW vortices over its suction side with the same change in orientation but with a slight lag due to the larger axial spacing as shown in Figures 5-32 (d) to (e). These similarities in down-washed vortex interactions between the downstream cases at both Sd give rise to the similar increasing positive C_L and C_D trends from Points (d) to (e) as shown in Figures 5-31 (a) and (b) respectively.

As the downstream airfoil at both Sd continue to pitch up into the positive α range from Points (e) to (f) where $\alpha_{da, u} \approx +38^\circ$, the interacting shed CCW vortices hovering over the suction side continue to reinforce the CCW boundary layer flows which enhance the formation of their respective primary CCW LEVs, thus supporting the production of high positive C_L peaks of about 12 at $t/T = 0.59$ and at $t/T = 0.56$ for the downstream airfoil at $Sd = 2$ and 2.5 respectively which is more than double the positive C_L peaks attained by their upstream counterparts during the first half of the cycle. In absolute terms, the downstream airfoil at both Sd are able to attain a 20% higher positive C_L peak during the 2nd half of the cycle than the negative C_L peak attained during the first half of the cycle. The downstream airfoil at $Sd = 2.5$ also attains its positive C_L peak slightly later due to the lag in the down-washed vortex interactions due to the larger axial spacing when comparing to its counterpart at $Sd = 2$.

In Figure 5-32 (f), the downstream airfoil at $Sd = 2$ exhibits a separated CCW boundary layer structure and the two interacting shed CW vortices have propagated further downstream and away from the suction side as it pitches up. This flow separation over the suction side may be accelerated due to the accelerated CCW boundary flow and consequential increase in adverse pressure gradients [77] as a result of the ‘indirect’ vortex interactions. The effects of this accelerated flow separation coupled with the diminishing ‘constructive’ effect of the

‘indirect’ vortex interactions as the interacting vortices propagate further downstream supports the sharp 47% reduction in C_L at Point (f) after attaining its peak C_L at $t/T = 0.59$ as shown in Figure 5-31 (a).

At Point (f), the downstream airfoil at $Sd = 2.5$ exhibits a different CCW boundary layer structure when comparing to its counterpart at $Sd = 2$. In Figure 5-32 (f), it can be observed that the interacting shed CCW_LEV_{s1} attaches to the primary CCW_LEV_1 which appears to be prematurely displaced rearwards towards to TE. Comparing the ‘indirect’ vortex interactions at Point (e) between the downstream cases, it is apparent that the interacting CCW_LEV_{s1} hovers much closer to the suction side at the larger Sd . Due to this closer proximity, as the downstream airfoil at $Sd = 2.5$ pitches up from Points (e) to (f), the primary CCW LEV formation displaces prematurely, thus undergoing rapid LEV displacement which have been documented by Broering and Lian [44, 45]. This rapid LEV displacement over the suction side disrupts the formation and growth of the primary CCW LEV which decreases positive C_L , thus supporting the steep 85% reduction in C_L after attaining its peak at $t/T = 0.56$ to Point (f).

As the downstream airfoil at both Sd experience their 2nd stroke reversal from Points (g) to (h), they exhibit similar CCW boundary layer structure with ζ_{max} of +0.04 and have not been influenced by any down-washed vortex interactions yet as shown in Figure 5-32 (h). In addition, their primary CCW_LEVs have spilled over the TE and remain attached; exhibiting similar prolonged LEV attachments which may explain the similar high positive C_L values produced by the downstream airfoil at both Sd from Points (g) to (h) as shown in Figure 5-31 (a). As the downstream airfoil at $Sd = 2$ exhibits an accelerated flow separation event due to the ‘indirect’ vortex interaction effects prior to the second stroke reversal documented in Point (f), this give rise to a more aggressive full stall as it pitches to Point (h) as depicted by the higher degree of CCW boundary layer flow separation in Figures 5-32 (g) and (h) in relation to its downstream counterpart at $Sd = 2.5$. Meanwhile, its counterpart at $Sd = 2.5$ experiences rapid CCW LEV displacement prior to the second stroke reversal at Point (f) which give rise to a more attached CCW boundary layer structure after the 2nd stroke reversal as shown in Figures 5-32 (g) and (h), thus indicating a less aggressive full stall. The more aggressive nature of the full stall experienced by the downstream airfoil at $Sd = 2$ supports the consistently higher positive C_D values during the 2nd half of the downstroke from Points (g) to (h) when comparing to its downstream counterpart at $Sd = 2.5$ which exhibits a gradual increase in positive C_D as shown in Figure 5-31 (b).

In conclusion, the out-of-phase oscillating downstream airfoil at $Sd = 2$ is able to produce an average C_L of 1.32 which is 94% and 78% higher than the single oscillating airfoil and the upstream airfoil at both Sd respectively as shown in Table 5-10. This higher average C_L by the downstream airfoil at $Sd = 2$ is attributed to the ‘constructive’ effect of ‘indirect’ down-washed vortex interactions on C_L during the 2nd half of the cycle. These ‘indirect’ vortex interactions consists of the interaction between two shed CW vortices that hover over the suction side inducing an accelerated ‘jet’ flow region that accelerates the CCW boundary layer flow which then reinforces its CCW LEV formation during the 2nd half of the upstroke. This stronger CCW LEV formation enables the downstream airfoil at $Sd = 2$ to attain a higher C_L peak of 12 at $t/T = 0.59$ which is more than doubled that achieved by the upstream airfoil at both Sd during the 1st half of their cycle.

As Sd increases from 2 to 2.5, the out-of-phase oscillating downstream airfoil produces an average C_L of 1.04 which 21% lower than its downstream counterpart at $Sd = 2$ but is still higher than the single oscillating airfoil and the upstream airfoil at both Sd . This 21% reduction in average C_L at $Sd = 2.5$ may be attributed to the rapid CCW LEV displacement that occurs during the 2nd half of the upstroke due to the vortex interaction between the shed CW_LEV_{s1} and its primary CCW LEV over the suction side. Due to the close proximity of this vortex interaction, it prematurely displaces the primary CCW LEV rearwards towards the TE as shown in Figure 5-32 (h) which disrupts the growth of the CCW LEV, thus causing a steeper reduction in positive C_L at Point (h) when comparing to its downstream counterpart at $Sd = 2$.

The downstream airfoil at $Sd = 2$ produces an average C_D of 1.26 which is the highest when comparing to the rest of the cases presented in Table 5-10. This higher average C_D at $Sd = 2$ may be attributed to the consistently higher positive C_D values throughout the 1st half of the cycle due to the effects of higher intensity of down-washed vortex interactions when comparing to its counterpart at $Sd = 2.5$. It also produces much higher C_D values after the 2nd stroke reversal than its counterpart at $Sd = 2.5$ due to the more aggressive full stall effects brought about by the ‘indirect’ vortex interactions over its suction side prior to the 2nd stroke reversal. This also demonstrates that the inherent trade-off between C_L and C_D of these ‘indirect’ down-washed vortex interactions.

On the other hand, the downstream airfoil at $S_d = 2.5$ produces a low average C_D of 0.21 which is 12.5% lower than its upstream counterpart. This low average C_D production by the downstream airfoil at $S_d = 2.5$ may be attributed to the consistently high thrust production during the 1st half of the downstroke due to its ability to maintain a stronger shed vortex cluster thus adding more momentum to the fluid at the trailing wake. In addition, during this phase of the cycle, the downstream airfoil at $S_d = 2.5$ does not experience any detrimental effects on C_D due to down-washed vortex interactions unlike its downstream counterpart at $S_d = 2$. After the 2nd stroke reversal, it also produces consistently lower C_D values and at a much gradual increasing trend when comparing to the downstream airfoil at $S_d = 2$ as it experiences less aggressive full stall effects brought about by the rapid CCW LEV displacement prior to the 2nd stroke reversal.

5.6.3 Combined Average Force Characteristics of Oscillating Tandem Airfoils at Various Phase Angles

(a)

(b)

Figure 5-33 : (a) Combined Average C_L and (b) Combined Average ΔC_L for Oscillating Tandem Airfoils at $Sd = 2$ and 2.5 for $\phi = 0^\circ$ and 180°

(a)

(b)

Figure 5-34 : (a) Combined Average C_D and (b) Combined Average ΔC_D for Oscillating Tandem Airfoils at Sd = 2 and 2.5 for $\phi = 0^\circ$ and 180°

After the investigation on the aerodynamics of the oscillating tandem airfoils at both $\phi = 0^\circ$ and $\phi = 180^\circ$, it is important that to provide an overview of the primary effects of phase angles at the various α cases and also to evaluate the overall aerodynamic performance of tandem oscillating airfoils in relation to the single oscillating airfoil. In order to facilitate this evaluation, the average C_L and C_D values from the upstream and downstream airfoils are combined and normalized using Equations 5.1 and 5.2 respectively which were earlier presented in Section 5.5.4.

With reference to Figure 5-33 (a), the in-phase oscillating tandem configuration at both Sd exhibit rather mixed average $C_{L,combined}$ results in relation to the single oscillating case. The in-phase tandem oscillating tandem configuration provides its lift enhancement advantage only at larger $\alpha > \pm 30^\circ$ with the maximum lift improvement shown at $\alpha = \pm 45^\circ$. At smaller $\alpha \leq \pm 20^\circ$, the in-phase oscillating tandem configuration did not offer much lift benefits in relation to the single oscillating airfoil with the worst lift performance at $\alpha = \pm 15^\circ$.

The average $C_{L,combined}$ results for the in-phase oscillating tandem configuration is moderately affected by the various in Sd as it exhibit the biggest ΔC_L of 0.14 between Sd of 2 and 2.5 at $\alpha = \pm 20^\circ$. Based on the ΔC_L plot in Figure 5-33 (b), it is apparent that the $C_{L,combined}$ performance is consistently better at the smaller Sd of 2 when compared to the larger Sd of 2.5. The in-phase oscillating tandem airfoils provide the maximum lift improvement of 44.7% at $Sd = 2$ with $\alpha = \pm 45^\circ$ in relation to the single oscillating airfoil. Based on the investigation of the in-phase oscillating tandem airfoils at $\alpha = \pm 45^\circ$ in Section 5.5.3, this large lift improvement is attributed to the downstream airfoil's ability at $Sd = 2$ to generate higher positive C_L values in the first half of the cycle due to the down-washed vortex interaction with the primary shed CCW LEV before the first stroke reversal that enables the substantial enlargement of its early detached primary CCW LEV over the suction side which increases positive lift production.

As ϕ changes from 0° to 180° , the average $C_{L,combined}$ performance also changes for these oscillating tandem configuration where it outperform both the single oscillating airfoil and the in-phase oscillating tandem configuration at $\alpha > \pm 15^\circ$ with its best lift performance at $\alpha = \pm 45^\circ$. For these out-of-phase oscillating tandem configuration, the effects of varying Sd on the average $C_{L,combined}$ are more evident than with the in-phase oscillating tandem configuration and are also more influenced by α . At pre-stall conditions of $\alpha < \pm 20^\circ$, the smaller Sd of 2 have proven to be detrimental to average $C_{L,combined}$ with its worst case at the smallest α of $\pm 10^\circ$ which relates to a 65% lift reduction in relation to the single oscillating airfoil. However, at larger $\alpha \geq \pm 20^\circ$ as displayed in Figure 5-34 (a), the out-of-phase oscillating tandem configuration at both Sd demonstrated their ability to generate large lift improvements in relation to both the single oscillating airfoil and the in-phase oscillating tandem configuration. In these 'deep stall conditions', the smaller Sd of 2 provides higher average $C_{L,combined}$ performance with the highest of 1.16 at $\alpha = \pm 40^\circ$ which corresponds to the largest lift improvement of 136% in relation to the single oscillating case. Through the investigation of the out-of-phase oscillating tandem airfoils at $\alpha = \pm 45^\circ$ presented in Section 5.6.2, this large lift improvement is attributed to the downstream airfoils' ability to generate remarkably high lift forces due to the 'constructive' effects of the 'indirect' down-washed vortex interactions over the suction side that resulted in an acceleration of CCW boundary layer flow which produces stronger CCW LEV formation.

In terms of the average $C_{D,combined}$ shown in Figure 5-34 (a), the in-phase oscillating tandem configuration at both S_d exhibit similar increasing trends without much variation. Importantly, it demonstrates that the in-phase tandem oscillating configuration at both S_d are not ideal for thrust production as they produce positive average $C_{D,combined}$ values throughout the α range tested. The single oscillating airfoil produces a stabilized region of between $\alpha = \pm 15^\circ$ to $\pm 40^\circ$ where it generates constant positive thrust forces averaging at $C_{D,combined}$ of -0.14. The in-phase oscillating tandem configuration produces its highest average $C_{D,combined}$ of 0.74 at $\alpha = \pm 60^\circ$ which relates to a drag increment of 54% when comparing to the single oscillating airfoil. The investigation of in-phase oscillating tandem configuration at $\alpha = \pm 45^\circ$ reveals that this drag increment is attributed to the downstream airfoil's ability to produce higher positive C_D than the single oscillating airfoil due to the vortex interactions with the primary LEVs shed by their upstream counterparts that cause the detachments and subsequent enlargements of their primary LEVs which induce higher drag forces.

The out-of-phase oscillating tandem configuration at both S_d are capable of producing large lift improvements of up to 136% over the single oscillating airfoil. However, this lift improvement does come with its trade-off as it exhibits consistently higher average $C_{D,combined}$ than the single oscillating airfoil across the α range tested. The average $C_{D,combined}$ production is also greatly affected by the variation in S_d . At $S_d = 2$, the out-of-phase oscillating tandem configuration generates the highest average $C_{D,combined}$ of 2.36 at $\alpha = \pm 10^\circ$ which relates to 5 times the amount produced by the single oscillating airfoil. These out-of-phase tandem oscillating airfoils greatly improve their average $C_{D,combined}$ as $\alpha > \pm 20^\circ$ especially so at the larger S_d of 2.5. With the out-of-phase oscillating tandem configuration at $S_d = 2.5$, the average $C_{D,combined}$ performance is in fact comparable to the in-phase oscillating tandem configuration at larger $\alpha \geq \pm 40^\circ$. Based on the findings in Section 5.6.2, this drag reduction at $\alpha = \pm 45^\circ$ is attributed to its ability to maintain stronger shed vortex cluster during the 1st half of its downstroke and due to less aggressive full stall effects after the 2nd stroke reversal brought about by the rapid CCW LEV displacement prior to the 2nd stroke reversal. However, at the smaller S_d of 2, the out-of-phase oscillating tandem airfoils produces a high average $C_{D,combined}$ of 0.67 which relates to 5 times the average C_D produced by the single oscillating airfoil. This large $C_{D,combined}$ increment is accredited to the downstream airfoil's ability to generate higher positive C_D due to the higher intensity of down-washed vortex interactions during the first half of cycle and the aggressive full stall effects after the 2nd stroke reversal due to 'detrimental' effects of the 'indirect' vortex interaction prior to the 2nd stroke reversal.

Figure 5-35 : Normalized Combined C_L / C_D Ratio Plot for Tandem Oscillating Airfoils at $S_d = 2$ & 2.5 and $\phi = 0^\circ$ & 180°

Regardless of variations in S_d and ϕ tested, these oscillating tandem airfoils are not ideal for thrust generation unlike the single oscillating airfoil. The in-phase oscillating airfoils at the smaller S_d of 2 performed generally better in relation to its $S_d = 2.5$ counterpart in generating consistently higher C_L / C_D ratios across the α range as shown in Figure 5-35. As ϕ increases from 0° to 180° , these tandem oscillating configuration demonstrated their ability to significantly increase the C_L / C_D ratios at larger $\alpha > \pm 30^\circ$ especially so at $S_d = 2.5$ due to its ability to generate large lift improvements and suppressing drag production through effectively capitalizing on its ‘indirect’ down-washed vortex interaction effects, thus making out-of-phase oscillating tandem configuration at $S_d = 2.5$ the most ideal configuration in the present study, potentially viable for hovering flight.

5.7 VORTEX SHEDDING MODEL

In this section, the Vortex Shedding model will be presented in order to explain the concept of formulating the ‘jet-force’ vectors that were consistently applied to the rationalization of the present study’s experimental findings. Firstly, the concept behind this Vortex Shedding Model originates from observing these counter-rotating shed vortices that make up the typical Von Kármán street and to deduce a method in which the momentum deficit or surplus of a Von Kármán street can be approximated in a simple yet effective approach. This concept of the Vortex Shedding Model involves approximating the individual shedding velocities which will be subsequently used to determine the respective shed vortex circulations. Based on these shed vortex circulation and orientation of these shed vortices, the present study was able to predict the magnitudes and directions of these induced ‘jet-force’ vectors. Based on the von Kármán vortex street momentum concept presented earlier in Section 3.3, the directions of these induced ‘jet-force’ vectors will indicate whether the vortex street is a ‘drag-inducing’ or ‘thrust-inducing’ one. The remainder of this section will further elaborate on the concepts behind this Vortex Shedding analytical model.

$$\Gamma_{lev} = \pi \cdot d \cdot V_{\theta} \quad (\text{Eq. 5.8})$$

$$\Gamma_{vs} = \pi \cdot d \cdot V_{vs} \quad (\text{Eq.5.9})$$

Firstly, from reviewing literature papers on the generation of LEV by van den Berg and Ellington [85] and also studying the effects on vortical patterns on thrust production by Koochesfahani [42], the circulations and the orientation of these shed vortices in the convected trailing wake do have significant contributions to the resultant velocity distribution in the wake which will provide an indication of drag or thrust production as documented by Von Kármán and Burgers [51]. Importantly, van den Berg and Ellington [85] presented Equation 5.8 in order to calculate the LEV circulation, Γ_{lev} based on the average vortex diameter, d and the swirl velocity, V_{θ} . In van den Berg and Ellington [85] study, the swirl velocity was assumed to be equal to the axial velocity, V_a as presented in Section 3.3.2. Hence, applying this same concept to the vortices shed in the trailing wake, the circulation of these shed vortices, Γ_{vs} can be similarly approximated using Equation 5.9 where the vortex diameter, d is taken as the average of the height and width of the shed vortex and V_{vs} denotes the axial vortex shedding velocity.

Figure 5-36: Vorticity Contours for In-Phase Tandem Static Case at $Sd = 2$ and 2.5 for $\alpha = +15^\circ$; (a) $t = 0s$ and (b) $t = 0.281s$

Table 5-12: Vortex Shedding Model Parameters for In-Phase Upstream Static Airfoil at $Sd = 2$ and 2.5 for $\alpha = +15^\circ$

Case	Vortex	Axial Vortex Shedding Velocity, V_{vs} (m/s)	Vortex Diameter, d (m)	Shed Vortex Circulation, Γ_{vs} (m^2/s)	Total Shed Vortex Circulation, Γ_{vs_total} (m^2/s)	Average C_D
Upstream Static Airfoil at $Sd = 2$	CCW Vortex A	0.041	0.017	0.0022	0.00251	0.15
	CW Vortex B	0.0082	0.012	0.00031		
Upstream Static Airfoil at $Sd = 2.5$	CCW Vortex A	0.039	0.0188	0.0023	0.00288	0.25
	CW Vortex B	0.0103	0.0176	0.00058		

The vorticity contours for the in-phase static tandem case at $\alpha = +15^\circ$ are used as an example to illustrate the Vortex Shedding model concept in order to deduce the ‘induced’ drag or thrust forces for static upstream airfoil through analyzing the vortex shedding structure as shown in Figure 5-36. The focus will be on the alternating shed vortex pairing circled in Figure 5-36 which consists of the CCW shed vortex A and CW shed vortex B that forms a typical von Kármán vortex street [48]. Subsequently, red and blue dotted lines are plotted in Figure 5-36 to trace the displacement for Vortex A and B respectively from $t = 0$ to $t = 0.281s$ in order to compute the vortex shedding velocities. These dotted lines are drawn from the approximate center of the vortices. With these displacements measured using the red and blue dotted lines, the vortex shedding velocities for Vortex A and Vortex B can be calculated by dividing with the time interval, Δt of $0.281s$. With reference of Figure 5-36 (b), the vortex diameters, d for these shed vortices can be measured. Lastly, with V_{vs} and d obtained, the shed vortex circulations, Γ_{vs} can then be calculated using Equation 5.9. These computed Vortex Shedding Model parameters are tabulated in Table 5-12.

Based on the orientation and the rotational directions of the shed Vortex A and Vortex B pairings, a ‘jet-force’ vector can be drawn in between these shed vortices for the both Sd cases as illustrated in Figure 5-36 (b). Both of these ‘jet-force’ vectors drawn are pointing upstream which indicate that the upstream airfoil at both Sd are producing ‘drag-inducing’ von Kármán vortex streets as explained earlier in Section 3.3. Next, it can be assumed that the magnitude of the ‘jet-force’ vector is proportional to the total shed vortex circulations, Γ_{vs_total} which is simply the summation of the respective vortex shedding circulations. In this vortex shedding model, it is assumed that the Γ_{vs_total} will be able to deduce the magnitude of the ‘induced-wake’ drag force produced by the respective airfoil. Based on the calculated

Γ_{vs_total} presented in Table 5-12, the upstream airfoil at $Sd = 2.5$ produces a 14.7% stronger ‘jet-force’ vector than its upstream counterpart at $Sd = 2$ which importantly supports the higher average C_D of 0.25 produced by the upstream static airfoil at $Sd = 2.5$.

In summary, this vortex shedding model is able to provide an effective method to evaluate the influence of von Kármán vortex streets on drag or thrust production based on the shed vortex characteristics observed. This concept can also be applied to shed vortex interactions with boundary layer flows but computing the boundary layer circulations can be tricky thus, comparing their core vorticities, ζ_{max} would be more ideal than calculating the vortex shedding model parameters such as the ones presented in Table 5-12.

6. CONCLUSIONS

The primary motivation for the present study is to provide the better understanding into unsteady aerodynamics at low Reynolds number flows for the application of Micro Ariel Vehicles (MAV)s. Inspired by natural small fliers such as the dragonfly, the present study aims to investigate the potential aerodynamic benefits of employing tandem oscillating NACA4421 airfoils over the single NACA4421 oscillating airfoil by varying pitch amplitudes, α , phase angles, ϕ , stagger distances, S_d and gap distances, G . The present study also intends to investigate on the viability of tandem oscillating airfoils as a propulsion mode under low Reynolds number flow. Based on the limited literature on unsteady aerodynamics of tandem oscillating airfoils at post stall conditions, the present study aims to provide new physics insights on the full spectrum of critical flow events associated with post stall conditions and the effects of vortex interactions on both upstream and downstream airfoils' aerodynamic performance. The force measurements and 2-D PIV measurements were carried out in the NTU Water Tunnel at U_∞ of 0.102m/s and at a low Reynolds number of 8.6×10^3 .

In the single static airfoil case, the average C_L and C_D results were compared to Swalwell [106] and the CFD results across the pitch angle, α range tested. The present average C_L results were consistently lower than Swalwell [106] results and did not exhibit a sharp peak in C_L at the static stall angle of $\alpha_{ss} = +15^\circ$ which were attributed to the different flow conditions as Swalwell [106] was operating at a higher Reynolds number flow of 2.85×10^5 than in the present study. The 2-D CFD κ - ϵ turbulence model's average C_L results revealed similar trends and values across the α ranges tested and exhibited no distinct peak at static stall as well in relation to the present result. The present average C_D results also agreed well with both Swalwell [106] and the CFD results.

Through the investigation on tandem static airfoils at various S_d of 2 and 2.5 and at various ϕ of 0° and 180° , the present study documented the effects of stronger CCW shear layer in generating higher positive average C_L and the effects of shear layer characteristics such as profile, structure and vortical strength in influencing the 'wake-drag' and 'induced-wake' drag force production. Most of the upstream static airfoils produced lower average C_L values than the single static airfoil due to the 'strengthening effect' of its CW shear layer which decreases the overall Δp across the airfoil surfaces and thus decreasing average C_L production. In terms of down-washed vortex interactions, the interaction location, pre-existing boundary layer conditions and proximity to other shed vortices were key factors in

determining whether that particular down-washed vortex interaction with the downstream airfoil is constructive or detrimental to its lift production. Stagger distances also played an important role in determining the location and strength of the down-washed vortex interactions for the downstream static airfoil which will have an effect on the resultant nature of the interaction. One major benefits from the down-washed vortex interactions observed throughout all of these static tandem cases is that due to these interactions, the boundary layer flows over the downstream static airfoil remained attached which resulted in smaller dead-air regions leading to neutral wake structures that supports the low induction of ‘wake drag’ and ‘induced-drag’ forces respectively. Importantly, this demonstrates the constructive effect of these down-washed vortex interactions in reducing the overall average C_D for the downstream static airfoil in relation to its upstream static counterparts and the single static airfoil. In addition, as S_d increases, the effects of the down-washed vortex interactions also decreases and this is similarly documented by Broering and Lian [44, 45] in their tandem wings study on the effects of axial spacing. In terms the best aerodynamic performance, the in-phase static tandem configuration at $S_d = 2.5$ with $\alpha = +15^\circ$ produced the highest combined C_L / C_D ratio of 3.5 which is 49% higher than the single static case due to the downstream airfoil’s ability to generate the highest average C_L and to suppress its average C_D production by capitalizing on the constructive effects of the down-washed vortex interactions. Importantly, the present study have provided substantially more insights than Sharpf and Mueller [43] conclusion that the individual airfoils’ pitch angle, α is critical in influencing the resultant aerodynamic performance of the tandem configuration by documenting and rationalizing the abovementioned driving factors and effects that influence the aerodynamic performance of both upstream and downstream static airfoils.

The in-phase oscillating tandem configuration has shown its potential to increase average C_L up to 47.7% but resulted in a corresponding increase in average C_D up to 54% at $\alpha > \pm 30^\circ$ in relation to the single oscillating case. It achieved its best combined C_L / C_D ratio of 2.33 which is 35% lower than the single oscillating airfoil at $\alpha = \pm 45^\circ$ with a smaller S_d of 2. This is attributed to the in-phase oscillating downstream airfoil’s ability to generate 89% higher average C_L than the single oscillating airfoil as a result of the down-washed vortex interaction between the primary shed LEV before its first stroke reversal during the 1st half of the cycle. As the primary shed CW LEV interacted with the downstream airfoil’s suction side, it resulted in an early detachment of its primary CCW LEV which remained close to the suction surface. Due to the increase in momentum from the rapid change in α during the first stroke

reversal, this early detached CCW LEV grows up to 150% in size after the first stroke reversal. As this enlarged CCW LEV propagates over the suction side after stroke reversal, it increases positive C_L generation throughout the downstroke. The biggest drawback is that as this enlarged CCW LEV generates lift, it also generates higher positive drag forces for the downstream airfoil at $Sd = 2$ which supports the increase in average C_D up to 5 times more than the single oscillating airfoil. As a result of the C_L and C_D trade-off relationship, the combined C_L / C_D ratio for the in-phase oscillating tandem configuration decreases.

The out-of-phase tandem oscillating configuration produced the highest combined C_L / C_D ratios at larger $\alpha > \pm 30^\circ$ with the larger Sd of 2.5 due to the downstream airfoil's ability to generate large lift improvements of more than 100% and comparable drag production in relation to the single oscillating airfoil through effectively capitalizing on its 'indirect' down-washed vortex interaction effects. During its upstroke after the 1st stroke reversal, the downstream airfoil at $Sd = 2.5$ encounters a pair of CW shed vortices that hover close to its suction side in a 'side-by-side' orientation. As these shed vortices do not interact directly onto the downstream airfoil, the present study has termed these interactions as 'indirect' down-washed vortex interactions where their effects are opposite of the 'like-signed' and 'unlike-signed' direct vortex interactions. As these shed vortices hover above the suction side, they induce a 'jet-like' flow that accelerates the CCW boundary layer flow thus increasing positive C_L . This supports the generation of a high C_L peak of 12 during the upstroke which is 100% more than the C_L peaks achieved by its upstream counterparts and single oscillating airfoil. The downstream airfoil at $Sd = 2.5$ then exhibits an indication of rapid CCW LEV displacement before the 2nd stroke reversal due to the closer interaction between the hovering shed CW vortex and its CCW boundary layer over the suction side. This rapid LEV displacement corresponds to the steep reduction in C_L at $t/T = 0.66$ which also gives rise to a less aggressive full stall after the 2nd stroke reversal for the downstream airfoil at $Sd = 2.5$ as compared to its downstream counterpart at $Sd = 2$. Thus, enabling the downstream airfoil at $Sd = 2.5$ to produce consistently lower positive C_D values throughout the 2nd half of downstroke which primarily reduces its average C_D production. This demonstrates the downstream airfoil's ability effectively capitalize on the constructive effects of its 'indirect' down-washed vortex interactions to enhance C_L and suppress C_D production which makes the out-of-phase oscillating tandem configuration at $Sd = 2.5$ the most ideal oscillating tandem configuration in the present study, potentially viable for hovering flight.

In summary, through the investigation on the oscillating tandem configuration, the present study have demonstrated that the variations in S_d and ϕ will affect the timing and location of the down-washed vortex interactions which consequentially affect the boundary flow structures that will influence the resultant aerodynamic performance of the downstream airfoil. Even though the present study did not present the vorticity contours at various gap distances, G , the average force coefficients results were presented. With these above mentioned conclusions, there is a high possibility that varying G will exhibit the same factors that influence the nature of the down-washed vortex interactions on the downstream airfoil. Moreover, the present study is able to provide a quantitative rationalization on the effects of wing-wing vortex interactions through the formulation of the Vortex Shedding Model presented in Section 5.7 which is a novel contribution to the understanding of vortex interactions under low Reynolds number flow. The present study not only further the understanding of vortex interactions but also provide new insights in pure pitching airfoils and validating Koochesfahani's claim on pure pitching airfoil not being ideal for thrust generation [42]. Despite in-phase tandem oscillating airfoils not proving to a better propulsive option when compared to the single oscillating airfoil, the out-of-phase tandem oscillating airfoils at post stall conditions have proven to be viable for hovering flight by effectively capitalizing on the constructive effects of its 'indirect' down-washed vortex interactions.

7. FUTURE WORKS

The present study primarily investigated the unsteady aerodynamics of tandem airfoils both in oscillating and static modes in two-dimensions where the airfoils are assumed to be infinitely long without taking account of any span-wise flow features. Inspired by recent computational 3-D studies such as by Hefler et al. [117] and Chen et al. [118], the present study could further investigate the span-wise unsteady flow mechanisms that could potentially unravel new vortex interaction effects associated with oscillating tandem airfoils by adopting new experimental techniques involving 3-D PIV measurements and validating the experimental results with 3-D CFD studies similar to recent literature.

Another potential improvement that the present study could adopt would be to carry out the force measurements and PIV measurements in synchronization rather than having to process and correlate the force and PIV datasets which takes a lot of time and resources. In addition, improvements can also be made to oscillating mechanisms for the tandem airfoils. The current oscillating mechanism is primarily design to accommodate only two phase angles where it utilizes a pair of belts and a gear system driven by a single servomotor for the in-phase oscillating cases and out-of-phase oscillating cases respectively. The problem with the current oscillating mechanism would be that the system is not versatile to test out more phase angles and the belt and gear driving system is not particularly great in terms of stability which induces noise during oscillations. As explained in Chapter 4, the noise induced by the oscillating mechanisms have been accounted for and filtered through the force measurement processing methods presented. In order to further streamline the data processing phase and to improve the stability of the oscillating motion, future experiments can incorporate the use of dual-synchronized servomotors for each tandem airfoil that allows more freedom to configure both phase angles and pitch angles accurately thus minimizing input variability. One key improvement to the present study would be to adopt a different PIV measurement setup that allows capturing both tandem airfoils in one flow field rather than having to process the flow field for individual airfoils and having to stitch the vorticity contours together. Improvements such as using a camera lens with a smaller focal length and with better resolution could also be implemented.

Another future work stream proposed could be a collaborative work with data computational scientists to be predict certain outcomes of vortex interactions and to validate the present

study's findings on the effects of 'direct' and 'indirect' vortex interactions on aerodynamic performance of the downstream airfoil. Due to the recent rampant emergence of data analytics in the world today, we should take this opportunity to leverage on the use of powerful computational and analytical tools such as machine learning capabilities or artificial intelligence (AI) to further the understanding of unsteady aerodynamics under low Reynolds number flows. With the knowledge acquired through the present study, a collaborative work with research institutes could also be initiated to develop a prototype micro-aerial vehicle (MAV) or an underwater-unmanned vehicle (UUV) that incorporates tandem oscillating airfoils to prove the real viability of tandem oscillating airfoils used as a propulsive system.

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9. APPENDICES

APPENDIX A - PIV SUPERIMPOSITION METHOD

The PIV superimposition method works by substituting the missing data in the ‘shadow region’ with the actual raw numerical data. In order for this method to work, it will require to carrying out PIV measurements on the airfoils in two orientations; normal (with the suction side facing the incidence laser sheet) and opposite orientation. Through identifying, extracting and manipulating the relevant data points, the PIV superimposition method aims to produce complete and seamless velocity vector maps and vorticity contours.

Grid Location		Pixel Velocities		Real Velocities		Vector Length
x	y	U_{pix}	V_{pix}	U	V	Length
0	0	5.492934	0.021669	0.120979	0.000477	0.120980116
0	1	5.555791	0.011076	0.122364	0.000244	0.122363819
0	2	5.640978	0.011283	0.12424	0.000248	0.124240012

Table 9-1 : Sample of PIV Numerical Data

The PIV velocity vector plots consist of grids or pixels, 50 x 37.5 grids (without any overlap) in total with each grid representing a velocity vector. Table 9-1 shows a numerical data sample from a velocity vector plot. The numerical data for this velocity vector map is stored in a ‘.csv’ file. Each row of data is arranged according to its x and y spatial coordinates with respect to the origin which is defined at the bottom left corner of the map. The U_{pix} and V_{pix} columns denote the velocities of the vector in x-direction and y-direction respectively in pixel-scale magnitude. The real velocities, U and V can be obtained by dividing U_{pix} and V_{pix} by 45.4 which derived from the scale factor, S_f and the length of the vectors can be derived from these velocities using Pythagoras Theorem. The principle of superimposing these velocity vector plots is based on the substitution of the region covered by the shadow or the ‘shadow region’ with the vectors from the ‘opposite’ airfoil that covers the same area as the ‘shadow region; this region is called the ‘substitution region’. These two velocity vector plots must be opposite to one another with respect to the angle of incidence of the airfoils. The steps for this PIV superimposition method are summarized as follows.

Figure 9-1 : (a) 'Shadow Region' for the 'Normal' Airfoil; (b) 'Substitution Region' for the 'Opposite' Airfoil

1. Mark the 'shadow region' for the 'Normal' airfoil and the 'substitution region' for the 'Opposite' airfoil on their respective velocity vector maps as illustrated in Figure 9-1.

Figure 9-2 : (49 X 36) Grid Sheet for Velocity Vector Plot

2. Create a grid sheet measuring 49 by 36 grids and overlay it on the marked velocity vector maps as shown in Figure 9-2. Identify the grid coordinates that both the 'shadow region' and the 'substitution region' cover for masking purposes.

A	B	G	H	I	J	K
x	y	U pix	V pix	U	V	Length
14	20	0	0	0	0	0
14	21	0	0	0	0	0

Table 9-2 : Nullified Numerical Data for Masked Region

3. The numerical data for the 'Normal' airfoil for this airfoil masked region will be nullified as highlighted in Yellow for Columns G to K in Table 9-2.

(a)

Step 5(a) : From 'Substitution Region'

A	B	G	H	I	J	K
x	y	U pix	V pix	U	V	Length
14	1	5.614876	0.48534	0.123665	0.010689	0.124126
14	2	5.635942	0.488503	0.124129	0.010759	0.124594
14	3	5.655432	0.47382	0.124558	0.010436	0.124994
14	4	5.666351	0.465042	0.124799	0.010242	0.125218
14	5	5.651215	0.413949	0.124465	0.009117	0.124799

(b)

INVERTED

Step 5(b) : 'Transposed Shadow Region' Data

A	B	G	H	I	J	K	Transposed Coordinates		Sorting Order
x	y	U pix	V pix	U	V	Length	x	y	
14	1	5.614876	-0.48534	0.123665	-0.01069	0.124126	12	35	1
14	2	5.635942	-0.4885	0.124129	-0.01076	0.124594	12	34	2
14	3	5.655432	-0.47382	0.124558	-0.01044	0.124994	12	33	3
14	4	5.666351	-0.46504	0.124799	-0.01024	0.125218	12	32	4
14	5	5.651215	-0.41395	0.124465	-0.00912	0.124799	12	31	5

SORTED

(c)

Step 5 (c) : 'Finalized Shadow Region' Data

A	B	G	H	I	J	K	Sorted Transposed Coordinates		Sorting Order
x	y	U pix	V pix	U	V	Length	x	y	
14	5	5.651215	-0.41395	0.124465	-0.00912	0.124799	12	31	5
14	4	5.666351	-0.46504	0.124799	-0.01024	0.125218	12	32	4
14	3	5.655432	-0.47382	0.124558	-0.01044	0.124994	12	33	3
14	2	5.635942	-0.4885	0.124129	-0.01076	0.124594	12	34	2
14	1	5.614876	-0.48534	0.123665	-0.01069	0.124126	12	35	1

Table 9-3 : Data Manipulation Steps; (a) Step 5(a); (b) Step 5(b) and (c) Step 5(c)

4. Extract all of the numerical data for the ‘Substitution Region’ from the ‘Opposite’ airfoil and import the data onto a separate worksheet for data manipulation to facilitate the superimposition to the ‘Shadow Region’. The data manipulation steps are as follows:
 - (a) Invert Columns H and J values as highlighted in red in Table 9-3 (a) by multiplying each cell with a negative, (-) due to the opposite directionality of the vectors in comparison to the ‘Normal’ airfoil data as indicated in Table 9-3 (b).
 - (b) The ‘*Substitution Region*’ numerical data will then be correlated to the ‘*Shadow Region*’ by transposition of the coordinates using mathematical formulation. These transposed coordinates shown in Table 9-3 (b) originated from the ‘shadow region’ coordinates that were identified earlier in Step 2.
 - (c) The ‘*Transposed Shadow Region*’ data now is then sorted in ascending order via a ‘Sorting Order’ column; indicated as the ‘*Finalized Shadow Region*’ data. This ‘Finalized Shadow Region’ is essentially the correlated data from the ‘substitution region’ and is sorted to produce the same order for ease of data import in the next steps.
 - (d) Column G, H, I, J and K in the ‘*Finalized Shadow Region*’ data will then be imported according to its ‘Sorted Transposed Coordinates’ shown in Table 9-3 (c). This step will superimpose the ‘Substitution Region’ onto the ‘Shadow Region’.
 - (e) For the final processing step, the finalized edited numerical data for the ‘Normal’ airfoil or the ‘Finalized Numerical Data’ will be processed via MatLab software to generate the superimposed Velocity Vector and Vorticity contours as shown in Figure 9-3.

Figure 9-3 : Example of Super Imposed Velocity Vector and Vorticity contours for Single Static NACA4421 airfoil at $\alpha = +45^\circ$, $t = 0.078s$

Figure 9-3 also illustrates the progression of the PIV superimposition method where in Figure 9-3 (a), the ‘shadow region’ is evidently observed on the ‘normal’ airfoil plots due to the initial PIV experimental arrangement. Subsequently, the PIV measurement of the ‘opposite’ airfoil in Figure 9-3 (b) provided the ‘substitution region’ to be manipulated and transposed to fill up the ‘shadow region’. Figure 9-3 (c) shows the successful substitution of the ‘shadow region’ to produce a complete and seamless velocity vector and vorticity contours. This superimposition method can be effectively applied to both static and oscillating cases with reasonable results. However, the process is too tedious and time-consuming to be efficiently incorporated to the immense number of experimental cases for the present study. Hence, the development of more innovative and reliable experimental methods was much needed.

APPENDIX B – TANDEM AIRFOILS VORTICITY CONTOURS STITCHING PROCESS

Step 1: Airfoil Geometry Superimposition Process

Continues to Step (f)

Figure 9-4 : Airfoil Geometry Superimposition Process

With reference to Figure 9-4, Points 1 to 6 below will provide the rationalization of the airfoil geometry scaling in Steps (c) and (d) and also the consequential positional misalignment correction in Step (e). All of which are imperative in order to produce accurate superimposition and scaling of the airfoil geometries for the velocity vector and vorticity contours.

1. The velocity vector and vorticity contours were processed using a MatLab program prior to carrying out this airfoil geometry superimposition process. However, it was discovered that these processed plots are generated with a slightly different scaling factor when compared to the camera image. A quick verification on this point would be to have both the camera image and the processed plots sized the same and one can clearly see that the camera image airfoil geometry cannot be superimposed onto these processed plots directly without the need to scale and position the airfoil geometry correctly.

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2. One important consideration before scaling the airfoil geometry for these processed plots would be maintaining the same profile ratio as the airfoil geometry from the camera image. This profile ratio is defined as the ratio between the airfoil maximum thickness and the chord. For the NACA4421 airfoil, the maximum cross-sectional thickness is defined as 21% of the chord [104]. This point is clearly illustrated in Figures 9-4 (c).
 3. In order to find the correct airfoil geometry dimensions for the velocity and vorticity plots, the newly scaled airfoil chord has to be determined first. Through the analysis of the vortical structures on the vorticity contours and velocity vector plots, the airfoil geometry's chord, c_2 on these plots are approximated to be 91% of the camera image airfoil geometry chord, c_1 and its corresponding thickness, t_2 can be easily computed, ensuring a consistent profile ratio of 21% as shown in Figure 9-4 (d).
 4. Another limitation of the MatLab processed plots is that with these scaled down airfoil geometries, the alignment lines drawn on the camera image in Step (c) cannot be applied to the processed plots even if these plots are exactly the same size. Hence, through the rigorous work to correlate the processed plots and camera images, it was determined that there is a consistent positional misalignment of the airfoil geometries between the processed plots and corresponding camera image.
 5. In order to deal with this positional misalignment, two lines are drawn with an offset of $+0.07c_1$ and $+0.02c_1$ from the x-direction and y-direction alignment lines respectively as illustrated by the red lines. These offset alignment lines are termed as the 'Plot Alignment Lines' which are critical to ensure the correct position of the scaled down airfoil geometries superimposed onto the processed plots. For example, if the measured camera image airfoil geometry chord, c_1 is 100mm, then the 'Plot Alignment Lines' (red lines) are drawn with an offset of +7 mm and +2 mm from the horizontal and vertical alignment lines (in blue) respectively as shown in Figure 9-4 (e).
 6. Once the scaled down airfoil geometry and the 'Plot Alignment Lines' are attained, Steps (f) and (g) are carried out to accurately superimpose the correct scaled airfoil geometry at the corrected position.

This process has been utilized for all the velocity vector and vorticity contours in the present study and has been demonstrated to produce repeatable results with an approximate 5% random positional error due to the manual positioning of the airfoil geometries and also a measurement uncertainty of $\pm 1^\circ$ in the recorded airfoil α in Step (b) in Figure 9-4 due to the measurement resolution of the “rotation” formatting function in Microsoft PowerPoint. However, despite these limitations, the present study has taken the necessary control measures to minimize scaling and positional errors to ensure a reasonably high degree of accuracy and precision in superimposing the airfoil geometries.

Step 2: Instantaneous Vorticity Contours Selection

After the airfoil geometry superimposition process, the next step in this vorticity contours stitching process is the instantaneous vorticity contours selection. In this step, the vorticity contours will be carefully selected where it function as snapshots that will effectively cover the entire cycle thus capturing critical points such as before and after stroke reversals. Importantly, these vorticity contours for tandem cases are stitched together and correlated to the instantaneous force measurements plots presented earlier.

The biggest challenge for this selection process is to deal with the ‘randomness’ of the PIV measurements. As presented earlier, these PIV measurements are taken at trigger rate of 5Hz which meant that the images are captured at a time interval of 0.2s. The ‘randomness’ stemmed from the fact that the triggering of the PIV measurements are not phased-locked in relation to the oscillation cycle due to the constraints in acquiring equipments such as synchronizers and functional generators. For brevity, the present study will process the stitching of vorticity contours for experimental cases at $\alpha = \pm 15^\circ$ and $\pm 45^\circ$.

Pitch Amplitude, $\alpha(^\circ)$	Rotational Speed ($^\circ/\text{s}$)	Cycle Period (s)	No. of Images Captured per Cycle	No. of Images Captured in 10 Cycles	No. of Snapshots Selected for 1 Cycle
± 15	88.24	0.68	3.4	34	7
± 45	93.75	1.92	9.6	96	10

Table 9-4 : Experimental Parameters for the Selected Oscillating Cases

As depicted in Table 9-4, for the two oscillating cases at $\alpha = \pm 15^\circ$ and $\pm 45^\circ$ selected, the number of images captured per cycle is computed by dividing the cyclic period, T with the PIV measurement time interval of 0.2s. The problem lies with small α in the case of $\alpha = \pm 15^\circ$, only 3.4 images were captured in one cycle which is insufficient to comprehensively study the flow dynamics of one oscillating cycle. Hence, the PIV measurements are taken across 10 oscillation cycles to increase the number of images captured as shown in Table 9-4. The total number of instantaneous vorticity contours is then defined to ensure a comprehensive coverage for one oscillation cycle.

Figure 9-5 : PIV Measurement Snapshots over 10 cycles at $\alpha = \pm 30^\circ$

As mentioned earlier in this section, the biggest challenge is the ‘randomness’ of the PIV measurements and this point can be clearly demonstrated in Figure 9-5 for an oscillating case at $\alpha = \pm 30^\circ$. Due to the trigger rate of 5Hz, these PIV snapshots are taken at instantaneous time frames that correspond to α that vary from cycle to cycle. The total number of measurements taken per cycle is also not consistent from cycle to cycle. Based on the first cycle, the total number of snapshots is 6 whereas the second cycle has a total number of snapshots of 7, thus illustrating the ‘randomness’ in the PIV measurements. However, by taking these snapshots across 10 cycles, it provides a substantial ‘pool’ of vorticity contours of various α at various points along the cycles. As the number of selected measurement snapshots for one cycle defined in Table 9-4, the next step is to select the instantaneous points in one cycle where the measurement snapshots were taken at. The selection of these points is based on three criteria; (1) the points taken must be available in the ‘pool’ of vorticity plots for that particular case, (2) there must be an equal number of points selected for both the upstroke and downstroke and (3) the time intervals between the selected points must not be too close to each other.

Figure 9-6 : Instantaneous Vorticity Contours Selection for Oscillating Cases at $\alpha = \pm 15^\circ$; (a) In-Phase Cases and (b) Out-of-Phase Cases

Figure 9-7 : Instantaneous Vorticity Contours Selection for Oscillating Cases at $\alpha = \pm 45^\circ$; (a) In-Phase Cases and (b) Out-of-Phase Cases

The pitch amplitudes for these selected instantaneous points, t/T are computed and the corresponding processed vorticity contours are then extracted from the ‘pool’ of processed plots (post-airfoil geometry superimposition process in Step 1). These extracted vorticity plots are used for the subsequent stitching process for the tandem cases which will be explained in the next section. Essentially, the fundamental aim for this vorticity contour plot selection process is to formulate a “phase-locking” mechanism for the present study in order to produce standardized set of vorticity contours for analysis and the subsequent image stitching process. The selected instantaneous points, t/T for one cycle for the oscillating in-phase and out-of-phase cases at $\alpha = \pm 15^\circ$ and $\pm 45^\circ$ are presented in Figures 9-6 and 9-7.

Step 3: Vorticity Contours Stitching Process

1. Position the selected processed vorticity contour plots for the upstream and downstream airfoils and ensure that the superimposed airfoil geometries have relatively identical pitch amplitudes.
2. Identify any common vortical patterns that can be found on both vorticity plots (for e.g the CCW LEV shed vortex circled in blue).
3. Draw the Alignment Lines (red dotted lines) for the tandem configuration which take reference from the ‘Origin’ point. This ‘Origin’ reference point is taken from the pitching axis point of the upstream airfoil denoted as the white ‘X’.
Note: For this case where $G = 0$, there is no need to draw another alignment line in the X-direction.
4. Adjust the spacing between the vertical alignment lines to $Sd = 2.0$ which is twice the chord. Use the airfoil geometry to represent one chord.
5. After the vertical alignment lines adjustment, do take note that the intersection of the horizontal and the adjusted vertical line would be the new position of the pitching axis of the downstream airfoil. This new pitching axis location for the downstream airfoil is depicted as the red ‘X’.

1. Adjust the position of the downstream airfoil to be aligned with the red 'X' which is the new downstream airfoil's pitching axis. This step will essentially eliminate any misalignment between the tandem airfoils and ensuring that the stagger distance in this case is correctly represented in the stitched vorticity plots.
2. As the downstream vorticity plot is repositioned, there will be an overlapping region where the matching of the common vortical structures is important. Hence, this reiterates the importance of selecting the ideal vorticity contour plots pairing prior to this stitching process.
3. Due to limitations of the PIV measurements, these vorticity plots were taken at different physical settings as the optical components and CCD camera were re-positioned and re-calibrated. The projected laser sheet was also re-aligned for every setup. Hence, these tasks will induce some degree of misalignment of the vortical structures from the two airfoils' captured flow field.

1. Crop the perimeter of the stitched vorticity plots.
2. Delete the alignment lines and change the airfoil geometries color to black.
3. All these editing steps will produce an accurately scaled stitched vorticity contour plots for the tandem cases and with the airfoils accurately spaced depending on their tandem configuration.
4. As these plots are selected based on the 'vorticity contour plots selection process' previously explained, these stitched plots can be correlated to the selected instantaneous times, t/T thus enabling the present study to correlate these plots to the instantaneous force measurements.

1. One limitation with the stitching process would be that for tandem cases at $S_d = 2.5$, there is a region in between the tandem airfoils that are not captured by the CCD camera. This region can be termed as the 'void region'.
2. Through applying the Steps (a) to (d) of this stitching process, this 'void region' measures approximately $0.39c$ in length as shown in Step (e).
3. This is an important consideration for the present study as for tandem cases at $S_d = 2.5$, flow structures within this 'void region' cannot be visualized and analyzed. However, these stitched plots are definitely more effective in visualizing vortex interactions between the tandem airfoils as compared to individual vorticity contour plots.

Figure 9-8 : Vorticity contours Stitching Process Steps and Descriptions for Tandem Airfoils Cases

In Figure 9-8, the process steps and its descriptions for the vorticity contour stitching process are presented. In Figures 9-8 (a) to (d), the process is demonstrated on a tandem case at $S_d = 2$ and highlighted the limitations of the process whereby due to the inherent differences in the physical conditions of capturing two independent PIV images, there is a certain degree of misalignment in the overlapping vortical structures. Another limitation is that for tandem cases at $S_d = 2.5$, there is a 'void region' between the airfoils due to inadequate CCD camera flow field coverage that measures approximately $0.39c$ in axial length as shown in Figure 9-8 (e). However this stitching process is sufficiently robust as it can be applied to the various gap distances as well to produce accurately scaled combined vorticity contours. Importantly, these stitched plots can be correlated to the instantaneous force measurements to provide the in-depth analyses presented in Chapter 5.