Design and development of energy-efficient mechanism for flapping-wing micro air vehicle

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**NANYANG
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**DESIGN AND DEVELOPMENT OF ENERGY-
EFFICIENT MECHANISM FOR FLAPPING-WING
MICRO AIR VEHICLE**

CHIN YAO WEI

**SCHOOL OF MECHANICAL AND AEROSPACE
ENGINEERING**

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**DESIGN AND DEVELOPMENT OF ENERGY-
EFFICIENT MECHANISM FOR FLAPPING-WING
MICRO AIR VEHICLE**

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School of Mechanical & Aerospace Engineering

A thesis submitted to the Nanyang Technological University
in partial fulfillment of the requirement for the degree of
Doctor of 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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I have reviewed the content and presentation style of this thesis and declare it is free of plagiarism and of sufficient grammatical clarity to be examined. To the best of my knowledge, the research and writing are those of the candidate except as acknowledged in the Author Attribution Statement. I confirm that the investigations were conducted in accord with the ethics policies and integrity standards of Nanyang Technological University and that the research data are presented honestly and without prejudice.

13 February 2019

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A handwritten signature in purple ink, reading "Lau Gih Keong", written over a dotted line.

Asst. Prof Lau Gih Keong

AUTHORSHIP ATTRIBUTION STATEMENT

This thesis contains material from 2 papers published in the following peer-reviewed journals where I was the first and/or corresponding author.

Chapter 6 is partially published as Gih-Keong Lau, Yao-Wei Chin, Joel T. W. Goh and R.J. Wood, "Dipteran-Insect-Inspired Thoracic Mechanism With Nonlinear Stiffness to Save Inertial Power of Flapping-Wing Flight," in IEEE Transactions on Robotics, vol. 30, no. 5, pp. 1187-1197, Oct. 2014.

The contributions of the co-authors are as follows:

- Prof Lau provided the initial project direction and edited manuscript.
- I prepared the manuscript drafts. The manuscript was revised by Prof. Wood.
- I co.-designed the study with Asst. Prof Lau and performed all the laboratory work at the School of Mechanical and Aerospace Engineering. I also analyzed the data.
- Joel assisted in contributing ideas to the design, fabrication, testing of some of the prototypes and data analysis.

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- I co-designed the study with Asst. Prof Lau and performed all the laboratory work at the School of Mechanical and Aerospace Engineering. I also analyzed the data.
- Yukai and Ziyuan assisted in the design, fabrication, testing of the prototypes and data analysis.

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ABSTRACT

Few micro air vehicles hover by flapping wings. Flapping wing flight is multi-modal and can generate high lift more aerodynamically efficient than fixed-wing flight at low Reynolds number because of unsteady aerodynamic lift enhancement such as clap and fling. However, a flapping wing micro air vehicle (FWMAV) is subjected to higher inertial power needed to accelerate and decelerate the wings and higher friction loss in its transmission. Small Dipteran insects can recover the loss of wing kinetic energy by elastic storage with their deformable flight thorax, while larger flyers make use of lightweight but flexible wings to minimize inertia. Inspired by these natural flyers, we developed energy-storing compliant wing hinges and efficient transmissions for FWMAV with lightweight flexible wings. To aid our design, we also developed novel analytical models to predict passive wing rotation, lift and energetic costs for the flapping of a chordwise-flexible wing with a linear wing twist.

We showed that elastic wing hinges can help to save inertial power and/or accelerate wings to higher speed at both the insect and bird scale, through three compliant mechanisms we had developed. Firstly, a thoracic mechanism with non-linearly increasing stiffness can save 31% energetic cost of a 100mm-span flapping wing, less than that incurred to the one without elastic storage. Secondly, a bistable thoracic mechanism inspired by Dipteran insect's click mechanism. This bistable mechanism snaps wings to either Λ or V-shape stable wing positions, which produces different wing dynamics from the conventional spring. It could save up to 49% power compared to a flapper without bistable mechanism.

Lastly, we scale up the elastic mechanism for a bird-sized 280-mm span FWMAV, which claps and flings four crisscrossing (X) wings like a scissor. The wing is constructed ultralight to reduce wing inertia. The X-wing is driven by a double crank-rocker mechanism, where each rocker is pivoted by a pair of elastic nylon hinges. The recoil of the hinge helps to accelerate the wing clapping and enhance thrust generation without incurring extra power. A gearbox with optimized speed reduction ratio helps to match motor loading at peak efficiency. Friction in the gearbox is minimized with bearings supporting the gear shafts. As a result, our nylon-hinge X-wing generates a maximum thrust of 39.9g at efficiency as high as 7.2 g/W at 15.4Hz, which is 49% higher than that of a propeller driven by the same motor (41 g maximum thrust, 4.81 g/W). This X-wing demonstrates stable indoor hovering for at least 7mins, with a total body mass of 26.24 g including 5.29-g 200-mAh 3.7V LiPo battery. It can also perform aerobatic flight such as fly forward fast and flip to fly backward, dive backward and recover to hover, descent and land.

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Chapter 1 Introduction

Past decades of research and development has witnessed the scaling up of man-made aircraft to carry more payload. Nowadays, fixed-wing airplanes and helicopters can weigh several thousand times more than natural flyers. For example, an Airbus A380 weighs 369 tons, a Sikorsky S-92 helicopter weighs 7030 kg. In comparison, the largest flying bird, Andean condor with a 3.2m wingspan weighs 15kg while a hefty home pigeon can weigh close to 0.4kg. In the opposite direction of scaling, micro aircrafts have been developed with the aim for surveillance mission carrying a little payload at a low travel speed near populated areas, which prohibit a normal-size drone. However, the development of micro aircrafts encounters many challenges in miniaturization, primarily due to weight limitation. So far, the micro aircrafts developed are still no rival to natural flyers in terms of flight endurance and robust maneuver.

Natural flyers can fly in many modes. For example, hummingbirds and fruit bats are capable of hover and fly forwards and backwards; swallow can make fast swoop and a quick turn. Pigeons are capable of vertical takeoff, fly forwards and glide with robust maneuver to avoid collision in between cluttered urban buildings; however, it cannot sustain hovering for long due to high energetic cost for hovering flight. In comparison, small flying insects can hover and fly forwards at low speed. Surprisingly, insect flight shows enhanced lift coefficient (2~3) at a low Reynold number, which range from 10 to 23,000 [1]. The origins of such aerodynamic lift enhancement at insect scale include: wake capture [2], rotational lift [3], leading edge vortex [4], clap-and-fling mechanism [1, 5]. Smaller natural flyers gain an edge in hovering over larger natural flyers because of improved aerodynamic efficiency (in terms of thrust per unit aerodynamic power) with scaling down. However, there is optimum sizing for natural flyers to strike a balance between aerodynamic efficiency and muscles capacity to sustain the need for hover and other modes of flight.

Flight apparatus of natural flyers consist of wings, flight muscles, bird tails, and other auxiliary components. Flapping wings are pivoted wing planes or levers that reciprocate under the direct or indirect drive by flight muscles. Flapping wings of natural flyers are responsible for aerodynamic force generation while insects' wings can even act as the control surface flight maneuver. Flapping wings are pivoted wings of high leverage and they present high load to the flight muscles. Acceleration/deceleration of wing reciprocation imposes an extra inertial load. Interestingly, natural flyers' flight muscles are energetic to drive and power various modes of flight. Flight muscles play a few roles: being an actuator, a battery and even a spring for elastic storage. Elastic storage in flight thorax or muscles is believed to help recover the wings' inertial power.

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Micro air vehicles were developed with the aim to fly as agilely as natural flyers. Common micro air vehicles are: micro airplanes, micro helicopters, miniaturized quadcopter. A new breed of micro air vehicles is bioinspired and powered by flapping wings. According to the definition by United States' Defense Advanced Research Projects Agency (DARPA) [6], a micro air vehicle or a micro aircraft weighs less than 70g (including a 20g payload); it should have a size less than 15cm. For mission, it is expected to fly for 20 to 60 minutes and travels up to 10km. Along with the development of MAV is an even smaller Nano Air Vehicles (NAV), which was inspired by insects to weigh less than 10g and have a wing span of 75-100mm; it is expected to fly for 20 minutes [7].

Among these MAV, a micro airplane can fly forwards fast for outdoor. Indoor flight restricts the travel speed of MAV to be slow to avoid collision; it is only possible with ultralight micro planes or micro helicopters. A micro helicopter or quadcopter excel in hovering at a low travel speed, generating enough lift by high-speed spinning propeller. However, high-speed rotating blades of helicopter or quadcopter can create loud noise and poses risk of accidental cut to human pedestrian upon collision. These MAVs are however limited in flight time (due to little payload) and they can only fly monotonic modes, either hovering or forward flight. None of them can fly as agilely as natural flyers.

Inspired by natural flyers, various flapping-wing MAVs or ornithopter were developed over the past decades. However, only a few can hover. They vary in scale, from a bee-size RoboBee of 60 mg, Nano-hummingbird of 19 g, to pigeon-size Mentor of 400-580 g. Only small bioinspired MAV can hover free, for example, DelFly, Nano Hummingbird, FlowerFly and so on, but not for long. Tiny RoboBee can hover with wired external power but not being able to carry on-board battery for a free flight. Bigger Mentor did hover untethered for 20s; the short flight time is caused by battery overheating and motor-overload. It is not clear what the optimum sizing is for a bioinspired MAV to hover long and perform other modes of flight as agilely as natural flyers do.

Invention of flight muscles is a holy grail of research to ultimately power bioinspired MAV. However, in the foreseen future, 'flight muscles' based on electro-active polymers will not be ready so soon. In practice, a brushless motor, which directly drive a propeller, was used for indirect drive the indirect drive of flapping wings. Existing wing-transmissions, which consists of a crank-rocker mechanism and speed-reduction gear trains, for a small bioinspired are not mechanically efficient. According to Professor Adrian Thomas [8], a zoologist from Oxford University and Animal Dynamics Ltd, the power loss by friction and wing inertial in small bioinspired MAV can be more than the aerodynamic power expended by flapping wings. High leverage by wings can induced a high load of fifty times the wing weight in their design of dragonfly-inspired MAV. This leads to high friction in the gearbox and wing-transmission

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which introduces extra weight more than a simple propeller. So far, indirect drive of flapping wings still cannot produce as much full-throttle thrust as direct drive of a recommended propeller does when both are powered by the same motor.

As inspired by small flying insects, elastic storage was theoretically proven to help reduce the torque requirement by flapping wings, even for bird-size ornithopters of a-few-hundred-millimeters wing span. A simple means of elastic storage can be realized to a motorized wing transmission by adding a spring as the pivotal support to a flapping wing. Ideally to fully recover the wing's kinetic energy, the spring stiffness needs to be tuned to have the 'resonant' frequency of elastically hinged wing matched with the operating wing beat frequency. Yet, in practice, such a tuned spring is stiff and presents a high elastic load, which induces extra friction to the wing transmission. Unfortunately, full elastic storage by the stiff spring may risk overwhelming a driving motor. Partial elastic storage could be a compromise to help existing flying bioinspired MAV. Yet, the effect of elastic storage on electrical power saving and wing's kinematics were clearly understood.

Figure 1.1 A dragonfly-inspired flapping wing micro drone Skeeter by Animal Dynamics.

1.1 Objectives

This thesis will present a number of bioinspired elastic storage to reduce the energetic cost incurred to electric motor for indirect drive of flapping wings, which span from wing size of a hawkmoth to that of a bat. The designs of elastic storage include: 1) a Dipteran-insect inspired thoracic shell with nonlinear stiffness; 2) a Dipteran-insect inspired click mechanism with bi-stable elastic behavior; and 3) a simple flexural spring. Despite of being inspired by insects, the thoracic shell and click mechanism will be scaled up to recover kinetic energy of flapping wings of 100mm span (hawkmoth size). We shall show the use of Dipteran-insect inspired elastic storage contributing to power saving for wing beating up to 30Hz. In addition, we study the effect of nonlinear hinge stiffness on the wing kinematic under indirect drive by a planetary geared motor of 1-2g.

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Next, we investigate a simple option of elastic storage for four-wing ornithopter which can hover freely. At a small bird size, four wings of the ornithopter has a wider wing span of up to 280mm; they presents a much higher inertial and aerodynamic load even when beating at a lower frequency of up to 15Hz. Here, the elastic storage developed includes polyimide film hinge and a Nylon flexure. To drive such stiff flexures, a speed-reduction gear train with ball bearing will be designed for a motorized double crank-rocker mechanism. This combination of stiff spring and low-loss transmission design helps reduce the flapping-wing induced apparent loads acting on the driving motor. This study shall reveal that four wings of 280mm span with partial elastic storage can at best produce as much full-throttle thrust and twice electrically efficient as compared to a motor-manufacturer recommended propeller on the same basis of 3.1g-motor drive at 4.2V supply. We shall show such four-wing ornithopter with greatly enhanced thrust generation being capable of aerobatics and double flight endurance, unattainable by the other MAVs.

1.2 Outline

Chapter 2 reviews natural flyers and current FWMAVs capable of hovering. The scale effects are examined to gain insights into the parameters of lift generation and energetic costs of hovering flapping flight. Performance of some micro-rotor-propellers is also included for comparison. The quasi-steady analysis of unsteady aerodynamics is reviewed together with unsteady aerodynamic mechanisms complement to the analysis.

Chapter 3 discusses the development of a simple analytical model based on quasi-steady aerodynamic supplemented with unsteady aerodynamic mechanisms for lift generation and the energetic costs of flapping flight. This analytical model is useful to guide designs that will generate sufficient thrust for hovering. A novel model for passive wing rotation is also presented. A case study is presented based on experimental results in Chapter 9.

Chapter 4 presents the conceptual design solution towards efficient flapping flight. We discuss conceptual designs of motor-transmissions and elastic flapping mechanism that minimize losses. Three different insect-inspired spring designs are proposed, of which first two are multi-spring thoracic mechanism for 100-140mm-span large-insect-sized two-wing flapper, and the third for 240mm-span and 280mm-span four-wing clap-and-fling flyer.

Chapter 5 presents the experimental setup built to characterize the flight performance of the different flapping mechanism. Details of the setup are given and are referred to in later chapters.

Chapter 6 introduces an insect-inspired non-linear stiffness thoracic mechanism for two-wing flapper of 100mm-span. The thoracic mechanism consists of three leaf-spring hinges in

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each symmetric half. This spring mechanism recoils wings to the midstroke like normal spring. However, its recoil stiffness starts very low at midstroke but increases drastically towards either ends of the wing stroke. It demonstrates the benefit of elastic recovery of inertial power in thrust generation.

Chapter 7 presents an insect-inspired bistable thoracic mechanism which is known as the click mechanism. The click mechanism tends to snap wings to either extreme of the stroke instead of midstroke. The elastic recoil accelerates the wing to higher peak speed in addition to what the motor torque normally does. As such the motor can sweep large wing stroke to a higher wingbeat frequency. The bistable mechanism is good to save power for large-wing stroke but low-frequency flapping wing mechanism. It is however difficult to start.

Chapter 8 shows the upscaling of elastic flapping mechanism for 240mm-span X-wing with single spring design. The spring is designed to induce a faster clap and a slower fling because its neutral position is shifted towards the end of clap stroke. The effect of increased stiffness is studied.

Finally, Chapter 9 leads to the development of the first hovering X-wing with elastic mechanism with conventional airplane control surfaces. The X-wing claps and flings two crisscross wing planes to produce high thrust to achieve hover and is more stable compared to two-wings flapper because the counter-rotating wings eliminate drag-induced body rocking. Each of the wing planes are supported by stiff elastic Nylon hinges which are tough and durable. The X-wing can hover for at least 7mins with a mass of 26g and 200mAh Li-Po battery capacity. This X-wing generates maximum 39.4g of thrust with a better thrust-power ratio of 6.1 g/W compared to the maximum 41 g thrust at 4.8g/W achieved by 3020 propeller driven by the same motor. An aerobatic flight with this nylon X-wing shows the high maneuverability of FWMAV.

Chapter 2 Literature Review on Hovering Natural Flyers and Flapping-wing Micro Air Vehicles

This chapter reviews the scaling effect in hovering natural flyers, aerodynamics relevant to insect hovering flapping flight, man-made FWMAVs and their transmission, elastic storage mechanisms for FWMAV and electric motors for FWMAV.

2.1 Natural Flyers Capable of Hovering

Size matters to hover ability of natural flyers. Only some small natural flyers can hover. Hovering insects include tiny, Dipteran insects, such as a two-winged crane fly and a bigger one, such as four-winged Dragonfly and nectar-feeding hawkmoth. Examples of small hovering birds and mammals are hummingbirds, and fruit bats and respectively. Moderate size birds like pigeons can take off vertically but cannot sustain hovering flight due to high energetic cost requirement by hovering. Figure 2.1 shows some examples of the hovering natural flyers. Their wings are of various shapes: half ellipse, parabola, rectangle, and triangle, while the wing aspect ratio ranges between 1 and 8. Generally, the wingspan of the natural flyers increases with their body mass.

Figure 2.1 Size and weight of some natural flyers capable of vertical takeoff and hover.

Inability of larger natural flyer to hover could be explained by the decreasing the aerodynamic efficiency. According to the disk actuator theory, the aerodynamic efficiency in terms of the thrust per aerodynamic power is inversely proportional to the disk loading (body weight per unit wing area). Larger wings of increased area can ideally reduce the disk loading; they are however subjected to increased inertial powers that may discount the gain of power-specific thrust generation. Furthermore, driving of larger wings require higher torque and thus it is only possible by bigger and powerful muscles. Examination of natural flyers' flight apparatus could provide insights and inspire the engineering innovation to resolve the problems

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of FWMAV. We shall first review the anatomy of flight apparatus of a hummingbird, a dragonfly, and a housefly.

Among flying birds, hummingbirds can hover like Dipteran insects. As shown in Figure 2.2(a), a hummingbird has massive muscles: (1) a prominent breast muscle, called pectoralis major, that originates from the sternum (i.e. the breastbone) and attach near the bottom of humerus (i.e. the upper arm bone); and (2) supracoracoideus muscle, which connects between the keel of the sternum and the top of the humerus. When the pectoralis major contracts, wings beat down. In the upstroke, wings are raised by the contraction of supracoracoideus muscle.

A Brauer dragonfly have a pair of fore wings and a pair of hind wings. Dragonfly wings are directly attached to large muscles within the flight thorax, as shown in Figure 2.2(b). Elevators muscles, which attach to the thorax base, connect to the inner side of a pivoted wing base; whereas, depressor muscles connect to the outer side of the pivoted wing base. Elevator muscles pull the wings up; whereas, depressor muscles pull the wings down. Direct muscles of dragonfly are synchronous muscles that are characterized by the synchrony between muscles electrical and mechanical activity.

Dipteran insects are smaller flyers. For example, a fruit fly (*Drosophila virilis*) weighs only 2mg. It has a pair of 3mm long wings fast at 240Hz over a 150° stroke. Dipteran wings are extensions of a thoracic exoskeleton. Figure 2.2(c) shows that there are two sets of muscles attached with the thorax, namely 1) vertical (dorsal ventral) muscles that connect from the thorax base to roof; 2) longitudinal (dorsal longitudinal) muscles that connects between the front (anterior) and back (posterior) ends of the thorax. Contraction of the vertical muscles pull the thorax roof down, lifting the wing up. Contraction of the longitudinal muscles bows the thorax roof upward, flipping the wings down. The indirect muscles of Dipteran insects are asynchronous muscles.

Figure 2.2 Anatomy of natural flyers. Flight muscles and apparatus in (a) birds (b) insects with direct flight muscles (c) insects with indirect flight muscles.

Size matters in natural flight. Large birds can no longer sustain hovering as compared to small insects like hawkmoth and mosquitoes, as shown in Marden's study [9]. To hover, the flyer must flap large enough wings to generate high lift to support its weight. Figure 2.3(a) shows the wing span increased with increasing body mass. The wing mass of insects is extremely light relatively to its body, forming only a small fraction of 0.35%-6% of the body mass (b.m.), as shown in Figure 2.3(b). For example, a 19-mg crane fly includes a wing mass of 0.6 mg for a pair of 13.7 mm-long wings [10]. Heavier flyers like hummingbirds and bats required larger wings which takes up 6-12.9% b.m. [10]. For example, a hummingbird has a body mass of 5.1 g which includes a wing mass of 1.16 g for a pair of 58.5-mm-long wings (117-mm wing span). Strong muscles are required to drive the wings. Figure 2.3(c) shows that flight muscles formed a large fraction of insect bodies (17%-56%), but the flight muscles fraction decreases with heavier flyers as the structural mass such as wing, bones, and non-flight muscles need also to increase to support the body. Nonetheless, larger flight muscles can beat larger wings to generate higher maximum lift, as shown in Figure 2.3(d).

Figure 2.3 Scaling in natural flyers. (a) Wing length or half wing span, (b) Wing mass fraction of body mass (c) Flight muscle mass fraction (d) Maximum lift generated given the flight muscle mass of the natural flyers. [1, 9]

Muscle expends inertial power to reciprocate wings to and fro. The inertial power increases with larger wing inertia J_0 , higher wing stroke acceleration, $\ddot{\phi}$ and faster wing stroke speed $\dot{\phi}$, $P_{acc} = J_0 \ddot{\phi} \dot{\phi}$. A longer wing has a larger rotational inertia because of more mass distributed further from the wing root, $J_0 = \sum mr^2$. A sinusoidal wing stroke ($\phi = \Phi \sin 2\pi ft$) at wingbeat frequency, f with wing stroke angle 2Φ yields a time-averaged inertial power, $\bar{P}_{acc} = 8J_0\pi^2 f^3 \Phi^2$. Figure 2.4(a) shows that the wing inertia increases with larger body mass. Larger flyers beat wings at lower wingbeat frequency, as shown in Figure 2.4(b), while the wing stroke is generally large from 105-180° (1.83-3.14 rad) as shown in Figure 2.4(c). Figure 2.4(d) shows that the inertial power increases with body mass even though wingbeat frequency reduces with mass, because the wing inertia increased dramatically more. Larger butterflies are exceptional as they beat larger wings slowly.

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Figure 2.4 Scaling of wing inertial power in natural flyers during hovering: (a) wing inertia J_0 (b) wingbeat frequency f (c) wingstroke Φ (d) simple estimate of wing inertial power ($\bar{P}_{acc} = 8J_0\pi^2 f^3 \Phi^2$). Data from Weis Fogh. [1]

Aerodynamic power is expended to move wings against the air drag. Figure 2.5(a) shows that each flapping wing can be modeled as a thin partial disk of which induces a momentum jet downward. [11, 12] The area of the partial disk depends on the stroke angle and the wing length. The induced momentum creates a reaction lift or thrust on the wings to support the body mass. This partial disk actuator is a modified version of the propeller disk actuator theory as shown in Figure 2.5(b). By disk actuator theory, the power to induce jet momentum down is the minimum power to hover. The induced power increases in a linear proportion to the generated thrust. Other than the induced power, the induced drag, pressure drag, and skin friction drag also contributed to higher aerodynamic power. Figure 2.5(c) shows the average aerodynamic power increases with the body mass of natural flyers, according to the estimate by Wei-Fogh [1]. However, the aerodynamic efficiency (i.e. the ratio of thrust to induced power) is inversely proportional to the disk loading, following the disk actuator theory, as shown in Figure 2.5(d). The disk actuator theory predicts that a bigger flyer can fly more efficiently with larger wings if the wing mass is negligible.

Figure 2.5 (a) Momentum jet created by the flapping wing kinematic. The stroke plane can be modeled as a partial disk which creates air flows around the body (b) Disk actuator theory of a rotor-propeller. (c) The aerodynamic power estimated for different natural flyers during hovering. Data from [1] (d) The thrust per aerodynamic power (efficiency) of natural flyers reduces with disk loading (thrust per disk area), following the disk actuator theory.

Figure 2.6 (a) Main energetic costs increases with body mass during hovering. (b) Significance of inertial power to aerodynamic power during hovering.

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Hovering flapping flight is energetically costly with the high inertial and aerodynamic power. Figure 2.6(a) shows that the inertial power is often higher than the aerodynamic power, regardless of the size. The ratio of inertial to aerodynamic power is generally high for most natural flyers, as shown in Figure 2.6(b). The hovering of hummingbird expends as high as 53% of total power for wing inertia [13, 14, 15, 16]. However, during wing deceleration the inertial work could be absorbed by the aerodynamic braking and reduce the total energetic cost by 18% for hummingbird [13] at 35 Hz wingbeat. For insects like hawkmoth, Combes and Daniel [17] showed that the wing inertia force dominates over aerodynamic moments (108° stroke angle) at 25Hz wingbeat. Only the mosquito, which has a small wing stroke and inertia but high wingbeat frequency at 600Hz, expends 50% less power for inertia compared to aerodynamics.

There are two approaches to reduce inertial power. Firstly, the wings can be ultralight at the expense of structural rigidity, which can be afforded by flapping wings. The wing mass, $m = \rho chR$, increases with the wing volume and density. Wing bending stiffness $K = Ech^3/R$, increases with the Young's modulus, E , thickness, h and chord width, c , wing but reduces with wing length, R . So, a thin flexible wing can have lower inertia. In fact, larger natural flyers have flexible wings, which is ultralight as compared to the lift force it generates. For example, a pair of 12.4 cm-long wings of a long-eared bat *Plecotus auritus* [18] weighs about 0.581 g each, can support its mass of 9 g. A 5.1 g hummingbird *Amasilia fimbriata fluvialis* [13] has a pair of 5.85 cm-long wings weighing 0.309 g in total, with a moment of inertia of $8.0442 \times 10^{-8} \text{ kgm}^2$. Whereas a tiny 2-mg fruit fly *Drosophila virilis* [13] has a pair of 0.3cm-long wings which weigh 4.8 μg with moment of inertia of $1 \times 10^{-15} \text{ kgm}^2$.

Secondly, entomologists had found that elastic storage helps small insects reduce the inertial power incurred by flapping of nearly rigid wings [1, 13, 19]. Insects have elastic elements in their flight muscles [20], flight thorax, and wing hinges [13, 16]. In *Dipteran* insects, each wing rotates about a fulcrum at the pleural process in their flight thorax when their muscles contract to deform the thorax. At the end of wing stroke, wing momentum is converted into elastic energies, in the deformed elastic elements. The elastic energy then pays back for the wing reversal in assistance to flight muscles [15, 16, 21, 22]. Hence, most power output by the insect flight muscles is expended on aerodynamic power, and little is needed for inertial power.

2.2 Aerodynamics of Hovering Flapping Flight

Insect move wings cyclically with varying velocity and attitude during hovering. This unsteady motion makes it difficult to predict the wing aerodynamics. Quasi-steady analysis is often employed to estimate the wing's lift and drag as if the wing is translating linearly at a constant velocity and attitude. This quasi-steady analysis is subjected to error, but it can be

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compensated with unsteady aerodynamic mechanisms such as rotational lift, clap and fling and wake capturing and added mass.

Figure 2.7 Basic aspects of fluid mechanics, related to flapping wings. (a) Force components of a flat blade oriented obliquely to a flow. (b) Lift coefficient measured against the angle of attack of a hummingbird wing [23] (c) Aerodynamic polar: lift coefficient as a function of drag coefficient; parameter aerodynamic angle of attack. [23] The flapping wings generate a time-varying vertical thrust (lift) and drag on the body depending on the angle of attack as illustrated in (d) Forward stroke of a hummingbird during hovering. (e) Backward stroke of a hummingbird during hovering.

First reviewed is the quasi-steady analysis of flapping wing. Figure 2.7(a) shows a 2D chord section of rigid, thin flat plate wing with chord length, c in a steady incompressible flow with freestream velocity U_∞ at an angle of attack below the stall angle. Lift force is generated perpendicular to flow and a drag force in the direction of flow. In experiments, the lift and drag of a wing are characterized by the dimensionless lift coefficient and drag coefficient:

$$C_l = \frac{L'}{(1/2)\rho c U_\infty^2}, C_d = \frac{D'}{(1/2)\rho c U_\infty^2} \quad (2-1)$$

where ρ is the mass density of air, c is the wing chord length, and U_∞ is the relative velocity of the incoming air to the wing. Figure 2.7b-c shows the lift coefficient polar and the lift and drag polar of a hummingbirds' chord section. The lift is positive when the angle of attack is positive, and the lift increases to a maximum when the angle of attack increases towards about 45° . During hovering, natural flyer, e.g. a hummingbird, can maintain a net vertical thrust by rotating its wing chord to positive angle of attack accordingly to the forward and backward stroke, as shown in Figure 2.7d-e.

Figure 2.8 (a) flat plate at the starting of the flow and (b) lift-generating flat plate in a steady flow [24]. (c) Kutta-Joukowski theorem of lift generation due to circulation. Adapted from [25]. (d) Different pressure distribution along chord for the upper and lower surface of the airfoil, which results in lift generation.

The lift generation can be explained by the continuity of mass and conservation of momentum in the air flow with the help of circulatory flow, as shown in Figure 2.8(a-d). At the start of translation, a pair of equal but opposite vortices are each developed at the leading edge and trailing edge respectively. The leading-edge vortex (LEV) is bound to the wing while the trailing edge vortex (TEV) is shed away from the trailing edge, leading to the steady state flow pattern in Figure 2.8(b). In the steady state, the flow rejoins at the trailing edge to leave tangentially at a finite velocity, i.e. there is zero vorticity at the trailing edge (*Kutta condition* for lift generation). The shed trailing edge vortex delays the buildup of the bound LEV vortex circulation until the wing has left it about three chord lengths in its wake (*Wagner effect*). Local air velocity, U is faster on the upper surface where the streamlines are squeezed closer in the smaller area between them, because of mass continuity ($\text{Area} \times \text{velocity} = \text{constant}$). Conversely, air velocity below the lower surface is slower where the streamlines are further from each other. Along the streamlines, momentum is conserved following Bernoulli equation ($p + (1/2)\rho U^2 = \text{constant}$). The upper surface has a lower static pressure p because of the higher dynamic pressure, $(1/2)\rho U^2$ at higher air velocity; whereas the lower surface has a higher static pressure because of lower air velocity. This pressure difference, $p_{\text{upper}} - p_{\text{lower}} = (1/2)\rho(U_{\text{upper}}^2 - U_{\text{lower}}^2)$ results in a suction force to lift the airfoil. *Kutta-Joukowski* theorem ascribes the velocity difference between upper and lower surface to a pure circulation, Γ , developed about the flat plate which is added to the steady incoming flow, as shown in the pictorial equation in Figure 2.8(c). The circulation Γ around the wing is a measure of the velocity difference by the circulatory flow, which is a unique value satisfying the *Kutta condition* for linear translation:

$$\Gamma = \pi c U_{\infty} \sin \alpha \approx \pi c U_{\infty} \alpha \quad (2-2)$$

for small angle of attack, α .

The lift per unit span due to the circulation around an air foil in steady motion is given by the *Kutta-Joukowski* theorem as:

$$L' = \rho U_{\infty} \Gamma = \pi \alpha \rho c U_{\infty}^2 \quad (2-3)$$

where ρ is the air density.

Equations (2-1) and (2-3) yield that the theoretical lift coefficient increases with angle of attack at the rate of 2π : $C_l = 2\pi\alpha$.

Following Ellington's quasi-steady analysis method [26], the aerodynamic forces caused by translational and rotational motion can be formulated based on circulation. The translation lift and drag can be calculated based on force polar measured from a scaled rigid wing after the fruit fly *Drosophila*'s [27] at Reynolds number (Re) of 136. The lift and drag coefficients of this *Drosophila*'s rigid wing varies with the angle of attack α in degree:

$$C_L(\alpha) = 0.225 + 1.58 \sin(2.13\alpha - 7.20) \quad (2-4)$$

$$C_D(\alpha) = 1.92 - 1.55 \sin(2.04\alpha - 9.82) \quad (2-5)$$

Delayed stall is apparent in this wing as the lift coefficient continues to increase to a maximum at around 45° , whereas fixed wing stalls at angle of attack below 20° . This delayed stall is due to spanwise flow of the flapping wing that stabilize the leading-edge vortex such that it does not separate from the wing [28] and maintains lift.

In addition to translation, the wings also pronate and supinate, as shown in Figure 2.9(a). These actions of rotating the wing chord about an axis parallel to leading edge creates a rotational circulation even before the wing starts translation. [22] This rotational circulation is additional to the circulation due to translation, and contributes a resultant force that is normal to the wing chord, as shown in Figure 2.9(b). [27] There are several rotational mechanisms which create rotational circulation [22]: the fling (or peel with flexible wing), the partial fling, near fling and the isolated rotation [3]. The amount of rotational circulation generated is proportional to Ωc^2 where Ω is the rotational angular velocity of the wing chord and c is the wing chord length.

(a) Wing supination and pronation **(b) Circulation induced by rotation** **(c) Circulation and impulse developed in Weis-Fogh Fling Mechanism**

- (i) Clap/Start of fling (ii) Development of rotational circulation (iii) End of fling (iv) Lift generated upon separation of TE

Figure 2.9 (a) Wing chord rotation due to pronation and supination in a flapping wing stroke cycle. (b) Circulatory flow and force developed by the wing chord rotation. (c) Rotational circulation induced by Weis-Fogh fling mechanism [1] and the impulse generated during the fling process according to Maxworthy [29].

The clap and fling are illustrated for a fruit fly in Figure 2.9(c(i-iii)). At the end of the upstroke, two wings are clapped together and starts to fling open. Subsequently, the wings wing chords open like a V, but the trailing edges remains in contact. Air rushes into the opening of the wings towards the apex of the V and forms vortices at the parting leading edges. Thus, an impulse is produced during this short clap and fling period. When the wings clap, two wings are brought to close to each other with their wing chord section forming a Λ shape. The wings supinate to close their trailing edges together to force out a downward air jet. As of now, there are no models on the clap, because at the end of clap the outflow velocities from the cavity becomes infinite. However, Ellington suggested that the clap can be estimated roughly using a reversed flat peel model. [30]

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The wings could also enhance lift by capturing the wake from either the previous stroke, or in a tandem winged insect case like the dragonfly, the wake from the fore wings. Insects which flap their wings with small stroke amplitudes, but at large pitch amplitudes benefit relatively more from wake capturing. This is demonstrated by mosquitoes with small 44° stroke angle and large pitch amplitudes of 80° in a study by Bompfrey et al [31]. A mosquito was observed to begin its downstroke with captured wake of trailing edge vortices from preceding upstroke, increasing the circulation about the wing. As a result, it generates a high peak lift force shortly after pronation. However, if the stroke amplitude is increased while maintaining the average speed, the peak lift is reduced. In Dickinson's experiment of 2D wing reciprocation [3], a reversing wing captures the wake of previous stroke when it flips direction. The vortex from previous wake enhances the circulation around the wing to increase the circulatory lift.

The final aerodynamic force considered is the added mass force, which is not due to circulation. This force is an inertia effect resulting from the accelerating or decelerating of neighboring air mass surrounding the wing. By inviscid flow theory, a thin plate, which is accelerated normal to its chord, carries an added mass equal to the mass of air in an imaginary circular cylinder around the wing with the chord as its diameter [10]. If the thin plate is at moved at an angle of attack, then the effective diameter of the added mass in that direction is reduced to $c \sin \alpha$.¹ The added mass per unit span in the lift direction and the drag direction is given by:

$$dm_{added,L} = \frac{1}{4}\rho\pi(c \cos \alpha)^2 dr, \quad dm_{added,D} = \frac{1}{4}\rho\pi(c \sin \alpha)^2 dr \quad (2-6)$$

The force per unit span due to added mass is the simply the product of the wing body acceleration and the added mass in the respective direction. Note that in hovering, we assume the wing chord is translating in a horizontal stroke without any acceleration in the vertical direction, then there is no force due to added mass in the vertical lift direction, only in the horizontal drag direction.

¹ See equation 8.12, page 337 of [119]

2.3 Man-made FWMAV Capable of Hovering

Figure 2.10 Size and weight of man-made FWMAV. Note that Robobee is currently hovering with tethered power supply because there are no batteries small enough for it.

Very few FWMAV capable of untethered hovering with onboard battery. Figure 2.10 shows the state of the art of hovering FWMAV. Like the natural flyers, a bigger FWMAV requires a larger wingspan to stay aloft in air. For example, a 60-mg Robobee (Harvard) has a wing pair of 30-mm span [32, 33]; a 16-g DelFly (TU Delft) has two wing pairs of 280-mm span; a 19-g Nano Hummingbird (DARPA-sponsored Aerovironment Inc.) has a wing pair of 165 mm span; a 440-g Mentor (Advanced Subsonics Inc.) has two wing pairs of 360-mm span.

The flight apparatus of the FWMAV mainly consist of a motor or actuator, gearbox, and flapping mechanism, as shown in Figure 2.11. The motor provides the power to drive the wings. A speed reduction gearbox to amplify the motor torque output to match the wing inertia and aerodynamic torque. The rotary motion of the motor is converted into reciprocating wing motions via three general mechanisms, namely the crank-rocker mechanism, crank-slider mechanism and the string-based crank-pulley mechanism.

Figure 2.11 Common flapping mechanism and transmissions in FWMAV.

The crank-rocker transforms a continuous turn into angular sweep or reciprocation. A crank-rocker drives only one wing plane. The crank-rocker has three moving linkages, namely the crank, the coupler link, and the wing base rocker, and a ground linkage between the anchor of the rocker and the crank center of rotation. A wing is supported by a wing base. The end of the wing base is hinged to an anchor. A connecting link couples the crank tip to the other end of the wing base. While the crank turns 360° , the rocker reciprocates and sweeps the wing over an angular stroke. The linkage lengths need to satisfy the Grashof condition for four bar-linkage crank-rocker mechanism, i.e. the sum of the shortest and longest link of the planar linkages is less than or equal to the sum of the two remaining links. The crank must be the shortest link because only the shortest link can possibly rotate 360° due to geometric constraint in a four-bar mechanism. The crank-rocker mechanism is suitable for the driving of large flapping wing mechanisms, but it will be subjected to high load. A spring can be added to the pivot of wing base for the purpose of elastic storage. The spring undergo deformation in proportion to the wing stroke.

The crank-slider transforms motor rotation into a linear reciprocation. This mechanism consists of four moving linkages and a sliding guide. The wing base end is joined to a slider at the symmetry. The wing is pivoted near its wing base by a vertical linkage at the side (pleural linkage). The pleural linkage is anchored to the ground. The wing rotates up as the slider moves down and vice versa. Because two wing bases can join to the slider at the symmetry, the crank-slider could drive two wings simultaneously in phase in a compact design. However, the crank-slider requires a sliding slot to guide the slider. Sliding incur high friction especially when forces normal to the sliding surfaces are high. Hence, the crank-slider mechanism is used for

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driving small flapping wings that impose a low load. This mechanism has an extra pleural linkage at the side which pivot the wing. This pleural linkage is anchored to the ground and rotates in lesser angle than the wing.

Revolute joint mechanisms usually wear out over time due to contact friction and introduce free play, which discounts the flight performance greatly. To avoid the wear of revolute joints, unconventional drive mechanism of the Nano-Hummingbird [34] uses series of tensioned strings to rock wings about pulley with large amplitude. Ball bearings were also used for supporting the rotating shafts and they significantly reduce the friction. This motorized rigid-mechanism has no means of energy storage to recover wing inertial power like insects do.

A proper gear ratio reduces the motor output speed to match the average wing angular speed and amplifies the motor output torque to match wing torque. By conservation of energy, the power output of the motor is equal to the power expended on the wing:

$$Q_{wing}\dot{\phi}_{wing} = Q_{motor}\omega_{motor} \quad (2-7)$$

$$\Rightarrow \frac{Q_{wing}}{Q_{motor}} = \frac{\omega_{motor}}{\dot{\phi}_{wing}} = \text{Speed Reduction Ratio (S.R)} \quad (2-8)$$

The right gear ratio allows the motor to operate at the peak efficiency operation. A common gear train consists of multiple stages of gears of same modules but different number of teeth. If the first stage has gear of teeth N_1 driving another gear of teeth N_2 , and the second stage has N_3 driving N_4 and so on, the gear ratio can be determined by:

$$G.R. = \frac{N_2}{N_1} \frac{N_4}{N_3} \dots \quad (2-9)$$

The crank-rocker/slider/pulley mechanisms themselves also help to magnify the output torque for driving the flapping wings. Upon a half turn by the output gear the rocker/slider/pulley reciprocates the wing for a stroke angle 2Φ in a half wingbeat cycle. Based on the principle of virtual work [35], a crank-rocker/slider/pulley angular ratio for torque magnification is:

$$C.R. = \frac{\pi}{2\Phi} \quad (2-10)$$

The total speed reduction ratio provided by the gear train and crank ratio is:

$$S.R. = G.R. \times C.R. \quad (2-11)$$

Richter's - 3.89g 14.3cm-span

Figure 2.12 Richter's 3D-printed mechanical insect from Cornell University. [36]

Figure 2.12 shows the 3D-printed mechanical insect from Cornell University with its weight breakdown. It has two pairs of 80-mm-long wings, both hinged together at the middle like a scissor. Each wing is flapped to sweep 80° stroke, actuated by an asymmetric double crank rocker mechanism which is driven by a Solarbotics GM15 gear motor. The transmission and wings are 3D-printed out of low friction, translucent acrylic-based photopolymer (FullCure720). Two sails, one at the top and the other bottom, helps to stabilize the X-wing to hover at 23Hz wingbeat frequency for 85s in air.

DelFly II - 16g, 28cm-span, 14Hz

Figure 2.13 DelFly II from TU Delft. [37, 38]

Figure 2.13 shows the DelFly II from TU Delft, having four wings of 280 mm span, each with a positive dihedral angle to stabilize flight against rolling. DelFly II can hover for 8 mins at 14Hz wingbeat frequency and fly fast for 15mins with onboard camera and radio system [37], with a 3.6g Cyclone 130mAh, 20C Lithium Polymer Cell². DelFly II weighs extremely light at 16.07 g [39] with the wings and brushless motor at 1.1 g and 1.362 g respectively [38]. It drives four wings which is reinforced by carbon ribs, sweeping a stroke of 44° [38] through a symmetric double crank-rocker transmission. A speed reduction gearbox of 21.33:1 ratio amplify the motor torque to drive large 28cm-span wings. It has low-friction transmission made

² http://www.atomicworkshop.co.uk/catalog/index.php?main_page=product_info&products_id=190

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of plastic injection molding and bearing supported shafts. It can achieve a thrust-to-electrical power ratio of 9 g/W.

Figure 2.14 FlowerFly from Temasek Laboratories, National University of Singapore. [40]

Figure 2.14 shows the 16.6 g FlowerFly from Temasek Laboratories (TL), National University of Singapore (NUS) having four separate wings of 22cm tip-to-tip wingspan. A double crank-slider mechanism was used to drive four wings of 10 cm long. Each wing swept a stroke angle of 90° and the wings clap and fling partially at either end of the stroke. It takes off at 10Hz and hover for 5 minutes with a 2.5 g 70 mAh LiPo battery, and 1.5minutes with an additional 4 g payload.

Figure 2.15 Nano Hummingbird from AeroVironment Inc. [34]

Figure 2.15 shows the DARPA sponsored Nano Hummingbird from AeroVironment Inc. It is a 19.0 g, 16.5cm-span two-winged FWMAV that beats a large wing stroke of 200° at frequency as high as 30Hz. Its hovering time lasted for 4.0 minutes while it carried onboard camera and autopilot. It uses an innovative string-based crank pulley mechanism to achieve this large stroke, which is impossible with crank-rocker or crank-slider mechanism. To withstand the high forces induced by the high-frequency flapping on the pulley mechanism, ball bearings are used to support shafts and pin joints, while the main structure are made of 7075 Aluminum and other components were steel and titanium. The 17.5 g Saturn, which is the weight-reduced version of Nano Hummingbird without camera and body shell but with larger battery, can hover much longer for 11 minutes.

van Breugel- 24.2g 45cm-span

Figure 2.16 Floris van Breugel's eight wings clapper. [41]

Figure 2.16 shows the 24.2 g FWMAV by van Breugel (Cornell University) having eight wings of 45 cm tip-to-tip span. Each pair of wings clap and fling partially at the wingtip when a 1.2 g Solarbotics GM15 gear motor drives the wing transmission based on a double crank rocker mechanism. Each wing sweeps a stroke angle of 36° . Each wing, about 83.81-mm long, is placed at extended arm of 139.08 mm away from the rocker wing hinge; this results in a tip-to-tip wingspan of 45 cm. This eight wing ornithopter can hover for 33 s by beating wings at 17.2 Hz wingbeat frequency, given the two onboard LiPo battery of 3.1-g, 3.7-V, 90-mAh. Its main structure is made of light weight balsa wood while the wings are made of $13\mu\text{m}$ thick PET films reinforced with carbon ribs. Cross sails on top and bottom of the FWMAV help to stabilize the FWMAV to a vertical attitude.

Robotic Hummingbird - 62g 30.48cm-span

Figure 2.17 Robotic Hummingbird from Texas A&M University. [42]

Figure 2.17 shows the 62-g Robotic Hummingbird from Texas A&M University having two wings of 30.48 cm tip-to-tip span. This two-wing ornithopter can hover for 5 s by wing beating for 120° at 22Hz. Figure 2.17 shows the 5-bar mechanical linkages, which combines a double-crank-rocker and two sliders, used to produce the wing stroke of 120° . A C-10 2900KV brushless DC motor through a speed reduction gearbox of 9.3:1 ratio was used to drive the mechanism. The onboard power supply is a custom built 3-cell (11.1V) LiPo battery pack.

The Mentor series of FWMAV are the heaviest FWMAV to date. Two types of drivers were used, namely an internal combustion engine to power a 582 g prototype of Mentor SF-2.5; an electric motor to power a 440 g prototype of Mentor SF-3. These ornithopters were equipped with two wing pairs of 36-cm span which are arranged like a scissor with a virtual center. This

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ornithopter of SF-3 can hover for 20 s by clapping and flinging the four wings. Driver of SF-3 is a brushless motor Astro flight 020 with sensor feedback motor controller and a 12.32:1 speed reduction gear ratio. A battery pack of 8×600 mAh NiCd cell was used to power the motor for a target flight time of 1.5-minutes. However, the hover time only lasts for 20 s, beyond when the overheating happens to the battery and deplete the power output. In comparison, a recommended propeller³ (7×4 APCE) under direct drive by the same motor can produce a maximum thrust of 635.

Mentor SF-3 - 440g 36cm-span

Figure 2.18 Mentor SF-3 electric-powered X wings clapper from Advanced Subsonic Inc. [43]

Figure 2.19 Dragonfly-inspired BionicOpter from Festo with nine degree of freedom wing movement and four degree of freedom of head and tail movement. Four wings are driven together at same frequency, yet each wing can be twisted and adjusted in stroke amplitude separately. [44]

³ <https://www.gravesrc.com/astroflight-brushless-020-direct-drive-airplane-motor.html>

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Figure 2.19 shows the 175 g BionicOpter from Festo having four wings of 63 cm wingspan and 44 cm body length [44]. A brushless VS external rotor drives four wings together from 15 to 20 Hz. Four motors, each at the wing joints infinitely adjusts the stroke angles of each individual wing from approximately 80° to 130° . In addition, another four servo motors each twist a wing by up to 90° pitch angle individually, turning the wing from horizontal to vertical. The swiveling of the wings changes the thrust direction while the thrust generation is changed by the amplitude controller, allowing the transition from hovering to forward flight. Four nitinol shape memory alloys (SMAs) wires are used to move the head horizontally and the tail vertically. The BionicOpter is powered by two LiPo cells at 7.6V and controlled by a high processor ARM processor with feedbacks from inertia, acceleration and position sensors onboard. However, its flight endurance and weight breakdown are not provided.

Table 1 summarizes the design specifications and performance of the hovering FWMAVs developed so far. The demonstrated hovering durations are much shorter than the DARPA's target of 20 mins. To increase hover endurance, motor and wing transmission must be redesigned for higher mechanical efficiency in addition to efficient aerodynamics. In view of these, a low-loss and powerful motorized transmission is in need to realize the efficient hovering by flapping wings.

Table 1 Size and flight endurance of hovering/near hovering FWMAV

FWMAV	Year	Mass (g)	Wingspan (cm)	No. of Wings	Hover Frequency (Hz)	Hover time (s)	Battery mass(g)
NAV Definition		<10	7.5-10	-	-	1200-1800	-
MAV Definition		<70	<15	-	-	1200-3600	-
Richter [36]	2010	3.89	14.3	4	22	85	1.39
DelFly II [45]	2006	16	28	4	14	480	3.6
TL-FlowerFly [40]	2015	16.6	22	4	10	300	2.5
Saturn	2012	17.5	15.8	2	27.5	660	4.8
Nano Hummingbird [34]	2012	19	16.5	2	30	240	4.8
van Breugel [41]	2007	24.2	45	8	17.2	33	6.2
Robotic Hummingbird (Texas A&M) [42]	2015	62	30.48	2	22	5	10.4
Festo BionicOpter [44]	2013	175	63	4	20	NA	NA
Mentor SF-3 [43]	2002	440	36	4	30	20	154
Mentor SF-2.5 [43]	2002	580	36	4	30	60 (Fuel up to 360s)	55 (fuel)

Figure 2.20 Scaling in FWMAV. (a) Wing length (b) Wing mass fraction of body mass (c) Motor mass fraction of body mass (d) Maximum lift generated given motor mass of FWMAV, compared to direct-driven propellers.

Figure 2.20 shows the scaling effect on FWMAV. A heavier FWMAV requires more powerful motor and larger wings for high thrust generation. The wing mass fraction is small at about 15% or less, except for van Breugel's eight-wing ornithopter at 26%. The motor mass fraction varies greatly from 8% to 30% of the body mass, with the lowest being DelFly II and the highest being Richter's 3D-printed mechanical insect. It is noted that a bigger motor is required for indirect drive of flapping wings for higher thrust. Similarly, the thrust generation by propeller under direct drive is also proportional to the motor mass.

However, the flight muscles of natural flyers are powered by themselves. To be fair, we add battery mass to the motor mass to form the 'flight muscle' of man-made flight. This immediately halved the maximum lift generation of man-made flyers by its muscle mass as shown in Figure 2.25(b). The mass-specific lift generation of direct-driven propellers is now in the range of 12-135N/kg. Note that increasing a battery mass would reduce the muscle-specific maximum lift for the propellers. Maximum lift generation can be increase only by increasing both the battery voltage and the propeller size. For FWMAVs, the brushed motor (Solarbotics GM15) in van Breugel's eight-wings flapper can only generate lift 1.5-2.6 times weight at 15.1-25.3N/kg. Whereas, the FWMAVs driven by brushless motors can generate maximum lift about 1.8-5.83 times the motor and battery weight at 17.6-57.2N/kg. It is observed that the muscle-mass specific maximum lift decreases for the FWMAV because of the large battery pack used.

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Hover time of micro air vehicles depends on several parameters, namely power specific thrust generation T/P to overcome the gravity, body weight mg , power capacity E_b of on-board battery, and efficiency η of electronic controller. It follows the relationship:

$$t_{\text{hover}} = \left(\frac{T}{P}\right) \left(\frac{E_b}{mg}\right) \eta \quad (2-12)$$

So far, most bird-size MAVs (including both ornithopter and quadcopter) has 15% to 35% of its body mass taken up by the battery mass (See Figure 2.21(a)). However, the hover time of MAVs shows a decreasing trends of hover time with respect to increasing the body mass (including the battery mass). Increasing the battery capacity does not simply increase the hover time because it may increase the disk loading and thus reduce the aerodynamic efficiency (i.e. the ratio of thrust to aerodynamic power). For example, 19g Nano Hummingbird can hover for 4 minutes while its stripped-off version, i.e. Saturn of 16g, can hover for 11 minutes. Too light a MAV needs not be better if it cannot carry powerful motor. For examples, eight-wing ornithopter cannot hover for long when powered by 4 inefficient brushed motors of 1.2 g each.

Figure 2.21 (a) Battery mass fraction (b) hover endurance of FWMAV and commercial quadcopters from HobbyKing. (c) Hovering endurance against battery mass fraction. Endurance of hovering is a function of thrust-to-power efficiency(T/P), MAV mass (m) and battery energy capacity (E): $t = \left(\frac{T}{P}\right) \left(\frac{E_b}{mg}\right) \eta$.

2.4 Elastic Storage for Flapping wing

Introduction of elastic to flapping mechanism can recover wings' kinetic energy from being lost during deceleration. They are various forms of elastic storage proposed and developed, ranging from insect-inspired thoracic compliant mechanism to just a simple coil spring (Figure 2.22). They need to be tough and durable to allow cyclic beating of large wing stroke. However, they present static elastic load and in turns increase the internal friction to the mechanism.

(a) Baek's linear coil spring for Vamp RC and a small hummingbird-inspired flapping mechanism

(b) Hines' torsional spring for direct-drive flapping wing

(c) Sahai's rubber flexure (leaf spring)

(d) Wood's elastic thoracic mechanism

Figure 2.22 Springs for FWMAV. (a) Baek introduced linear coil spring as energy storage to Interactive Toy's Vamp RC ornithopter and achieved up to 19% power reduction. [46] (b) Torsional spring at the motor shaft assist direct flapping of a pair of 70m-long wings [47]. (c) Rubber flexures increase stiffness of polyimide hinges to flap a pair of 60mm-long wing. (d) Piezoelectric actuators are used as the elastic energy storage in the Harvard Microrobotic Fly (Now Robobee). Its thorax is a compliant mechanism but not utilized to store energy. Picture adapted from [48]

Benefits of elastic storage was theoretically proven for motor's indirect drive of flapping wings. Mandangopal et al (2005) [49] and Tantanawat and Kota [50] separately showed that addition of a linear coil spring in a crank-rocker mechanism can reduce the peak torque requirement up to 42% as compared to that without a spring. This proposed conceptual design of elastic storage has a linear coil spring coupled between a motor shaft and wing base; this was not practically realized because of limited space and geometry constraint in the crank-rocker mechanism. Experimental implementation of elastic storage was realized on crank-slider mechanism; a linear coil spring was coupled to the slider and an external supporting plate. [46] This setup of elastic storage is however limited to tethered test but not applicable to free flight. The tethered test showed this means of elastic storage helps the driving motor and saves electric power up to 19% as compared to that without elastic storage.

Geared motor or brushless motor was seldom used for direct drive of flapping wings due to high torque requirement. Interestingly, a direct resonant drive of flapping wings was recently

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demonstrated by adding a torsional spring that coupled the motor shaft to a flapping wing. [47, 51] Sinusoidal voltage activation of the motor can excite the spring-supported wing into resonant oscillation. The resonant drive can magnify the wing stroke amplitude but require minimal input power. Beyond or under resonance, performance of direct driven flapping deteriorates. So far, direct resonant drive can only drive two-wing bioinspired flapping wings for wired take-off, with external power supply. It has yet been used to power a heavier bioinspired MAV for a free flight.

Instead of using motor as the primary driver, an insect-inspired nano air vehicle, RoboBee from Harvard (previously known as Microrobotic Fly) made use of a piezoelectric bimorph to resonant drive a pair of flapping wings on the support of a film-hinged pseudo-rigid body mechanism. [52, 53] Piezoelectrically resonant drive of the insect-size flapping wings can magnify the stroke amplitude and beat the wings at very high frequency. This 60mg RoboBee demonstrate a vertical take-off with external power supply. [54] It is too light to carry onboard battery. On the other hand, dielectric elastomer actuators were also explored for resonant driving of flapping wing, but they together with high-voltage converter are far too heavy and too slow to drive a bioinspired for a take-off. [55]

A pseudo-rigid-body compliant mechanism behaves like a rigid-body mechanism, except with elastic storage at the joint. It is free from friction by replacing a pin joint with a film hinge. It can be made lightweight by monolithic construction of the film hinge together with carbon-fibre reinforced polymer plates. To increase the elastic storage, a coil spring or rubber strips were separately integrated to the pseudo-rigid-body mechanism. [56] Distributed compliant mechanism makes use of long and thicker plastic flexures instead of film hinge. It comes in a monolithic piece with complex pattern of flexures that vary in thickness. It can store much more elastic storage than the film-hinged compliant mechanism. Precise dimensions of the flexure require fabrication by injection molding.

In addition to recovering kinetic energy during wing deceleration, a spring helps a motor accelerate wings upon elastic recoil towards the spring's neutral position. In this way, the spring recoil helps achieve a faster wing reversal and thus enhance the passive wing rotation amplitude. Bias of the spring also leads to asymmetric duration between upstroke and downstroke. For example, if the no-load position of the spring is designed at the extreme position of downstroke, the elastic resistance will slow down the upstroke while the spring recoil accelerate the return of downstroke. According Tang [57], such duration asymmetry is found to help increases the dynamic force output while a harmonic force of constant amplitude drives the flapping wings. This also helps enhance lift generation according to Kok [58].

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Other than the compliant hinges, distributed compliant mechanism [50, 59] was also presented, where energy is stored in an entirely flexible plastic body frame. However, such frame design is complicated with structural members of varying thickness, and risk stress concentration at thin sections. Also, manufacturing is costly as the plastic structure is made using expensive custom molds. It is simpler and cost effective to have the compliance restricted to hinges alone, as long as the compliant hinges is stiff enough to store sufficient energy.

Besides recovering kinetic energy, spring recoil assists motor to achieve a faster stroke return towards the spring's neutral position. The quicker wing reversal also induce a faster passive wing rotation. A bias of the spring no-load position causes asymmetric duration between upstroke and down stroke. For example, if the spring neutral position is placed at the end of down stroke, the up stroke is slower due to elastic resistance; whereas the down stroke is faster because of spring recoil. Asymmetric duration of upstroke and down stroke are demonstrated by Dipteran insect's click mechanism [60] and its scaled model with nonlinear stiffness [57]. The duration asymmetry is found to help increase the dynamic force output of wing flapper given a constant amplitude force input. Recent simulation by Kok et al also shows that the duration asymmetry helps improve the lift generation two-wing flapping, with large wing stroke.

2.5 Motors for FWMAV

Rotary DC electric motors are first reliable choice for FWMAVs, because they available off-the-shelf in various sizes and supplemented with light and compact control electronics. They are fast spinning rotors with low torque. Common DC electric motors exists in two classes: brushed and brushless motors. DC motors generate torque directly proportional to the current, I through the windings:

$$Q_m = k_t I \quad (2-13)$$

where k_t is the motor torque constant.

The motor spinning induces a back emf V_e that is proportional to its rotational speed ω_m :

$$V_e = k_e \omega_m \quad (2-14)$$

where k_e is the back emf constant.

The motor input voltage is therefore equivalent to the back emf and the potential drop across the resistance:

$$V_{in} = IR + V_e \quad (2-15)$$

By conservation of energy, the electrical power input to the motor is equal to the mechanical output and the electrical resistive loss:

$$IV_{in} = Ik_t \omega_m + I^2 R$$

$$I^2 R + I k_E \omega_m = I k_t \omega_m + I^2 R \quad (2-16)$$

which yields the motor torque constant to be equal to the back emf constant: $k_e = k_t$

The shaft power of the motor can be easily obtained from:

$$P_{shaft} = Q_m \omega_m = k_t \omega_m I(t) \quad (2-17)$$

The motor rotational speed is proportional to the wing beat frequency f multiplied by the gear ratio: $\omega_m = 2\pi f \times G.R.$ for a FWMAV. The motor efficiency for converting the electrical power input to mechanical shaft power is theoretically given as following:

$$\eta_{motor} = \frac{P_{shaft}}{P_{electric}} = \frac{k_t \omega_m I(t)}{VI(t)} = \frac{k_t}{V} \omega_m \quad (2-18)$$

It is proportional to the motor rotational speed given a constant motor torque constant and constant input voltage. The speed constant, $k_v = 60/(2\pi k_t)$, is often used to characterize the brushless motors, e.g. the brushless motor AP03-7000kv has a speed constant of 7000 RPM/V.

Figure 2.23 (a) Motor efficiency along motor mass. (b) Mass-specific output power of brushed and brushless motors. Data extracted from [61] and [62]

Brushed DC motors are suitable for high-speed low-torque operations and are throttled easily by just increasing the input voltage, as shown in Harrington's [61] characterization on off-the-shelves motors. The motor outputs more power as voltage increases. At the same voltage, the motor torque decreases linearly at a constant slope with motor speed (RPM). Therefore, the motor outputs maximum power at half of the maximum motor speed. However, the motor could suffer high-current overheating that burns the coil insulation if operated at too high voltage. Also, brushed motors are short-lived and less efficient for high cyclic loading because their commutator brushes suffered from friction wear in the long run.

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In contrast, brushless motors are more efficient without brush friction, as shown in Figure 2.23(a), while being larger in mass. So, the brushless motors expend less electrical power to produce the same mechanical power. The brushless motors also have higher mass-specific power output, as shown in Figure 2.23(b). Brushless motors produce 575 to 1333 W of power per kg of motor mass, whereas the brushed motor only achieve 96 to 386 W of power per kg of motor mass. However, brushless motors require dedicated electronic speed controllers (ESC) to throttle the motor speed. Losses in the ESC reduces the overall brushless motor efficiency, of which the brushed motors do not suffer. The brushless motors are also larger in size and mass compared to the brushed motors.

For small FWMAV of less than 5g, the brushed gear motors are better choice because it is light and compact. Furthermore, the motor can be throttled easily by ramping up voltage without ESC. Because of the limited power output, the brushed motor can only drive smaller 10-14cm span flapping wing mechanism instead of the large 28cm FWMAV. Also, the brushed motors are fitted with factory-made speed reduction gearbox with planetary gear which is compact and reliable than if we custom-made our gearbox. We select a small 1.2 g 6-mm diameter coreless DC brushed gear motor (Precision Microdrives 206-102 [62]) which produce a maximum continuous torque of 0.2mNm at 3V and rated load speed of 1650RPM. Increasing the voltage supply this motor will increase the maximum torque and so the maximum speed, but risk overheating the motor. Another DC brushed gear motor (Solarbotics GM15) of 1.3g with similar rated performance was also tested but not used because its gearbox breaks easily even at rated loading.

For the larger FWMAV, the AP03-7000 brushless motor is found to be the most versatile off-the-shelf motor which has comparably high efficiency in a wide output power range, as shown in Figure 2.24. It is light at 3.1 g and outputs high mechanical power up to 1.9W at 70% throttle with 3.8V input voltage at relatively high efficiency around 50%. It can be powered by a single cell 3.7V LiPo and controlled by a lightweight 1S ESC (0.36g Mi3A ESC or 0.7g 1~2S YEP-7A ESC). Furthermore, it was tested with a propeller (3020 prop) to give a maximum thrust of 44g at full throttle. Hence, ideally it can generate sufficient thrust for the hovering of a 30g FWMAV. The AP03-7000kv motor produces a stall torque of 3.0 N.mm at 4.2V input at the ESC terminals.

Figure 2.24 Motor efficiency and the mechanical power output of some custom-made and off-the-shelves brushless motors investigated by TU Delft for DelFly. Delfly18 represents a modified motor with 18 windings. The DelFly motor modification is described in [39].

Figure 2.25(a) compares the maximum lift per muscle mass of natural flyers to the maximum lift per motor mass of man-made flyer and propeller. On average, natural flight muscles can produce lift 5-6 times of its mass (i.e. mass-specific lift of 54-63 N/kg) [9]. Hovering insects size from milligrams to grams which clap and fling, could augment maximum lift by 25% to 72.2-86.2 N/kg [9] for damselflies, butterflies and moths. In comparison, the mass-specific lift generation of direct-driven propellers is much higher than natural flyers, ranges between 76 and 251 N/kg. However, a small brushed motor (Solarbotics GM15) can only power the eight-wing flapper for 31-57 N/kg. In comparison, brushless motors can indirectly drive flapping wings for mass-specific thrust generation up to 47-143N/kg. Even a 1.7g brushless motor can drive flapping X-wings for 16 gram thrust generation.

Figure 2.25 (a) Muscle-specific maximum lift by natural flyers [9]⁴ compared to motor-specific maximum lift for man-made FWMAV and propeller. (b) Muscle-specific maximum lift of natural flyers compared to that of FWMAV, by taking FWMAV flight muscle as the motor mass and battery mass. As reference, we use following lightest battery mass for propeller, found in HobbyKing, according to voltage: 4S 500mAh 14.8V – 74g, 3S 180mAh 11.1V – 19.3g, 2S 180mAh 7.4V – 13.7g, 1S 200mAh 3.7V – 7g.

⁴ Values are derived from rounded data in Marden's appendix [13]. It yields statistical error in the muscle mass-specific lift. Refer text for the accurate values.

Chapter 3 Analytical Model of Flapping Flexible Wing with Passive Wing Rotation

This chapter introduces the development of a simple analytical model for passive wing rotation of a flexible flapping wing. Designers of FWMAV face problem in predicting lift generation and power requirements because wing shape and motion are a priori unknown. To date, most studies make use of known simplified wing kinematics from insects or experimental prototypes to predict lift and power requirements of flapping wing [2]. In another spectrum, engineers of untethered FWMAV system simply test various wing design to obtain the best performing flexible wings for their specific flying prototypes. [34, 38, 43] It should be critically noted that the wings used in untethered flying prototypes are significantly different in shape, size and reinforcement than those reported to be optimized in the ground experiments. This shows the gap between ground study models and actual flying prototypes because ground experiment discounts the need of lightweight wings and actuators to hover in air.

Here, the hovering flapping flight with flexible wing is first modeled with a simple reciprocating wing stroke with linear wing twist, in contrast to the conventional model of rigid wing rotation without twist. We also show that the wing passive rotation can be modeled as a pendulum excited by a base oscillation (the stroke of the leading edge) with a linear spring (which is due to foil tension) and linear structural damping. The amplitude and phase of the wing passive rotation changes with respect to the damping ratio and ratio of wingbeat frequency to natural frequency of the pitch oscillation. Experimental wing kinematic yields the phase and amplitude along frequency which can be compared to the model prediction to obtain the corresponding damping ratio and natural frequency. We also develop a novel analytical model to predict the lift and power requirement for the flapping wing transmission with elastic storage.

3.1 Kinematics of Flapping Flexible Wing

We consider a simple flexible wing in reciprocation with passive wing chord rotation and linear twist along the wing span in the X-wing mechanism that claps and flings at the horizontal plane as shown in Figure 3.1. The wings close together at the horizontal plane and open to a total wing stroke angle of 2Φ . The wing leading edge sweeps through the stroke plane while the wing chord rotates passively about the leading edge to a pitch angle $\beta(r, t)$.

Figure 3.1 Illustration for flapping wing kinematics: stroke and rotation. Blade element section view shows the aerodynamic forces are generated by translational and rotational circulations.

During wing reciprocation, the wing stroke angular position $\phi(t)$ follow a harmonic function of time:

$$\phi(t) = \Phi_m + \Phi \cos(2\pi f t + \pi) \quad (3-1)$$

where Φ_m is the mid stroke angle, Φ is the wing stroke amplitude (i.e. half the wing stroke angle). The maximum opening angle is $\Phi_m + \Phi$ while the minimum closing angle is $\Phi_m - \Phi$. By quasi-steady assumption and blade element theory, the air velocity relative to the wing blade element is the translation speed at that particular distance from the flapping axis, $r\dot{\phi}$.

Accompanying the wing stroke is a passive rotation of the flexible wing plane about the leading edge. The wing rotation $\beta(r, t)$ is a harmonic function of time:

$$\beta(r, t) = B_m + B(r) \cos(2\pi f t + \pi + \varphi) \quad (3-2)$$

with B_m and $B(r)$ being the mid pitch position and the pitch amplitude at the spanwise distance r , and φ the phase lead as induced by wing stroke reciprocation.

For this flexible wing, the amplitude of wing rotation varies linearly with the spanwise position r on the wing, following:

$$B(r) = B_t \left(\frac{r}{R} \right) \quad (3-3)$$

where B_t is the amplitude of wing rotation at the wing tip. Hence, the wing rotation amplitude is zero at the wing root ($r = 0$) due to the fixed root chord; whereas, it become maximum at the wing tip ($r = R$) due to large freedom of wing foil edges.

For this flexible wing, the angle of attack in degree is found to be:

$$\alpha(r) = 90^\circ - \text{sign}(\beta \cdot \dot{\phi})|\beta| \quad (3-4)$$

The pitch angle is a sensitive parameter in force generation [27, 63], because it determines the angle of attack and direction of the aerodynamic force due to translation. The pitch angle along the wing stroke can be parameterized with the phase lead and the mid-pitch angle. Appendix A shows three cases of wing kinematics demonstrating the effect of the phase lead and one case of a shifted mid pitch angle.

3.2 Passive Wing Rotation of Flexible Wing

It is unclear on how to engineer a flexible wing for passive wing rotation. Successful hovering FWMAV like Mentor, Nano Hummingbird and DelFly, simply tested various nature-inspired design of flexible wings reinforced with carbon fiber ribs to obtain the best performing wing with passive wing rotation. Inertia and aerodynamic forces are usually proposed as the cause of passive wing rotation for flexible wings [64, 65, 66]. The inertia and aerodynamic forces can act to bend insect wing into a cambered twist, as shown by Kang et al [2, 67]. These forces can also rotate Robobee's rigid wing about the wing leading edge which is pivoted by a bending flexure, as shown by Whitney et al [66].

However, Whitney et al [66] noted that the aerodynamic forces due to translation and rotation provide little damping for wing rotation. Without sufficient rotational damping, the model yields severely under-damped rotation in stark contrast to experiment results that yields overdamped characteristics. Over rotation of the underdamped wings risk wing flutter. For example, Figure 3.2(a) shows an unconstrained flexible wing in translation will flutter like a flag. At the beginning of the translation, aerodynamic and inertia forces rotate the wing chord to an acute angle of attack, or a large pitch angle. The only damping on this rotation is due to the rotational aerodynamic force which acts in opposite direction to the wing rotation speed. Considering that translational lift and drag are much higher than the weight of the wings and the rotational force, the sum of aerodynamic forces and wing momentum force would continue to flip the wing chord to negative angle of attack, if not limited by more damping and recoil forces. Consequently, a negative lift generated by the negative angle of attack rotates the chord back to positive angle of attack. This cycle of flipping angle of attack continues, resulting in wing flutter and the loss of steady lift.

Well-constrained flexible wings with clap and fling proved to generate high lift sufficient for takeoff and hover. The researchers of DelFly, FlowerFly and Hummingbird find it necessary to constrain their flexible wing to a fixed root chord and reinforced the wing with carbon ribs in order to generate high lift, but the reason is not well understood. We propose here that fixing

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the wing root chord give rise to foil tension (stiffness) and structural damping to the rotation of the flexible wing as shown in Figure 3.2(b). Recoil force from the tensioned foil limits the chord rotation hold an acute angle of attack along the wing stroke. As the wing sweeps the stroke, the aerodynamic forces twist the wing foil along span to a larger pitch angle and stretches the foil to higher tension. The higher tension would then balance the aerodynamic torque to limit the increase of pitch angle and prevents the wing from flutter. In addition, structural damping forces from the foil also limits the pitch amplitude and modifies the phase lead of the wing rotation with respect to the wing stroke.

(a) Free unrestrained flexible wing

(b) Flexible wing with fixed root chord

Figure 3.2 (a) A translating flexible wing flutters to produce zero net lift when it is unconstrained. (b) A constraint at the root chord to tension the wing foil limits the passive pitch angle.

Here, we propose a novel model of passive wing rotation as a harmonic response to the reciprocation of the wing leading edge. We consider normal hovering with wings reciprocating in horizontal stroke plane to be the simplest case. The wing foil is pivoted about the wing leading edge and is free to rotate about the leading edge. Other than the tension force along the chord, the foil tension also induces a torsional spring about the leading edge that tends to rotate the wing chord to a vertical manner like the fixed root chord. The gravity also acts as a restoring force acting behind the leading edge to return the chord to vertical attitude. The wing chord is assumed not to camber or bend because of foil tension and rib reinforcement, which is verified in our experiment later. In the absence of active pitch control, this model shows that as the wing

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leading edge moves, inertia causes the wing to rotate passively to a pitch angle limited by rotational stiffness and damping. The amplitude of the rotation increases to an asymptotic limit at wingbeat frequency much higher than the natural frequency.

For a simple analysis, a passively rotating flexible wing is modeled in Figure 3.3 as a wing pendulum rotating about a moving slider (leading edge) subjected to harmonic base excitation. The wing pendulum has a lumped mass m (includes wing mass and added mass) located at the center of rotation (r_m, l_m) in the spanwise and chordwise positions respectively. The chord with length, c , is a pendulum rod with uniform mass which rotates about the leading edge, but is now assumed to concentrate on point mass, m (moment of inertia $= mc^2/3 = ml_m^2$). Hence, the effective position of the point mass center from leading edge, l_m is $1/\sqrt{3}$ of the chord length.

Figure 3.3 Passive wing chord rotation during wing stroke, modeled as a pendulum excited by a reciprocating slider on the leading edge. For a small angle and quasi-static approximation, forces acting on the point mass m are the weight, tension action from leading edge along rod, the chord rotational recoil force and the internal damping of the foil. At small speed, non-linear aerodynamic forces can be taken out from this analysis to keep a solvable equation of motion.

The moving slider of negligible mass moves for $y = r_m \phi(t)$ following the leading edge at the spanwise location r_m . The lumped mass of wing pendulum, m moves horizontally for x_m , lagging behind the leading edge to yield a wing pitch angle, β . The pendulum is moved horizontally by horizontal component of the pendulum rod tension, $F_T \sin \beta$, the horizontal component of structural damping forces with damping coefficient c_{damp} , and the horizontal force component of the restoring torque due to chord rotation stiffness, $K_\beta \beta \cos \beta / l_m$. Translational aerodynamic forces tend to increase the wing rotation while the rotational aerodynamic force tends to damp the wing rotation. However, these non-linear aerodynamic forces are taken out from the analysis to yield an analytically solvable equation of motion.

The equation of motion for the pendulum point mass, m in the direction of x is derived to be (by Newton's Second Law, $F=ma$):

$$mg \tan \beta + c_{damp} l_m \dot{\beta} \cos \beta + \frac{K_\beta \beta}{l_m} \cos \beta = m \ddot{x} \quad (3-5)$$

Small angle approximation ($\sin \beta \approx \beta$, $\cos \beta \approx 1$, $\tan \beta \approx \beta$, $\beta \approx \frac{y-x}{l_m}$) simplifies the analysis to yield an analytically solvable equation as follows:

$$\ddot{x} + \frac{c_{damp}}{m} \dot{x} + \left(\frac{g}{l_m} + \frac{K_\beta}{m l_m^2} \right) x = \frac{c_{damp}}{m} \dot{y} + \left(\frac{g}{l_m} + \frac{K_\beta}{m l_m^2} \right) y \quad (3-6)$$

The equation of motion is compared to the linear spring-damper model to yield:

$$\ddot{x} + 2\zeta \omega_n \dot{x} + \omega_n^2 x = 2\zeta \omega_n \dot{y} + \omega_n^2 y \quad (3-7)$$

where the natural frequency is $\omega_n = \sqrt{\frac{g}{l_m} + \frac{K_\beta}{m l_m^2}}$ and the damping ratio is $\zeta = \frac{c_{damp}}{2m\omega_n}$.

Given the sinusoidal leading edge reciprocation: $y(t) = r_m \phi(t) = r_m \Phi_m + r_m \Phi \cos(\omega t + \pi)$ at frequency $f = \omega/2\pi$, the solution of the forced oscillation of mass m is given by:

$$x(t) = r_m \Phi X \cos(\omega t + \pi + \vartheta) + r_m \Phi_m \quad (3-8)$$

where $\vartheta = \tan^{-1} \left(\frac{-2\zeta \frac{\omega^3}{\omega_n^3}}{1 - \frac{\omega^2}{\omega_n^2} + \left(\frac{2\zeta \omega}{\omega_n} \right)^2} \right)$, $-\pi \leq \vartheta \leq \pi$, and $X = \sqrt{\frac{1 + \left(2\zeta \frac{\omega}{\omega_n} \right)^2}{\left(1 - \frac{\omega^2}{\omega_n^2} \right)^2 + \left(\frac{2\zeta \omega}{\omega_n} \right)^2}}$.

This yields the steady-state pitch angle oscillation:

$$\begin{aligned} \beta(r_m, t) &= \frac{y(t) - x(t)}{l_m} \\ &= B(r_m) \cos(\omega t + \pi + \varphi) \end{aligned} \quad (3-9)$$

where $B(r_m) = \frac{r_m \Phi}{l_m} \sqrt{1 + X^2 - 2X \cos \vartheta}$ and $\varphi = \tan^{-1} \left(\frac{-X \sin \vartheta}{1 - X \cos \vartheta} \right)$, $-\pi \leq \varphi \leq \pi$

As the driving frequency increases to infinity ($\omega \rightarrow \infty$), $X = 0$, $\varphi = 0$ and $B(r_m) = r_m \Phi / l_m$.

Without the continuous base excitation, the pendulum oscillates freely with decaying amplitude when released from an initial angle. This free damped oscillation can be modeled by the characteristic equation of the mass-spring-damper model:

$$\ddot{x} + 2\zeta \omega_n \dot{x} + \omega_n^2 x = 0 \quad (3-10)$$

to yield the free oscillation as:

$$x(t) = x(t=0) e^{-\zeta \omega_n t} \cos(\sqrt{1 - \zeta^2} \omega_n t) \quad (3-11)$$

given the initial displacement of the mass $x(t=0)$. For a fixed leading edge (take $y(t) = 0$), the pitch angle oscillates freely with an initial angle displacement $\beta(r_m, 0)$ according to:

$$\begin{aligned} \beta(r_m, t) &= \frac{y(t) - x(t)}{l_m} \\ &= \beta(r_m, 0) e^{-\zeta \omega_n t} \cos(\sqrt{1 - \zeta^2} \omega_n t) \end{aligned} \quad (3-12)$$

Figure 3.4 Free damped oscillation of linear-spring-mass-damper pendulum. The amplitude of oscillation decays exponentially to a decay constant $\zeta\omega_n$ and the pendulum oscillates at a damped frequency ω_d .

The pitch angle oscillates at the damped frequency $\omega_d = \omega_n \sqrt{1 - \zeta^2} = 1/T$, where T is the peak-to-peak period of the free oscillation. The natural frequency and damping ratio can be calculated from the amplitude decay constant $\zeta\omega_n$ and damped frequency ω_d as follow:

$$\omega_n = \sqrt{(\omega_d)^2 + (\zeta\omega_n)^2} \quad (3-13)$$

$$\zeta = \frac{(\zeta\omega_n)^2}{\sqrt{(\omega_d)^2 + (\zeta\omega_n)^2}} \quad (3-14)$$

This linear spring-mass-damper model does not consider the quadratic aerodynamic damping and therefore will yield error. However, we can calculate the amplitude and phase response of this systems for various damping ratio and compare to the experiment value to obtain the corresponding damping ratio for the correct amplitude and phase. Nevertheless, we will show in the following case study that this model can relate the physics of the passive wing rotation to measurable parameters of the wing, which is useful for designers of FWMAV.

3.3 Aerodynamic Lift and Power of X-wing with Clap and Fling

The aerodynamic forces are estimated based on quasi-steady analysis and complemented with lift enhancement from unsteady aerodynamic mechanisms such as wing rotation, clap and fling. The delayed stall and leading edge vortex effect are accounted by using the lift and drag coefficients obtained from experiments conducted by Dickinson [27]. The added mass of air is considered as inertia effect which is discuss in the next section. This section details the derivation of the lift and energetic cost arise from the speed and attitude of the wing.

A flexible wing twists to yield spanwise variation of angle of attack, so the aerodynamic force for each blade element varies spanwise. At the fixed wing root chord, the translational lift coefficient is zero but drag coefficient is maximum because the chord attitude is normal to the wing motion direction, i.e. 90° angle of attack. The lift coefficient increases spanwise while the drag coefficient decreases to the tip. This spanwise dependence is simplified using a linear interpolation function from the wing tip:

$$C_L(r, \alpha) = \frac{r}{R} C_L(\alpha_{tip}) \quad (3-15)$$

$$C_D(r, \alpha) = C_{D,max} - \frac{r}{R} \{C_{D,max} - C_D(\alpha_{tip})\} \quad (3-16)$$

where α_{tip} is the wing angle of attack at the wing tip. $C_L(\alpha_{tip})$ and $C_D(\alpha_{tip})$ are based on Dickinson's experimental results [27], given in equations (2-4) and (2-5).

The translational lift force $L_{trans}(t)$ acting on the flexible wing is a spanwise integral of elemental lift following:

$$\begin{aligned} L_{tran}(t) &= \int_{r=0}^{r=R} \frac{1}{2} \rho c |r \dot{\phi}|^2 C_L(r, \alpha) c dr \\ &= \frac{1}{8} \rho c R^3 |\dot{\phi}|^2 C_L(\alpha_{tip}) \end{aligned} \quad (3-17)$$

where $C_L(\alpha_{tip})$ is the lift coefficient at the wing tip angle of attack α_{tip} based on equation (2-4).

Similarly, the total translational drag is induced opposite to the direction of stroke speed. It is obtained as the integral below:

$$\begin{aligned} D_{tran}(t) &= \int_{r=0}^{r=R} \frac{1}{2} \rho c |r \dot{\phi}|^2 C_D(r, \alpha) c dr \\ &= \rho c R^3 |\dot{\phi}|^2 C_D(\alpha_{tip}) \left(\frac{1}{24} \frac{C_{D,max}}{C_D(\alpha_{tip})} + \frac{1}{8} \right) \end{aligned} \quad (3-18)$$

where $C_D(\alpha_{tip})$ is the drag coefficient at the wing tip angle of attack based on equation (2-5).

The torque due to drag is given by the integral of the cross product of radius and the drag:

$$\begin{aligned}
Q_{tran}(t) &= \int_{r=0}^{r=R} r \times \frac{1}{2} \rho c |r\dot{\phi}|^2 C_D(r, \alpha) c dr \\
&= \rho c R^4 |\dot{\phi}|^2 C_D(\alpha_{tip}) \left(\frac{1}{40} \frac{C_{D,max}}{C_D(\alpha_{tip})} + \frac{1}{10} \right)
\end{aligned} \tag{3-19}$$

The aerodynamic power P_{tran} to overcome the translational drag D_{tran} is the summed contributions of all blade elements:

$$\begin{aligned}
P_{tran}(t) &= \int_{r=0}^{r=R} |\dot{\phi}| r \frac{1}{2} \rho c |r\dot{\phi}|^2 C_D(r, \alpha) c dr \\
&= \rho c R^4 |\dot{\phi}|^3 C_D(\alpha_{tip}) \left(\frac{1}{40} \frac{C_{D,max}}{C_D(\alpha_{tip})} + \frac{1}{10} \right)
\end{aligned} \tag{3-20}$$

Following Maxworthy's [29] analysis for wing flinging, a quadratic interpolation function is used to account for the spanwise variation of rotational circulation.

$$\Gamma(r) = C_{rot} c^2 \dot{\alpha}_{tip} (r/R)^2 \tag{3-21}$$

where $C_{rot} = \pi(0.75 - \hat{x}_0)$ is the rotational coefficient and $\hat{x}_0$ is the normalized distance of the axis of rotation from the leading edge along the chord, which is zero if the wings supinate and pronate about the leading edge.

The rotational force F_{rot} , which acts normal to the wing surface is obtained from the integral of the circulation developed:

$$\begin{aligned}
F_{rot}(t) &= \int_{r=0}^{r=R} \rho |r\dot{\phi}| c^2 \dot{\alpha}_{tip} C_{rot} \left(\frac{r}{R} \right)^2 dr \\
&= \frac{1}{4} \rho c^2 R^2 |\dot{\phi}| \dot{\alpha}_{tip} C_{rot}
\end{aligned} \tag{3-22}$$

with $\hat{x}_0 = 0$ for this flexible wing design. This is generally true for isolated rotation of the wing without any other wing interaction. However, when the wings clap and fling, the rotation axis is shifted to the trailing edge, i.e. $\hat{x}_0 = 1$, because the trailing edges remained in contact while the leading edges separates from each other.

This rotational force center locates at $\int r dF_{rot} / F_{rot} = 0.8R$ spanwise, of which its corresponding angle of attack is $\alpha(0.8R) = 90^\circ - \text{sign}(\beta \cdot \dot{\phi}) |0.8\beta_t|$. This force yields a rotational lift component $L_{rot} = F_{rot} \cos(\alpha_{0.8R})$ and a rotational drag component $D_{rot} = F_{rot} \sin(\alpha_{0.8R})$.

Similarly, the aerodynamic power P_{rot} to overcome the rotational drag D_{rot} is derived to be:

$$\begin{aligned}
P_{rot}(t) &= \int_{r=0}^{r=R} |r\dot{\phi}| dF_{rot} \sin(\alpha_{0.8R}) \\
&= \frac{1}{5} \rho c^2 R^3 |\dot{\phi}|^2 \dot{\alpha}_{tip} C_{rot} \sin(\alpha_{0.8R})
\end{aligned} \tag{3-23}$$

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An impulse is produced during this fling period t_{fling} , where the wings fling open to the angle just before the wings' trailing edges separate:

$$I_{fling} = \rho \int_{r=0}^{r=R} \Gamma_r s_r dr \text{ for } t < t_{fling} \quad (3-24)$$

where $\Gamma_r = \dot{\beta} c^2 = \frac{r}{R} \dot{\beta}_{tip} c^2$ is the circulation at r distance away from the root due to wing opening, while $l_r = \frac{r}{R} l_{tip}$ is the distance between the two leading edge vortices. l_{tip} is taken to be $\frac{5}{4}c$ for an opening angle where $\beta_{tip} = 60^\circ$ from the experimental values at $Re=13000$ in [29].

For this clap-and-fling X-wing with a small stroke, the fling period ends when the trailing edges separate right after pitch angle reaches its maximum, hence is dependent on the phase lead of the rotation: $t_{fling}/T = (0.5 - \varphi/2\pi)$ where T is the wingbeat cycle period. It is implicit that the pitch angle cannot be positive at the start of the stroke for a pair of clapping wings which are parallel and immediately close to each other because it will mean two wings rotate pass into each other. A symmetric chord rotation gives the fling to end at a quarter of the cycle: $t_{fling} = T/4 = 1/4f$. The mean force is given by the impulse over the time:

$$\begin{aligned} \bar{F}_{fling} &= \frac{I_{fling}}{t_{fling}} \\ &= \frac{1}{t_{fling}} \int_{r=0}^{r=R} \frac{5}{4} \rho \dot{\beta}_{tip} c^3 \left(\frac{r}{R}\right)^2 dr \\ &= \frac{1}{t_{fling}} \frac{5}{12} \rho \dot{\beta}_{tip} c^3 R \end{aligned} \quad (3-25)$$

The clap of the wings is modeled by the reverse process of the fling to yield the same impulse.

Total lift force generation at time t by each wing is contributed by wing translation and rotation:

$$\begin{aligned} L_{total}(t) &= L_{trans}(t) + L_{rot}(t) + F_{fling}(t) + L_{added\ mass}(t) \\ &= \frac{1}{8} \rho c R^3 |\dot{\phi}|^2 C_L(\alpha_{tip}) + \frac{1}{4} \rho c^2 R^2 |\dot{\phi}| \dot{\alpha}_{tip} C_{rot} \cos(\alpha_{0.8R}) + \frac{5}{3} f \rho \dot{\beta}_{tip} c^3 R \end{aligned} \quad (3-26)$$

There is no lift force due to added mass $L_{added\ mass}(t)$ as we assumed that the wings only accelerate horizontally without vertical acceleration.

Hence, the total aerodynamic power is a sum of the power components following:

$$\begin{aligned} P_{aero}(t) &= |P_{tran}(t) + P_{rot}(t)| \\ &= \left| \rho c R^4 |\dot{\phi}|^3 C_{D,max} \left(\frac{1}{40} + \frac{1}{10} \frac{C_D(\alpha_{tip})}{C_{D,max}} \right) + \frac{1}{5} \rho c^2 R^3 |\dot{\phi}|^2 \dot{\alpha}_{tip} C_{rot} \sin(\alpha_{0.8R}) \right| \end{aligned} \quad (3-27)$$

The lift or thrust-to-aerodynamic power ratio is therefore

$$T/P_{aero} = \frac{\frac{1}{8} \rho c R^3 |\dot{\phi}|^2 C_L(\alpha_{tip}) + \frac{1}{4} \rho c^2 R^2 |\dot{\phi}| \dot{\alpha}_{tip} C_{rot} \cos(\alpha_{0.8R}) + \frac{5}{3} f \rho \dot{\beta}_{tip} c^3 R}{\left| \rho c R^4 |\dot{\phi}|^3 C_{D,max} \left(\frac{1}{40} + \frac{1}{10} \frac{C_D(\alpha_{tip})}{C_{D,max}} \right) + \frac{1}{5} \rho c^2 R^3 |\dot{\phi}|^2 \dot{\alpha}_{tip} C_{rot} \sin(\alpha_{0.8R}) \right|} \quad (3-28)$$

The lift force is proportional to the second order of speed ($R^2|\dot{\phi}|^2$ and $R^2|\dot{\phi}|\dot{\alpha}$). Whereas the aerodynamic power is proportional to the third order of speed ($R^3|\dot{\phi}|^3$ and $R^3|\dot{\phi}|^2\dot{\alpha}$). Like the propeller (See Appendix B, equation (B-3)), the flapping wing's ratio of thrust-to-aerodynamic power is inversely proportional to tip speed. The additional rotational lift allows flapping wing to generate more thrust at lower wing tip speed. So flapping wing gain better efficiency because it can generate same thrust to hover at lower tip speed than propeller.

3.4 Energetic Cost of Flapping Wing Mechanism with Elastic Storage

Figure 3.5 Motor expends power to accelerate the wing inertia against aerodynamic drag and elastic recoil.

A motor-driven FWMAV expends substantial power to move its wings through the air as shown in Figure 3.5. The basic energetic cost for flapping flight is a sum of the aerodynamic power P_{aero} to move the air (See section 2.3) and the inertial power P_{acc} to accelerate and decelerate the wings. To recover wing's inertial power, elastic storage is added to yield the elastic power ($P_{elastic}$). Ignoring power loss by electric conversion and mechanical transmission, the minimum mechanical power P_{mech} for flapping flight is:

$$P_{mech} = P_{aero} + P_{acc} + P_{elastic} \quad (3-29)$$

Consider a rectangular wing with a moment of inertia J_0 . An inertial torque $J_0\ddot{\theta}$ is required to move the wing at an angular acceleration of $\ddot{\theta}$ in the stroke plane about the flapping axis. The wing moves together with the air surrounding it, hence the inertia of the wing, J_0 is increased by the mass of air that is accelerated, i.e. a virtual mass or added mass, J_{added} . The total inertial power is given by the product of inertial torque and the wing stroke speed $\dot{\phi}$:

$$P_{acc} = Q_{acc}\dot{\phi} = (J_0 + J_{added})\ddot{\phi}\dot{\phi} \quad (3-30)$$

The added mass is relatively small and negligible in fixed wing, because the wing attacks air at a small incidence angle and the rigid wing is much more massive. However, this added mass is

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large for a thin flexible wing moving at a large angle of attack and a long chord. The moment of inertia due to added mass is given by the integral along the span:

$$J_{added} = \int_0^R r^2 dm_{added} \quad (3-31)$$

For simplicity, the added mass is estimated using a constant angle of attack at the mid-span, yielding $J_{added} = \frac{1}{12} \rho \pi \left(\frac{c}{2} \sin \alpha_m \right)^2 R^3$.

Elastic storage can recover the wing kinetic energy lost in deceleration. Consider a torsional spring with a rotational spring constant K to provide a means of storing elastic energy to reduce inertial power. The elastic torque required to deform the spring to a wing angular position is:

$$Q_{elastic} = K(\phi - \Phi_0) \quad (3-32)$$

where Φ_0 is the load-free neutral position of the spring with a constant stiffness. Statically ($\dot{\phi}=0$), the motor is loaded by the elastic torque away from neutral position. The elastic power required to deform the spring is a product of the spring torque and the wing angular speed about flapping axis:

$$P_{elastic} = K(\phi - \Phi_0)\dot{\phi} \quad (3-33)$$

The sum of elastic power and inertial power is therefore:

$$P_{acc} + P_{elastic} = [J_0 + J_{added}]\ddot{\phi} + K\dot{\phi} - K\Phi_0\dot{\phi} \quad (3-34)$$

At the start of the wing reciprocation, a larger torque is required to move the wings against the spring and inertia. During reciprocation, the wings acceleration is in opposite direction of the spring force (second order time derivative of the stroke is in negative direction to stroke), therefore reduced by the spring torque.

The springs are usually designed for a sinusoidal time profile of stroke. The neutral position of the spring is at the midstroke, and that spring is deformed towards either end of stroke. The elastic torque helps to decelerate the wing inertia at the end of stroke, and accelerate the wing reversal, i.e. the elastic torque is in antiphase with inertial torque. The elastic power could cancel inertial power, near the resonant flapping frequency, $f_n = (1/2\pi)\sqrt{K/J_0}$. Above the optimum frequency, the maximum wing kinetic energy exceeds the total elastic energy storage. The maximum kinetic energy increases with inertia and the maximum stroke speed ($\dot{\phi}_{max} = 2\pi f\Phi$):

$$KE_{max} = 2J_0 \pi^2 f^2 \Phi^2 \quad (3-35)$$

By this equation, we see that a larger wing, or a larger wing stroke, or a faster wingbeat will cost more inertial power. An appropriate spring stiffness has to be chosen to match the kinetic energy according to these desired operating wing kinematics, without impeding a small motor from starting.

3.5 Case Study: 280 mm span 88mm long chord Polyurethane (PU) Film Wing

Here, we study a bird-sized X-wing FWMAV, which is the experimental prototype from Chapter 9. Figure 3.6 shows the dorsal view of an overlapping pair of transparent PU-film wing plane of the closed X-wing. Each single wing (half of a wing plane) extends $R = 0.14$ m in length and $c = 0.088$ m at the root chord. This ultra-lightweight single wing inertia is calculated to be $7.2832 \times 10^{-6} \text{ kgm}^2$ from the measured mass of the respective carbon rods, adhesive tapes and foil at their respective placing with the aid of SolidWorks CAD. The mass of the single wing, including the leading-edge spar and rib reinforcement, is measured to be 1.015 g. The added mass of air to this wing is calculated from the integration of equation (2-6) to be 1.178 g, assuming an angle of attack of 90° (an overestimate). The leading edge of the wing is designed to sweep from 0° at the symmetry plane to $2\Phi = 50^\circ$ stroke angle (experiment may yield deviation due to free play in transmission), while the root chord is fixed at pitch angle of 0° . A pair of spring hinges supports the wing and helps to recover the wing kinetic energy in the stroke plane, with its elastic stiffness measured at $K=12.972 \text{ mN.m/rad}$ and no-load angle at $\Phi_0 = 5^\circ$. This spring hinge is stretched as the wings fling open. When the wings reverse stroke to close together, the spring hinge recoils to assist the closing stroke.

Figure 3.6 Wing design of the prototype from Chapter 9 used for this case study.

The wing chord rotates passively upon a torque action about the leading edge. Figure 3.7 (a) showed that the mid-rib chord ($r_m = 0.065\text{m}$) rotates to a larger pitch angle when its trailing edge is loaded with more weight. The mid-rib chord can support a load of 12.22 g at its trailing edge at the pitch angle of 26.75° . The torque M about the leading edge increases with the pitch angle following $M = 212.42\beta^3 - 66.25\beta^2 + 5.12\beta$ (mN.m). To simplify our analysis, the chord rotation

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stiffness is approximated with a linearized torsional stiffness $K_\beta = 34.12 \text{ mN.m/rad}$ from 9.74° to 26.75° , which yields a natural frequency of 4.3 Hz calculated from Eqn (3-7).

Figure 3.7 (b) shows two free damped oscillations: first at larger initial pitch angle of $\beta(t=0) = 24.35^\circ$ and second at smaller initial pitch angle of $\beta(t=0) = 8.62^\circ$. Both free damped oscillations showed a consistent damped frequency at about 4.4 Hz regardless of the starting pitch angle. However, the damping ratio is larger at $\zeta = 0.2225$ for larger initial pitch angle of $\beta(t=0) = 24.35^\circ$ compared to $\zeta = 0.1383$ for smaller initial pitch angle of $\beta(t=0) = 8.62^\circ$. Despite the different damping ratio, the two free oscillations yield similar natural frequency ω_n at about 4.4 Hz, close to the calculated natural frequency. The higher damping is mainly attributed to viscoelastic polyurethane film wing being strained at higher rate at higher pitch amplitude, whereas effects of aerodynamic forces are minimal here because the wing is not translating. Kanyanta et al [68] showed faster strain rate caused increased internal stress in polyurethane rubber.

(a) Mid rib chord rotation stiffness

(b) Free damped oscillation of mid rib chord

Figure 3.7 (a) Incremental weight loading on the trailing edge rotates the mid-rib chord to larger pitch angle. (b) Free damped oscillation of the mid-rib chord with a large initial pitch angle $\beta(t=0) = 24.35^\circ$ (Exp 1) and a small initial pitch angle $\beta(t=0) = 8.62^\circ$ (Exp 2). Snapshots of the free oscillation are shown for Exp 2.

When the wing is reciprocated continuously, the damping increases due to the additional aerodynamic forces and higher rotation speed than the statically determined. Our model calculates the wing kinematics from the frequency response at different damping ratio and compares to experimental results from Chapter 9 in Figure 3.8.

Figure 3.8 Wing kinematics of an X-wing with reinforced PU-film wing (from Chapter 9). (a) Snapshots of wing opening stroke at 15 Hz wingbeat, viewing from the top and the side of the X-wing. (b) The mid-rib chord attitude along opening and closing strokes with time interval of $0.05 T$. Not to scale. (c) Comparison between experimental and sinusoidal stroke angles at 15 Hz (d) Experimental pitch angles and calculated pitch angle of the mid-rib from equations (3-1) and (3-9) with wing stroke and damping factor: $2\Phi = 46^\circ$, $\zeta=1.35$ at 15 Hz. (e) Amplitude of the mid-rib chord rotation along frequency matches the model at damping factor of 1.35. (f) Phase lead of mid-rib chord rotation matches model prediction at damping factor of 1.35 with an offset of 0.20π rad. Experiment values are denoted by cross marks.

The experiment wing kinematics can be sufficiently represented by sinusoidal model, for example, the wing stroke and pitch cycle at 15 Hz. The wing reciprocated a stroke of $2\Phi=46^\circ$ and the wing reversal starts at 0.541 of the wingbeat period T , while the mid-rib chord oscillated with an amplitude of 22.32° , swinging between pitch angles of 24.96° and -19.69° . The opening

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stroke began with a fling, where the leading edges of the dorsal and ventral wings separated from each other while their trailing edges maintain partial contact (wing pronation). The wings pronate to a large pitch angle 23.70° at the end of the fling ($t/T=0.280$), and then completely separate from each other. The pronation ends at the peak pitch angle at $t/T=0.308$, leading the stroke peak by a phase of 0.47π rad. Thereafter, the wings supinate until the end of the opening stroke ($t/T=0.556$) to near-zero pitch angle. As the wing stroke reverses into closing stroke, the wing continues to supinate to a peak pitch angle at the middle of the closing stroke, and then pronates to a near-zero pitch angle at the end of the closing stroke. Such wing rotation is symmetrical, i.e. the stroke starts and ends with a zero/near-zero pitch angle, following Kang's definition [2]. With the same stroke at 15 Hz, our passive rotation model predicted similar pitch amplitude of 23.34° for a damping ratio of 1.35, but a different phase lead at 0.231π rad, which represents a delayed rotation. A phase offset of 0.20π rad shifts the wing rotation model closer to that of the experiment.

Overall, the experimental amplitude matched closely to the model-predicted amplitude and phase trend along wingbeat frequency with damping ratio of 1.35. However, the experiment phase lead is consistently higher by about 0.20π rad than the model prediction along frequency. Experimental results showed that the wing rotation changes from advanced rotation to symmetrical rotation when the wingbeat frequency increases from 5.5 Hz to 15 Hz with the phase lead reducing from 0.80π rad to about 0.47π rad. Whereas, the model predicted that the wing rotation transits from advanced rotation before 4.4 Hz to delayed rotation after 4.4 Hz. Such transition from advanced to delayed rotation when frequency increases was also shown by Kang et al. [2] by computationally solved Navier-Stokes equation for a flapping two-wing without clap and fling. Hence, difference of the phase lead can be attributed to the clap and fling effect not accounted in our model. The impulse created by fling during the opening stroke pronates the wing early in the opening stroke, thereby shifting the phase of the pitch angle.

Based on the sinusoidal model, fling period, wing pitch rotation amplitude and phase at 15Hz, we calculate the lift and energetic costs for wingbeat frequency up to 20Hz, using the models discussed in Section 3.2 and 3.2. Figure 3.9 (a, d) show the lift history at 6 Hz and 15 Hz wingbeat cycle respectively. The translational lift is sinusoidal at twice the wingbeat frequency, peaking at the middle of each stroke, following the change in stroke velocity. However, the rotational lift is asymmetrical due to the effect of the clap and fling. In the opening stroke, the lift starts high with impulse generated by the fling. The fling induced a positive rotational circulation during pronation, resulting in a positive rotational lift. The closing stroke starts with a negative rotational lift instead because the wing supination is isolated without fling. Near the end of the closing stroke, the wing claps with the opposing wing to produce an

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impulsive lift, leading to a peak lift. As result, the total lift peaked twice in the opening stroke, but only once at a higher value in the closing stroke.

Figure 3.9 (b, c, e, f) shows the power components and total energetic costs with and without the elastic storage with hinge stiffness K and load free angle Φ_0 at 5° in a wingbeat cycle at 6Hz and 15Hz. We also calculated the power for a stiffer elastic storage with $12K$ stiffness and $\Phi_0 = 25^\circ$ of which would have ideal elastic power matching the inertial power at 15 Hz. The aerodynamic power expenditure follows the lift generation due to translation and rotation to peak twice in a wingbeat cycle. Wing inertia power is also as significant as the aerodynamic power. The inertial power follows sinusoidal profile of wing acceleration to oscillate twice in a wingbeat cycle. The elastic power varies sinusoidal but in opposite phase of the inertial power, thereby reduces the total power required. With stiffness of K , the elastic storage can save significant power at 6 Hz, but the power saving is less at 15 Hz. With higher stiffness of $12K$, the elastic storage in fact increased the power requirement at 6 Hz greatly, but reduce the power significantly at 15 Hz.

Figure 3.9 (h) shows that the maximal lift generation increases with wingbeat frequency. The lift is mainly contributed by the translational lift, followed by the clap and fling and lastly the rotational lift. Combined unsteady effects from the rotation and clap and fling contributed to more than half of lift generation.

Figure 3.9 (i) shows the mean power required to flap the X-wing along wingbeat frequency. The aerodynamic power and the inertial power increases to the third order of wingbeat frequency. Elastic power only increases linearly with higher frequency while the inertial power increases to the third order of the wingbeat frequency, the recovery of inertial power by elastic storage diminishes at higher frequency. At frequency beyond 6 Hz, the elastic storage at current hinge stiffness of K is sufficient to save the inertial power. At high frequency of 15Hz, the elastic power is relatively small to recover the inertial power. It is shown that the stiffness of the elastic storage needs to be increased by 12 times in order to match the inertial power at high frequency. However, such high stiffness results in much higher power consumed at low wingbeat frequency.

Figure 3.9 (j)-(k) shows the lift-to-power ratio along wingbeat frequency and lift-to-power ratio along the mean lift. The lift-to-power efficiency is found to reduce with higher wingbeat frequency or higher mean lift. Higher lift-to-power can be achieved by the current elastic storage with stiffness K when the elastic power is matched to the inertial power.

Figure 3.9 Calculated time variations of lift components, mechanical power components, and mechanical power requirement without and with elastic storage at 6 Hz (a-c) and 15 Hz (d-f) wingbeat frequency. Legends: L_{trans} - translational lift, L_{rot} - rotational lift, L_{fling} - lift to clap and fling, P_{aero} - aerodynamic power, P_{acc} - inertial power, P_{elas} - elastic power. Mean of (h) lift components, (i) mechanical power required, (j) ratio of lift to mechanical power required along wingbeat frequency. (k) Ratio of lift to mechanical power to lift generation.

Chapter 4 Mechanism Design for Hovering Flapping-wing Flight

Flapping-wing flight is energetically costlier than rotating flight because of the loss of wing kinetic energy. This inertial energetic cost increases for larger flyer because the wing inertia increases with larger size. Small insects have elastic flight apparatus to recover the wing's inertial power, but it is not clear how larger FWMAV can integrate elastic storage to save power. Therefore, this chapter discuss the design of motorized-FWMAV with elastic flapping mechanism, scaling from a 100m-span to 280mm-span.

Firstly, we design a small moth-size flapper with a pair of 50-60mm-long wings in attempt to investigate insect-scale flapping flight. The wings are made rigid like insects with relatively high moment of inertia and sweep through large wing stroke. Elastic storage is designed for the recovery of the wing kinetic energy. Secondly, we scale up to larger X-wing flapper at 240-280mm span. The wings are made ultralight and flexible, having a relatively low inertia compared to aerodynamic forces.

Figure 4.1 Design of elastic mechanism for a 100mm-span two wing to 280mm-span four wing flapper.

4.1 Flexure Hinges for Elastic Storage

Previous spring designs (See Section 2.4) were either too big to be fit into limited space of the flapping wing mechanism. A short leaf spring or a film hinge is compact enough to replace the revolute joint the motorized-wing transmission. These flexures are frictionless and can act as spring to store energy. However, the challenge is to design a compact and stiff hinge to store energy but does not fail at large bending.

Figure 4.2 Hinge bending and the fracture by bending and twist.

Elastic energy stored in a film hinge, $W_E = \frac{1}{2} K \beta_h^2$ is proportional to the stiffness K and the hinge bending angle β_h . The hinge torsional stiffness, K is linearly proportional to the Young's modulus E of the film material, the second order moment I of the cross-sectional area, and inversely proportional to the hinge length l , following [69]:

$$K = \frac{EI}{l} = \frac{Ebh^3}{12l} \quad (4-1)$$

where l , b , and h are the original length, width and thickness of the unbent hinge respectively.

The compliant hinges fail when the hinge material yields to fracture during bending and twisting as shown in Figure 4.2. In bending, the outermost (convex) surface of the hinge from the neutral axis is tensioned highest, while the innermost (concave) surface is compressed. Assuming the hinge bends in a circular arc (no axial stress/strain) and the neutral axis is in middle of the hinge, the strain and stress at the outer most edge is given by:

$$\epsilon_{outer} = \frac{\beta_h h}{2l}, \sigma_{outer} = \frac{E \beta_h h}{2l} \quad (4-2)$$

To be safe, the maximum bending angle is limited at yield point of the outer edge:

$$\beta_{h,max} = \frac{2l}{h} \epsilon_{yield} \text{ or } \beta_{h,max} = \frac{2l}{Eh} \sigma_{yield} \quad (4-3)$$

Hinge fractures beyond the ultimate strain ϵ_{break} at the ultimate tensile strength, σ_{break} .

The hinge could still fail under twisting even though it is not bent beyond the maximum limit. During flapping, the wing forces far behind the hinges coupled with the crank force in

front to twist the wing hinge. Twisting induces high shear stress at the edge of the hinge, causing it to tear. A wider or thicker hinge could increase torsional stiffness to counter twist.

Fatigue failure of compliant hinges is one of the main reasons that no free-flying FWMAVs has made use of compliant mechanism to recover wing kinetic energy in flapping flight. The Harvard group is one of the pioneer of compliant flapping mechanism. Common available compliant mechanism is fragile and short-lived, e.g. the RoboBee survived only a maximum lifespan of 5mins at a wing beat of 120 Hz (about 36000 cycles), and often fails within much shorter time (Dr. Teoh Zhi Ern, Q&A session during seminar in NTU). The failure occurred in either the polyimide film hinge in the flapping mechanism or the piezoelectric actuator. This is despite the micrometer-precise MEMS pop-up manufacturing process.

Table 2 Properties of the plastic materials considered for elastic hinge

Material	Density (g/cm ³)	Tensile Modulus (GPa)	Yield Strain (%)	Tensile Strength, Yield (MPa)	Elongation @ Break (%)	Ultimate Tensile Strength (MPa)	Available thickness
Polyimide (PI) [70]	1.42	2.5	3	69	72	231	0.127mm, 0.250mm
Acetal/Delrin (POM) [71]	1.41	2.8	20	-	50	66	≥3.2mm
Nylon 6 (PA 6) [72]	1.15	2.76	>20	80.0	>20	82.7	≥3mm
PEEK	1.26	3.70	-	-	50	70-100	3.2mm
Stainless Steel	8	200	0.1	215	70	505	25.4μm, 50.8μm

Table 2 shows the available materials considered for the elastic hinges. Stainless steel, being too stiff, had to be very long to bend a large angle without breaking, hence does not fit into our application. The PI film is tough but with a lower tensile modulus compared to Delrin. Delrin has a higher yield strain but low ultimate tensile strength, so it can operate a larger elastic range without plastic deformation but breaks at smaller bend angle. Nylon is the best choice so far because it is most durable and has relatively high tensile modulus with lower density. This is supported by fact that Nylon is widely used for high-tension application such as guitar strings and fishing lines.

Available material thickness is also critical in the design of film hinges for different size FWMAV. The PI film hinge is very suitable for application of the 100-140mm-span FWMAV. When sized up for 240mm-span FWMAV, the PI hinge had to increase in width to 20mm because the PI film thickness is limited at 0.127mm. Such hinges size become obstructive. Hinge stiffness increases to the third order of thickness but only proportional to the width. With 3mm thick nylon sheets, we can mill thicker Nylon hinges but at smaller 3mm-width by precise computer-numerical-controlled (CNC) milling. Also, the hinge can be made as a monolithic structure together with the wing base linkage and the body frame. However, the CNC machine has limited cutting precision at 0.05 mm, hence not able to make micro-size hinges for insect-size FWMAV.

4.2 Proposed Spring Designs

(a) Single-Spring Design

(b) Two-Spring Design

(c) Three-Spring Design

(d) Spring Stiffness

Figure 4.3 Three spring designs proposed for FWMAV. (a) A single spring design with thick hinge for 240mm and 280mm-span four-wing FWMAV. (b) Two-spring design for a 140mm-span large stroke two-wing FWMAV. (c) Three-spring design for a 100mm-span two-wing FWMAV. (d) Elastic torque acting on the wing as a result the wing hinge deformation, along the wing's stroke angle.

Commonly proposed design for FWMAV are of single spring designs attached to wing base. Some of the springs are external to the flapping mechanism and grounded to a platform. These designs are only experimental effort and not ready for flight. However, springs internal to the flapping mechanism are feasible for flight, like insect's thorax, of which is a deformable shell formed by stiff plates connected with elastic joints. In the Robobee Nano Air Vehicle, thin but tough polyimide film hinges are used to replicate such elastic joints. Inspired by the insects and the Robobee, we propose three spring designs for FWMAV made of such flexure hinges as shown in Figure 4.3.

4.3 Single-spring hinge for bird-sized (240mm and 280mm-span) Four-wing Flapper

A flexure itself is the simplest spring element. A single-spring mechanism is implemented into the flapping wing mechanism of 240mm-280mm-span four-wing FWMAV. This four-wing FWMAV is a wing clapper with two diagonal wing planes which crisscross like an X, inspired by the Luna [73] and DelFly [37] ornithopters. At the center of the wing clapper is a symmetric T-shape elastic mechanism. Each symmetric half consists of an elastic hinge joined to a horizontal wing base. Each wing base holds a wing plane at its center. Rotating the wing base downwards fling opens the two crisscrossing wing planes like an 'X'. A torque is required to bend the hinges as the wing base rotate downward. However, in the reverse wing base rotation, the hinge recoils to close the wing planes towards the horizon and clap them together. Hence, the elastic mechanism assists the clapping of the X-wing. This spring design supports flapping large wings with small stroke angle. We developed a pair of PI hinges for a 240mm-span X-wing flapping at 22Hz over 40° wing stroke, and a pair of Nylon hinges for a 280mm-span X-wing flapping at 15Hz over 48° wing stroke. Further details of these X-wing mechanism are discussed in Chapter 8 and Chapter 9.

4.4 Two-spring Bistable Thoracic Mechanism for Large Wing Stroke 120mm-span Two-wing Flapper

We design a two-spring thoracic mechanism with a large wing stroke of 110° for 120mm-span two wing flapper, inspired after the click mechanism of Dipteran insects [60]. This is a bistable mechanism, where the elastic recoil tends to move the wings to either two stable wing positions, which are the wing down 'Λ' and wing up 'V' attitude. Basically, the thoracic mechanism is a narrow symmetrical thorax shell with two plates and two hinges in each symmetrical half. The pleural process plates are moved closer to each other such that the thorax width is shorter than the wing base length. The two wing-base plates are joined together by a revolute tergal joint at the symmetry, such that geometrically they form a 'Λ' triangular shape.

Depression of the tergal joint rotates the wings upward to form the 'V' shape. A force is required initially to depress the tergal until the midstroke, but after that the thoracic mechanism snaps the tergal joint by itself to the bottom position. Conversely, a force is required initially to lift the tergal joint, but after the midstroke the thorax snaps the tergal joint to highest position and rest there. This bistable mechanism stores up 1.43mJ in the first half stroke and is used to flap a pair of 60mm-long wings (root chord about 21mm long, each with a rotational inertia of $244 \times 10^{-9} \text{ kg.m}^2$) at 17Hz over 110° wing stroke.

4.5 Three-spring Thoracic Mechanism for 100mm-span Two-wing Flapper

Lastly, we develop a three-spring insect-like thoracic mechanism for 100mm-span two-wing flapper (see Figure 4.3c). It is a symmetric shell which has three rigid plates and three PI film hinges for each symmetric half. A wing base plate is elastically joined between a horizontal tergal plate and a vertical pleural process plate, while the bottom of the pleural process plate is anchored to a belly plate which houses the motor. The wings naturally rest at the horizontal attitude. Depression of the tergal plate rotates the wing upwards and vice versa. The force required to depress the plate increases non-linearly towards the extreme wing position. Such non-linear stiffness is due to the elastic hinge deformations. When depressed to a 33.2° wing stroke amplitude, this thoracic mechanism can store up to 0.4937mJ for the recoil of wing. It is used to flap a pair of wings (each being 50mm span-long and 31mm root chord length, with rotational inertia of $7.21 \times 10^{-8} \text{ kgm}^2$) at 33Hz over 79° wing stroke angle.

Chapter 5 Experimental Testing Method

Various experimental setups were developed along this course of study to characterize the different flapping wing mechanisms. The experimental setups help to quantify the following performance of the flapping wing prototypes: 1) Elastic properties of the hinges; 2) Wing kinematics; 3) Wing lift and torque; and 4) Power consumption. The greatest challenge lies the force measurements of the flapping mechanisms. Different force range and sensitivity are required between of a small 100mm-span two-wing, a large 240mm-span four-wing, and a larger 280mm-span four-wing FWMAVs and propeller.

5.1 Pendulum Method to Measure Average Thrust Generation (<5g)

Figure 5.1 Tethered flight test using a simple pendulum: (a) Gravity pulls the pendulum downward with idle flapper; thrust generated by the wing flapper's wingbeat at 33 Hz, lifts the pendulum above the horizon. (b) Schematic illustrating the balance between gravitational forces of the weight components and the thrust generation at equilibrium angles.

Small thrust generation can be measured using a simple pendulum method as shown in Figure 5.1(a). A pendulum naturally stays at the lowest position due to gravity pull. A flapper is attached to the pendulum such that thrust generated is perpendicular to the pendulum. When the flapper is powered, it generates a mean thrust and effect a torque that lifts and holds the pendulum against gravity to a quasi-static angle γ from the vertical line in quasi-static equilibrium, as shown in Figure 5.1(b). A simple moment of equilibrium would yield the thrust, T in relation to the gravitational forces acting on the mass components along the pendulum:

$$T = \frac{\sum m_i g l_i \sin \gamma}{l_T} \quad (5-1)$$

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where l_i and m_i are the position of the center of gravity and the component mass of the pendulum respectively, and the mean thrust center, l_T had to be known or estimated reasonably.

The components of the pendulum are as follows: a) The pendulum rod is a 7cm long ($l_{cg,rod} = 0.35cm$) carbon spar (0.465g) is pivoted freely to the center of a 360° protractor. b) A balsa wood (0.08g) attachment platform of which center is glued to the end of the pendulum rod. c) The prototype weight is at the end of the pendulum arm, with center of mass at the pendulum end. The average thrust is estimated to act at the wing midstroke, which is about 2.5cm away from the pendulum rod end. Because the pendulum is very light, it is very sensitive to small forces in the range of sub-gram, even to the weight and bending forces of the motor lead wires. Care must be taken to ensure the wire is pivoted around the pendulum center, and not dangling around. Based on these values with prototype weight of 3.57 g, a 1-degree swing from the vertical is caused by approximately 0.049 g of thrust.

The flapper is driven by a 6mm DC gearmotor (Precision Microdrive model 206-102). The revolution per minute (rpm) and torque of the motor were controlled by a driving voltage, which was ramped from 0V to 5.4V at a 0.2V step. A source/measure unit (Agilent B2902A SMU) was used to provide the voltage ramp and measure the supplied current simultaneous, yielding the input power. This SMU has a minimum measurement resolution of 100fA/100nV.

Wing kinematic was recorded separately in video of 512×512 pixels at a frame rate of 2000 frame per second using a high-speed camera (Photron FASTCAM 1024PCI). The flapper had to be clamped instead freely moving so that the high-speed camera can focus. The wing angle can be tracked to estimate the wing stroke angles. The precision of tracking depends on the ratio of the pixels to a known length captured in the video. For example, for this 100mm-span flapper, a known 8.00 mm length captured on the video as crossing 60 pixels, then each pixel is precise to 0.132 mm. However, on screen pixels are higher than the video pixels, which could help increase the precision during tracking.

The averaged inertial power can be estimated semi-analytically from the measured wing kinematics. The average inertial power can be estimated from the total work done to change wing kinetic energy over a wingbeat cycle [15, 22]. Ideally, wing gains kinetic energy in first half of downstroke and first half of upstroke, because of wing acceleration. In the latter half of the down and upstrokes, deceleration work dissipates this wing kinetic energy to bring wings to back to rest. The total work done is the sum of maximum kinetic energy changes in each half stroke:

$$\bar{P}_{acc} = \frac{1}{T} \left(\frac{1}{2} J_o \dot{\theta}_{max}^2 + \frac{1}{2} J_o \dot{\theta}_{min}^2 \right) \quad (5-2)$$

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where J_0 is the rotational inertia of a single wing and $\dot{\theta}_{max}$ is the maximum wing angular velocity measured.

The following estimates holds valid if the wing stroke profile is a sinusoidal function of time. The average inertial power can be estimated from the maximum change of wing kinetic energy over a quarter cycle of the wing beat, where the speed peaks at the mid-stroke [15, 22]:

$$\bar{P}_{inertial} = 4\pi^2\phi^2f^3J_0, \quad (5-3)$$

where f is the wing beat frequency, ϕ is the wing stroke angle, and J_0 is the rotational inertia of each wing. This estimate assumes that the wing stroke profile is a sinusoidal function of time and the maximum angular speed of the wing beat is $\pi f\phi$.

The averaged elastic power is estimated from the maximum elastic energy storage over a quarter cycle of the wing beat as the elastic elements are stopping the wings:

$$\bar{P}_{elastic} = -4fW_{elastic} \quad (5-4)$$

in which $W_{elastic}$ is the work done on the elastic element as derived in equation (4-2).

For the small FWMAV powered by the electric brushed motor, motor output torque is directly proportional to the motor torque constant and the current. The average mechanical power is given by the product of average motor torque and average motor speed:

$$\bar{P}_{mech} = 2\pi f k_m \bar{I}, \quad (5-5)$$

in which $2\pi f$ is the average motor speed, k_m is the motor torque constant (7.664mNm/A), and $\bar{I}$ is the average current drawn by the motor (Precision Microdrives 206-102 [62]).

This setup is used to characterize the dc-motor-powered non-linear thoracic mechanism in 4.4.

5.2 Method to Characterize Instantaneous Static Thrust, Electrical Power and Wing Kinematics

To obtain instantaneous thrust generation and power, a tethered flight test was done by running a flapping-wing mechanism on a single-axis load cell using the setup as shown in Figure 5.2. The thrust generated by flapping wings were measured using a load cell (Zemic Q47-20*6.6, 120g capacity). The signal output of the load cell was amplified 1000 times using a signal conditioner (Vishay 2311 Signal Conditioning Amplifier) and logged using a data acquisition card (NI USB 6009 DAQ card) at 2000 readings per second. The force reading was zeroed when the wing mechanism was idle. The average lift generated by flapping wings was obtained from 10 cycles of sample data in the 8s continuous measurement. This setup was used to characterize the bistable flapping mechanism and the 240mm-span PI-film hinge X-wing.

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The motor's powered with SMU with a constant voltage input. Throttling of the brushed motor is done by ramping up the input voltage; whereas for the brushless motor-driven X-wing, a transmitter throttles the motor, but the input voltage is maintained at 4.2V.

Figure 5.2 Experiment setup for the measurement of thrust and input power to motor and high-speed video of wing kinematics with a 1-axis beam load cell with Vishay Signal Conditioner. The load cell is fixed to a 1kg-heavy platform. Left- Setup for a DC brushed-motor powered flapper with two-wire power connection. Right- Setup for a DC brushless-motor powered 240mm X-wing with 2-wire connection which has remote sensing of the terminal voltage of the ESC to compensate for voltage drop due to lead wire resistance.

At the same time, wing motions of the running mechanism were recorded in video of 512×512 pixels at a frame rate of 2000 frame per second using a high-speed camera (Photron FASTCAM 1024PCI). However, due to small internal memory, only a maximum 6400 frame can be recorded, which is about 3.2s. For each recording, five cycles of wingbeat are saved for analysis. Tracking of the recorded wing motion in the stroke plane yields the instantaneous wing angular position and wing angular speed about flapping axis; while tracking of the projected chord motion yields the wing pitch and chord rotation speed about the wing leading edge.

Figure 5.3 shows a second thrust measurement setup using a load cell ATI Nano17 with 6-axis force/torque measurement. It is used to characterize the 280-mm span X-wing FWMAV. The brushless motors of the devices are attached to an ATI Nano17 6-axis force sensor, which records the forces and torque at 2000Hz. The ATI Nano17 has a capacity of 12N in the F_y and F_x directions, 17N in F_z direction and a 120Nmm capacity in the 3-torque axis. Its resolution in force and torque are 3.125mN (0.3186 gram-force) and 0.015625 N.mm respectively.

The brushless motor is controlled by YEP-7A ESC and powered at constant input voltage 4.2V by an SMU with remote-sensing of the voltage at the ESC terminals. The current and ESC

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voltage are recorded at 2000Hz. Throttle signals to the ESC are generated via an Arduino kit with commands input in the PC. The average data on force, torque and power are taken from 10 cycles in the sample for each throttle settings.

Figure 5.3 Experimental test setup to measure thrust and torque with ATI Nano17 6-axis force/torque sensor. The ATI force sensor is suspended in air, 23cm away from nearest side wall and 70cm above ground through a bridge fixed to a vibration table. This setup is used to characterize (a) a 280mm-span X-wing for the thrust generation and power, and the torque when only a single wing plane of the X-wing is flapping; (b) The wing stroke angular position and the pitch angle calculated from tracked points on the leading edge and the trailing

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edge of the wing spars in the high speed video; (c) a propeller for its thrust generation, drag torque, power and spin speed.

The wing kinematics are recorded by Photron FASTCAM 1024 PCI, at a resolution of 768×512 , 2000 frames per second and 1/10000s shutter speed. The wing stroke position and the wing pitch are traced from the white dot markers placed on the front leading edge and the trailing edge of the mid-rib chord (at radius = 65mm). The wing pitch angle is calculated from the projected length and the actual length of the mid-rib chord. Note that the kinematics may differ between the half wings in terms of the absolute angles, but the time profile of the wing motion is similar. We will only show the kinematics of the half wing attached to the right of the front leading edge in the result.

This same experimental setup with ATI Nano17 sensor is used to characterize propeller which spins in high frequency range of 100-240Hz. The sampling rate for the thrust, torque and power is increased to 10000 Hz to capture sufficient information for the cycle variation. The high-speed video is recorded at 5000 frame per second at a resolution of 384×384 with a shutter speed of 1/96000 second. The torque measured for propeller is contributed by the wing drag because there is no rotational acceleration. Hence, aerodynamic power of the spinning propeller can be calculated from the product of the torque Q and the constant spin speed ω . However, where the torque sensor is not available, the aerodynamic power can be estimated from the difference in the mechanical shaft power consumed between the flapping of wings in air $\bar{P}_{mech,air}$ and in vacuum $\bar{P}_{mech,vac}$:

$$\bar{P}_{aero} = \bar{P}_{mech,air} - \bar{P}_{mech,vac} \quad (5-6)$$

The motor shaft power can be obtained by product of the current, average motor speed and the motor torque constant following equation (2-17): $k_t \omega_m I(t)$. The friction loss in the motor can be estimated from the difference between the motor shaft power and the aerodynamic power $Q\omega$. A minimum friction loss can be obtained from the mechanical power consumption by operating the flapping mechanism without the wings installed. These give the aerodynamic, mechanical and electrical efficiency of thrust generation.

5.3 Characterization of Elastic Properties

The elastic stiffness of the mechanism is measured statically by the displacement the spring from its neutral position given a load on the spring. We add incremental weights by 1 g at a fixed rigid point of the mechanism to deform the springs and measure the displacement of the point. For example, for the small non-linear thoracic mechanism, we attach loads on the center of the tergum. The elastic energy storage is equal to the positive work done by the external force F to depress or elevate the tergum from its no-load position:

$$W_{elastic} = \int_0^y F dy \quad (5-7)$$

For the single spring design for large X-wing, we attach loads on the stiff wing leading edge at a distance away from the hinge. The wing base was disengaged from the crank to free it from the friction of the gears and motor. Increasing weights induced a larger moment M to bend the hinge to larger angle β_h . This moment-angle relationship must be determined experimentally because the hinges will yield at large angle unaccounted by theory, though it maintained elasticity. This actual moment-angle relationship is used to determine the elastic energy stored:

$$W_{elastic} = \int_{\beta_{h1}}^{\beta_{h2}} M(\beta_h) d\beta_h = \int_{\beta_{hmin}}^{\beta_{hmax}} K\beta_h d\beta_h \quad (5-8)$$

However, methods of unidirectional loading will not work for the bistable mechanism with two neutral positions. This is because the elastic forces changed direction from resisting to assisting depression as the tergum is depressed past the midstroke, which caused the tergal joint to snap abruptly without restraint. As such, we use an INSTRON tensile tester with a 10N load cell which can restrain the tergum movement and measure the force simultaneously. The tergum of mechanism is depressed by the INSTRON through a rigid rod for a 10mm travel downward at 0.1mm/s rate. The compressive force readings are recorded 100 times per second. This compression test can precisely measure the force-change over the complete downstroke or upstroke from one stable to next stable position. A negative force will be measured when the tergum, which was supposed to snap, is restrained by INSTRON between the meta-stable to the opposite stable positions. The elastic energy storage is equal to the positive work done by the external force F to depress or elevate the tergum from a stable position to the meta-stable position right before the snap:

$$W_{elastic} = \int_{\text{stable end}}^{\text{meta-stable point}} F dy \quad (5-9)$$

Secondly, we need to demonstrate the fact that a stiffer hinge stores more energy to recoil wing faster to a higher speed. Therefore, we deform the mechanism to a certain wing angle and release the mechanism for recoil. We record the free-oscillating wing motion at the moment of release with the high speed camera. Then the maximum angular velocity and the time taken to return to neutral position can be obtained from the tracking of the wing motion. For the bistable mechanism, a snap test is devised to measure the wing speed gained from the snap. The mechanism is mounted on top of a load cell while a screw approaches from above to depress the tergal joint down until it snaps. During the snap, the screw loses contact with the tergal joint while wings accelerate towards to the opposite extreme position upon release of the elastic energy. The maximum wing speed gained from the snap depends on the amount of elastic storage, minus the friction of either the air or revolute joint.

Chapter 6 Flight Thoracic Mechanism with Nonlinear Stiffness

This chapter discuss an insect-inspired three-spring thoracic mechanism for the elastic recovery of inertial cost of a 100mm two-wing flapper. The solution is a Dipteran-like three-spring thoracic mechanism of which elastic stiffness increases drastically towards the end of a stroke, to stop and bounce the wing in reversal. It can generate thrust with a lower energetic cost than that without elastic storage.

6.1 Dipteran Insect Inspired Thoracic Mechanism

A Dipteran insect's flight thorax segment is a deformable ring structure, onto which the wings are attached [16, 74]. Figure 6.1 shows the ring segment composed of four joined rigid exoskeletal plates, namely a back plate (tergum), an abdominal plate (sternum), and two side plates (pleura). The sternum and pleura plates are rigidly joined while the tergum plate is free to move relative to the rest. Wing bases are elastically hinged to the tergal and pleural plates. These exoskeleton plates are made of tough *chitin* lamellae (with Young's modulus of 7.848GPa [75]), while the wing hinges are composed of soft elastic *resilin* (with Young's modulus of 588.6kPa [75]). The wings beat together with the reciprocating tergum plate as the thorax is deformed by an agonist–antagonist muscles pair. Towards the end of a wing stroke, the wings are decelerated rapidly by impact contact between the wing bases and the elastic thorax. The impact deforms the thorax elastically and transfers wing kinetic energy into thorax's strain energy. Subsequently, the stored elastic energy in the thorax is released to help reverse the wings. In this way, the Dipteran insects save inertial power for flapping wings.

Figure 6.1 Dipteran insect's flight thorax (After Snodgrass [76])

To address the need for elastic storage in a 10-15cm wing-span motorized FWMAV, we designed a compliant mechanism shaped like Dipteran insect's thorax. This thoracic mechanism is a deformable shell structure, made of CFRP exoskeleton plates and polyimide

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film hinges (Figure 6.2 (a-b)). Figure 6.2 (c-d) shows that a symmetric half of the thoracic mechanism has a wing base plate elastically joined between a tergal plate and a pleural process plate. The pleural process plate is hinged to the anchor of pleural plate. Depression of the thorax at the tergal plate produces an upward wing stroke. Conversely, an upward tergal displacement causes a downward wing stroke. Reciprocation of the tergal plate is driven by a motor through a coupling crank-slider mechanism.

This compliant mechanism exhibits a nonlinearly increasing stiffness as its tergal plate is displaced from the neutral position towards the maximal position. Though configured differently, this thoracic mechanism can store elastic energy like that achieved by Dipteran insects through the interaction between the radial stop and elastic thorax. This geometrically nonlinear stiffness is attributed to plate rotations and elastic hinge deformations.

Figure 6.2 (a) Compliant thoracic mechanism with integrated polyimide film hinges for elastic energy storage. As the tergal of the thorax is depressed, its wings beat upwards. A schematic drawing for the compliant thoracic mechanism: (c) a full thorax when not loaded; (d) a symmetric half thorax when it is loaded downward at its tergal plate.

To simplify the geometrically nonlinear analysis, a symmetric half of the thoracic mechanism is modeled as a four-bar linkage with elastic torsional-springs in parallel with the joints. Figure 6.3 shows this pseudo-rigid model: a slider representing the tergal plate while linkages of length l_b and l_p representing the wing base and pleural process plates respectively. Torsional springs at the joints represent polyimide hinges between the plates. The torsional stiffness of pleural hinge is K_0 , while those for the wing and tergal hinges are K_1 and K_2

respectively. Torsional hinge stiffness can be calculated from the formula below, according to [69]:

$$K = \frac{dM}{d\phi} = \frac{EI}{H} \quad (6-1)$$

where E is the Young's Modulus, I is the beam cross-sectional area moment of inertia, and H is the equivalent hinge length, which is the sum of the linkage length and half of the connecting hinge lengths, e.g. the effective length of wing base linkage is the sum of half-length of tergal hinge, half-length of wing hinge and the length of wing base plate.

Figure 6.3 A pseudo-rigid model for a symmetric half thorax: (left) neutral state when not loaded; (right) deformed state when the tergum (i.e. the slider) is loaded by a downward vertical force F .

This pseudo-rigid-model allows quick estimates for the wing rotation and the required force for a given tergal displacement. As such, the design of the thoracic mechanism can be iterated easily.

When not loaded, the tergum is statically positioned at the neutral point y_0 , with the wing base angled slightly downward (see Figure 6.4(b)). As the tergum is depressed by a vertical force F downwards, the wing base plate rotates (l_b) counterclockwise by an angle ϕ above the horizontal plane while the pleural process plate (l_p) rotates by an angle α off the vertical plane. This causes a downward tergum displacement y off the neutral position:

$$y = y_0 + l_b \sin \phi - l_p \cos \alpha \quad (6-2)$$

As the anchor to the pleural wall has a constant distance W from the plane of symmetry, the rotations of wing-base and pleural-process plates will satisfy the following constraint:

$$W = l_b \cos \phi + l_p \sin \alpha \quad (6-3)$$

Equations (5-2) and (5-3) suggest that the wing stroke angle per tergum displacement depends on the linkage lengths l_b and l_p .

The depressive force F on the tergum is resisted by the spring forces of the thoracic compliant mechanism. It can be determined from a force analysis of the individual linkages. The moment equilibrium for the pleural process plate yields:

$$K_0\alpha + K_1\left(\phi - \alpha + \frac{\pi}{2}\right) - Fl_p \sin \alpha + Rl_p \cos \alpha = 0 \quad (6-4)$$

The moment equilibrium for the wing base plate yields:

$$K_1\left(\phi - \alpha + \frac{\pi}{2}\right) + K_2(\phi) - Fl_b \cos \phi - Rl_b \sin \phi = 0 \quad (6-5)$$

The force acting on the tergum plate is determined as a function of the joint angles following:

$$F = \frac{\left\{K_0\alpha + \left[K_1\left(\phi - \alpha + \frac{\pi}{2}\right)\right]\left(1 + \frac{l_p \cos \alpha}{l_b \sin \phi}\right) + K_2(\phi)\frac{l_p \cos \alpha}{l_b \sin \phi}\right\}}{l_p\left(\sin \alpha + \frac{\cos \alpha \cos \phi}{\sin \phi}\right)} \quad (6-6)$$

The force increases with the increasing tergum displacement from the neural position. As the thoracic mechanism is deformed, the elastic energy stored in it is equal to the work done by the depressive force F on the tergum plate. Integration of the force over the tergal displacement y yields:

$$W_{elastic} = \int_0^{y_{max}} F dy \quad (6-7)$$

in which F and y are functions of ϕ according to equations (6-6) and (6-2) respectively. The maximum elastic storage depends on the maximal tergal displacement y_{max} , which is determined by the crank length of a motorized crank-slider mechanism.

In a quarter cycle of the wing stroke as the thorax is depressed, the applied force is in the direction of the tergum displacement. This results in elastic energy storage in the flexures of the thorax. In the next quarter cycle as the thorax is relaxed, the stored elastic energy is released and converted into wing kinetic energy. Similarly, the energy storage and release happen as the tergum of the thorax is lifted.

6.2 Design and Elastic Properties

Table 3 Parametric values for the pseudo rigid model.

Parameter	Input Value
l_b	7 mm
W	7 mm
l_p	4 mm
K_0	4.267 mNm/rad
K_1	0.03414 mNm/rad
K_2	0.1366 mNm/rad

A thoracic compliant mechanism is designed with the listed parameters in Table 3. For simplicity, its wing base l_b is designed to be the same as the spacing W between the pleura anchor and the symmetry plane. Among the three elastic hinges in a half thorax, the wing hinge between the wing-base and pleural-process plates is designed with the least stiffness K_1 to allow for a large wing stroke. The tergal hinge between the tergum and wing-base plates is stiffer at

K_2 to transfer the actuation force. The pleural hinge between the pleural-process plate and the anchor is the stiffest at K_0 to store the most elastic energy. These hinges will be bent most towards the maximal tergal displacement, up or down.

This design intends to produce a static wing stroke of 60° given $\pm 3\text{mm}$ of tergum displacement, when driven by a 6-mm diameter coreless DC gearmotor (Precision Microdrives 206-102 [62]) through a 3mm long crank.

Figure 6.4 (a) Wing angular position as a function of tergum depression of the compliant mechanism. (b) Static force required to depress the tergum plate

To measure its elastic properties, the thorax was statically loaded with increasing deadweights at a 1-gram increment and photographs of its deformed shapes were taken in sequence. This thoracic design was observed from Figure 6.4(a) to sweep a 10° wing stroke for a 1 mm tergal displacement. Meanwhile, the force acting on the tergum increased non-linearly with the tergum displacement as shown in Figure 6.4(b). The elastic energy stored in the thorax can be calculated from the area under the force-displacement curve. It amounted to 0.4937mJ at a 4.20 mm tergum displacement (i.e. at a 33.2° wing stroke). As shown in these figures, the analytical estimates agree well with measurements.

6.3 Fabrication of the Transmission Mechanism

The compliant thoracic mechanism is a deformable shell structure made of CFRP plates and polyimide film hinges. CFRP plates and polyimide hinges can be fabricated and integrated on a strip of polyimide film (DuPont Kapton HN) by selective reinforcement [77, 78, 79]. Plates are CFRP reinforced patches while a hinge is an unreinforced film spacing between the reinforced patches. The CFRP patches are obtained by manual cutting CFRP prepreg (HexPly M10R/38%/UD150/ CHS) using a rotary blade cutter. For this thoracic mechanism design, the three hinges of distinct stiffness are made from three separate polyimide strips of different film thickness as shown in Figure 6.5.

Figure 6.5 Assembly of a half compliant thoracic mechanism from three strips of polyimide hinge/CFRP plates.

Table 4 Design parameters of the polyimide hinges

Design Part	Symbol	Dimension
Tergal hinge	t1	0.0508 mm
	H1	2 mm
	P1	1 mm
Wing hinge	t2	0.0254 mm
	H2	1 mm
	P2	3 mm
Pleural hinge	t3	0.1270 mm
	H3	1 mm
	P3	2.5 mm
Width of film hinges		10 mm
Half space between pleura	W	7 mm

As listed in Table 4, film hinges are prepared from 10 mm wide polyimide films of distinct thickness: 50.8 μ m thick tergal hinge, 25.4 μ m thick wing hinge, and 127.0 μ m thick pleural hinge. After selectively reinforced by CFRP patches, the three strips of integrated hinge and plates are assembled by lapping and adhesively bonding over common plate areas. The assembly are subsequently folded and mounted to an acrylic plate housing. As a result of these fabrication processes and assembly, the thoracic compliant mechanism is obtained as shown in Figure 6.2.

This wing design adopts a semi-elliptical shape, which has a 50 mm long leading edge and a 31 mm long root chord. The leading edge of the wing foil is reinforced spanwise by CFRP on both sides; whereas, the root chord of the wing foil is lightly reinforced with one-side CFRP such that it is flexible chord-wise for passive wing rotation about the leading edge. Table 5 lists and compares this transmission mechanism and wing properties with a rigid-body mechanism described next.

To serve as a benchmark for comparison, a rigid-body mechanism was developed to bear the same wings and beat almost the same wing kinematics (80° wing stroke) as the compliant thoracic mechanism does. This rigid-body mechanism is an assembly of acrylic linkages, and revolute and prismatic joints, and metal pins. These linkages and its joints or slots are fabricated by laser cutting 1 mm thick acrylic plates.

Figure 6.6 (a-b) A benchmark flapping-wing rigid body mechanism without elastic energy storage. (c-d) Schematic design of the rigid-body mechanism, which consists of a pair of wing levers coupled with a slider that reciprocates with motion in the y -direction.

As shown in Figure 6.6, this rigid-body mechanism has a pair of wing levers. The proximal end of each wing lever is a pivoted guide (i.e. wing base) about a fulcrum, which is spaced from the other fulcrum at a distance $2W$, the same as the pleural wall spacing for the compliant mechanism. Wing bases of the two wing levers are coupled with a pin and guided vertically. As the pin is reciprocated by a motor-powered crank slider mechanism, each wing base is driven into angular oscillation about each fulcrum. Each of the wing bases (i.e. the plastic guides) weighed 0.08g and contributed very little rotational inertia, at 2.69gmm^2 as compared to 72.05gmm^2 of each wing. To minimize the friction loss between its pin and guides, the rigid-body mechanism is lubricated with oil.

Table 5 Design specifications for the compliant thoracic mechanism and rigid body benchmark mechanism.

Design specifications	Compliant Mechanism	Rigid Body Mechanism
Dynamic (Static) wing stroke	79° (56°)	85° (80°)
Wing span (tip to tip)	100mm	100mm
Wing rotational inertia, J_0	$7.21 \times 10^{-8} \text{kgm}^2$	$7.21 \times 10^{-8} \text{kgm}^2$
Half space between pleura, W	7mm	7mm
Crank length	3mm	5mm
Weight	3.51g	3.34g

6.4 Experiment Results and Discussions

The compliant thoracic mechanism can store elastic energy, whereas the rigid body mechanism cannot. To isolate the effect of elastic storage on energetic costs, the two mechanisms are designed to beat the same wings with approximately the same wing kinematics.

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In this way, any power saving by the compliant mechanism to produce the same thrust could provide a measure for the effectiveness of the elastic energy storage.

Figure 6.7 Free oscillation of a wing about its root which was supported on a compliant thoracic mechanism whose tergum was cranked up. Wing spar positions, at the tip (y_{tip}) and the root (y_{root}) between which were spaced at length (l), were tracked from a high-speed video of the oscillation.

In a lightly damped free vibration test, this winged thoracic compliant mechanism exhibited a natural frequency over a range of 30~37Hz, depending on the crank position. Figure 6.7 showed free oscillation of a wing attached on the thoracic compliant mechanism whose tergum was cranked up. As the thorax was driven by a motor into forced oscillation, its dynamic wing stroke increased with the increasing wing beat frequency. Figure 6.8 shows that the wing stroke angle increased from a static angle of 55° at 0Hz to a higher dynamic angle of 78° at 25 Hz. This 42% enhancement of the dynamic wing stroke angle is attributed to hinge flexibility and reduced dynamic stiffness of the thorax. Due to the added virtual mass of air trapped by flapping wings [80, 81], its resonance for the forced oscillation is expected to occur at a lower driving frequency than the natural frequency. However, resonance peak was not observed from Figure 6.8 due to increasing loads with higher frequency.

In comparison, the wing stroke angle generated by the rigid-body mechanism is not frequency dependent. It remained nearly constant at 85° , slightly above the static angle of 80° , throughout the whole frequency range. Above 22 Hz, both compliant and rigid-body mechanisms generated almost the same wing stroke angle. For example, Figure 6.8(a) showed a cycle of wing beat of similar wing stroke amplitude at 25 Hz for the two mechanisms. In addition, Figure 6.9(c) showed that the maximum driving frequency for either mechanism is limited by the motor capacity. However, a lower driving voltage is required for driving the compliant mechanism, as compared to that driving the rigid-body mechanism at the same wing beat frequency.

Figure 6.8 (a) Stroke angular position along time over one wing beat cycle at approximately 25Hz (b) Wing stroke for the compliant mechanism (CM) and rigid body mechanism (RBM) as a function of the wing beat frequency.

Figure 6.9 Frequency dependent performance of the non-linear compliant mechanism (CM) and the rigid body mechanism (RBM): (a) Mean thrust generation, (b) Mean electrical power, (c) Thrust-to-electrical power ratio and (d) Motor mechanical conversion efficiency.

Thrust generated by the wing flappers was measured on a tethered flight test stand using a simple pendulum method [82, 83] as described in 5.1. This tethered flight test demonstrated that the thrust generated by the wing flappers increased with the increasing wingbeat frequency (see Figure 6.9(b)). Meanwhile, the thrust generated by the compliant depends also on the dynamic wing stroke, which varies with the frequency. Over the low driving frequency range, the compliant mechanism produced less thrust than the rigid-body mechanism did, e.g. at most

18% lower at 15 Hz, due to a smaller wing stroke. However, this thrust difference appeared small in the plot along with much larger thrust magnitude at higher frequencies. Over the higher frequency range above 25 Hz, the thrust generated by the compliant mechanism increased and become almost equal to the thrust generated by the rigid-body mechanism as its dynamic wing stroke increased as much as that of rigid-body mechanism. As such, the effect of elastic energy storage could be better seen by comparing their energy energetic costs over the high frequencies.

The electrical power required by a DC motor to drive flapping wings depends on several factors, namely wing beat frequency, aerodynamic load, and mechanism design. Meanwhile, the aerodynamic load increased with the wing stroke and wing beat frequency. Figure 6.9(b) showed that the average electrical power required increases with the wing beat frequency. Note the motor driving the compliant mechanism consumes less electrical power than that driving the rigid-body mechanism. For example, the compliant mechanism consumed 31% less power while beating wings at 15Hz. It consumed 22% less power while beating wings at 25Hz. During a cycle of wing beat, the motor powers were observed from Figure 6.10 to peak twice towards the ends of wing strokes, either up or down.

Figure 6.10 Electrical powers required to drive the compliant and rigid-body mechanisms over two wingbeat cycles at 25 Hz. The average power required to drive the compliant mechanism (CM) is lower than that required to drive the rigid body mechanism (RBM).

To better isolate the effect of elastic energy storage, one could alternatively compare the two mechanisms in term of the thrust-to-power ratio, which measures the effectiveness of the mechanism to convert electrical power to aerodynamic thrust using the same motor. Figure 6.9(c) shows that, for either mechanism, the generated thrust per power increases at decreasing rates with the increasing frequency, up to an ‘optimum’ frequency beyond which the effectiveness drops. Instead of the influence by elastic resonance, the drop of thrust per power beyond the maximum is believed to be attributed to limited motor capacity. This belief is supported by Figure 6.9(d) which showed that the motor efficiency decreased with faster flapping, which loads more either mechanism. Due to reduced reactive load, the motor efficiency is improved by using the compliant mechanism, instead of the rigid-body mechanism.

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As a result, the compliant mechanism produced more thrust per power than the rigid-body mechanism over the same to frequency, e.g. 20% more at 25Hz.

6.4.1 Power Components along Frequency

To verify if sufficient elastic energy storage could fully recover the wing inertial power, we shall analyze the breakdown of the power components which were consumed by each flapping-wing mechanism.

Mechanical power measurement provides a means to indirectly measure a power component, which differs in two distinct operating conditions. For example, the mechanical power incurred to drive a wing flapper in air ($\bar{P}_{mech}$) is higher than that in vacuum ($\bar{P}_{mech,vac}$) due to the absence of aerodynamic power ($\bar{P}_{aero}$) in vacuum [34, 37]. On the other hand, the mechanical power required to drive a wingless flapper in air ($\bar{P}_{mech,wingless}$) is mainly due to the friction loss of the gears and motor shaft ($\bar{P}_{losses}$). Hence, the apparent inertial power ($P_{acc} + P_{elas}$) can be estimated as the difference between the mechanical power for driving winged mechanism in vacuum and that for driving the wingless mechanism, which was tested in air instead of vacuum to avoid motor overheating.

Figure 6.11(a) shows the mechanical powers required to drive the compliant mechanism under various conditions while Figure 6.11(b) shows the power required to drive the rigid-body mechanism under those conditions. In general, the mechanical power required increased with the wing beat frequency. Interestingly, it is observed from Figure 6.12(a) that the apparent inertial power for the compliant mechanism vanishes over the frequency range up to 25Hz. In contrast, the inertial power for the rigid-body mechanism remains substantial and increases monotonically with increasing frequency. However, beyond 25 Hz, the compliant mechanism cannot fully recover the inertial power because the wing inertial power then exceeds the elastic power capacity as shown in Figure 6.12(b).

To this thoracic compliant mechanism with nonlinear stiffness, the effect of resonance is not obvious in reducing the electrical power (see Figure 6.9(c)), unlike how it did to the linear elastic system [47, 84]. However, sufficient elastic energy storage, even by a nonlinear one, is effective to reduce the inertial power. For example, to drive a compliant mechanism at 25 Hz, the inertial power amounted to merely 2% of the total power (see Figure 6.12 (a)). On contrary, to drive a rigid-body mechanism at the same frequency of 25 Hz, the apparent inertial power amounted to 23% of the total mechanical power (see Figure 6.11(b)). The apparent inertial power remained small up to 25Hz wing beat frequency and it showed a trend of slightly more dip with increasing frequency towards 25 Hz (see Figure 6.12 (a)).

Figure 6.11 Mechanical power required to drive the (a) compliant thoracic mechanism and (b) rigid body mechanism under different operating conditions: flapping wings in air ($\bar{P}_{mech}$), vacuum ($\bar{P}_{mech,vac}$), and operating mechanism without wings ($\bar{P}_{mech,wingless}$).

Figure 6.12 (a) Apparent inertial powers requirement for the compliant mechanism and the rigid-body mechanism as a function of wing beat frequency. Semi-analytical estimates were calculated from equations (5-3) and (5-4). (b) Calculated inertial powers and elastic power as a function of wing beat frequency, following equations (5-3) to (5-4) respectively and using the measured wing stroke and wing beat frequency

6.4.2 Comparison with Previous Works

In comparison with coil springs (as listed in Table 6), polyimide film hinges are much lighter in weight. The CFRP shell structure with these integrated polyimide film hinges exhibit nonlinear increasing stiffness as its tergum plate is depressed increasingly. This shell structure exhibits a low stiffness when it is at the neutral position, where the wing speed reaches maximum; whereas, it exhibits high stiffness, up to 700 N/m when it reaches its maximum position where the wing stroke is stopped. As such, it could store sufficient elastic energy storage, without loading the motor unnecessarily near the neutral position. As a result, this thoracic compliant mechanism is effective for fully recovering the inertial power from a low wing beat frequency up to 25 Hz. In contrast, the previous compliant mechanism with a discrete linear spring [46] could only save power above a 15Hz frequency threshold, which is about 66%

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of the 24.5 Hz resonant frequency. In a recent work, coil springs were directly coupled with DC motor for flapping wings [47, 85], but information of power saving by adding springs were not available for comparison.

6.5 Theoretical Model Prediction of Lift and Power

The mechanism beats a sinusoidal wing stroke of $2\Phi=80^\circ$ with pitch angle amplitude, $B=19.5^\circ$ with a phase lead of $\varphi=0.34\pi$ rad at 33.7 Hz. These behaviors are modelled with the following simplified wing kinematic equations:

$$\phi(t) = \Phi \sin(2\pi ft) \quad (6-8)$$

$$\dot{\phi} = 2\pi f \Phi \cos(2\pi ft) \quad (6-9)$$

$$\ddot{\phi} = -4\pi^2 f^2 \Phi \sin(2\pi ft) \quad (6-10)$$

$$\beta(t) = B \sin(2\pi ft + \varphi) \quad (6-11)$$

$$\dot{\beta} = -2\pi f B \cos(2\pi ft + \varphi) \quad (6-12)$$

Consider a rigid semi-elliptical wing with a wing length $R=50\text{mm}$ and a root chord length, $c_{\text{root}} = 31\text{mm}$. The chord length changes with the radial distance with the following function:

$$c(r) = c_{\text{root}} \sqrt{1 - \frac{r^2}{R^2}} \quad (6-13)$$

The instantaneous total lift and drag torque due to translation and rotation of a single wing is estimated based on the integrals in equations (3-17) and (3-22) but with rigid wing assumption where the angle of attack of the wing is the same along the wing span. The lift and drag coefficients are from equations (2-4) and (2-5). The wings do not clap and fling but only pronate and supinate in isolated rotation. Hence, the total lift and drag torque due to translation and rotation are:

$$L(t) = \int_{r=0}^{r=R} \frac{1}{2} \rho C_L c(r) (r\dot{\phi})^2 + \frac{3\pi}{4} \rho (r\dot{\phi}) c(r)^2 \dot{\alpha} \cos(\alpha) dr \quad (6-14)$$

$$= \frac{\pi}{32} \rho C_L c_{\text{root}} \dot{\phi}^2 R^3 + \frac{3\pi}{16} \rho c_{\text{root}}^2 \dot{\phi} \dot{\alpha} R^2 \cos(\alpha)$$

$$Q_{\text{aero}} = \int_{r=0}^{r=R} \frac{1}{2} \rho C_D c(r) (r\dot{\phi})^2 r + \frac{3\pi}{4} \rho (r\dot{\phi}) c(r)^2 \dot{\alpha} \sin(\alpha) r dr \quad (6-15)$$

$$= \frac{1}{15} \rho C_D c_{\text{root}} \dot{\phi}^2 R^4 + \frac{\pi}{10} \rho c_{\text{root}}^2 \dot{\phi} \dot{\alpha} R^3 \sin(\alpha)$$

The inertial torque is simply the product of the wing's inertia ($J_0 = 7.21 \times 10^{-8} \text{kgm}^2$) and the angular acceleration:

$$Q_{\text{inertia}} = J_0 \ddot{\theta} \quad (6-16)$$

To calculate the elastic torque, we first find out the corresponding tergal displacement to the wing angle from curve fitting of Figure 6.4(a), and corresponding tergal force from curve-fitting

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of Figure 6.4(b). Then the elastic torque is given by the product of the vertical force and the moment arm given by the wing angle and the wing base length ($D=0.004\text{m}$):

$$y = 0.1056572 \phi + 1.2634463 \quad (6-17)$$

$$F_y = 1.1832156y^3 - 4.9814946y^2 + 6.318788y \quad (6-18)$$

$$Q_{elastic} = F_y D \cos \phi \quad (6-19)$$

The instantaneous power required of the motor is given by the absolute sum of the instantaneous torque times the instantaneous wing angular velocity:

$$P_{mech} = |Q_{aero}\dot{\phi} + Q_{inertia}\dot{\phi} + Q_{elastic}\dot{\phi}| + P_{loss} \quad (6-20)$$

The power and lift calculated in the above equations are for one wing. The results are multiplied by two and compared to the experimental values. The power loss is not modeled but we use the experimental measured data in Figure 6.11.

Figure 6.13 (a) Theoretical lift generation increases with wingbeat frequency, contributed largely by the rotational lift and partly translational lift. The theoretical model fits well to the experimental value. (b) Theoretical mechanical power with elastic mechanism is lower than without elastic storage along frequency, similar to experimental findings. (c) The mean absolute of aerodynamic power, inertial power and elastic power along frequency shows that inertial power dominates as frequency increases. (d) The theoretical lift to mechanical power is vastly improved with elastic mechanism.

The theoretical calculated trend along frequency is shown in Figure 6.13. The theoretical lift generation based on the wing kinematic model matches the experimental results closely. The rotational lift contributes the most compared to translational lift. This shows that flapping

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wing flight relies on unsteady effect rather than steady translation to generate lift. This is especially true for flappers with small wing stroke at high wingbeat frequency, such as mosquitoes [31].

Figure 6.13b shows that the elastic energy saves power at wingbeat frequency above 15Hz. Below 15Hz, the elastic power is higher than net sum of inertial and aerodynamic power, hence the extra power is to be dissipated by motor. Above the 15Hz, the inertial power increases more and more significantly, thus elastic storage becomes more useful, being able to recover most power at about 25Hz. However, the elastic power cannot catch up with the increase in inertial and aerodynamic power. Beyond 25Hz, the inertial power and aerodynamic power became too large to start diminishing the effect of the elastic storage. As a result, the lift per mechanical power with elastic storage increases at higher rate above 15Hz to 25Hz than that without the elastic storage.

Figure 6.14 Analytical estimates of the lift and power variation in wingbeat cycle at 25 Hz: (a) Model of sinusoidal wing stroke and pitch angle. (b) Translational lift, rotational lift and the net lift. (c) Instantaneous aerodynamic power, inertial power and elastic power variation in a wingbeat cycle. (d) The net power required with and without elastic storage, compared to the experimental results of the non-linear mechanism and its rigid body counterpart at 25Hz.

Figure 6.14 shows the variation of the lift and power in a cycle according to the wing kinematics at 25Hz. At a maximum pitch angle of 25° , the translational lift is low even at high angular speed because wing angle of attack is very high. However, a large rotational lift is still

generated because of the long wing chord and wing pitch rotation. This agrees with findings that the small wing stroke natural flyers like mosquitoes.

Benefits of the elastic storage to recover energy in wing deceleration is evident in Figure 6.14b. The larger inertial power in wing deceleration cannot be totally transferred to aerodynamic braking, but can be fully recovered with elastic storage, as denoted by the valley plateau in Figure 6.14c. The elastic storage pays back the absorbed power in the reverse stroke to accelerate the wing, further reducing the net required power. This assistance occurs twice in a wingbeat cycle, therefore the average power over a cycle is greatly reduced.

Hence, the finding from this case study shows that the elastic energy storage can reduce the power requirement of hovering flight if the elastic energy is sized properly to the inertia.

6.6 Conclusions

We have presented a compliant thoracic mechanism with integrated polyimide film hinges for elastic energy storage. These film hinges add no almost extra weight to the compliant mechanism, which consists of CFRP plates and polyimide film. Due to hinge flexibility, this thoracic compliant mechanism enhances the dynamic wing stroke angle up to 42% higher than the static wing stroke angle when beating wings above 22Hz frequency. As a closed-form shell structure, this compliant thoracic mechanism exhibited nonlinear stiffness characteristics, with increasing stiffness from a low value at the neutral position to as much as 700N/m at the maximal position. As a result, it can provide sufficient elastic storage to fully recover the kinetic energy from 100-mm-span wings flapping at 25Hz. This power saving is attributed to almost zero apparent inertial power, which is verified both experimentally and theoretically, with sufficient elastic energy storage. In future, we shall further investigate the effectiveness of such means of elastic energy storage for a larger flapping-wing micro air vehicle (with greater-than-240-mm wing span), which could carry more payload.

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Table 6 Comparison among various flapping-wing mechanisms with capability of elastic energy storage

Current and Previous Works	MAV Weight	Wing Span	Wing stroke	Flapping Frequency	Spring Type	Spring Stiffness	Spring Weight	Power Saving
This thoracic compliant mechanism	3.51g	100mm	56°-80°	9-33Hz	Shell Structure	0-700N/m	0.066g	31% max
Baek's compliant mechanism [46]	5.8g	80mm ^a	~55°	10-30 Hz	Coil Spring	140N/m	0.259g ^b	30% max
Vamp RC (rigid-body mechanism) [46]	13.0g	304.8mm	45°	16Hz	Coil Spring	980N/m	0.444g ^c	19% max
Sahai's compliant mechanism [56, 86]	3.0g	124mm	100°	16-18Hz	Rubber Strips	3.2mNm/rad	0.02g	20% max
Bejgerowski's compliant mechanism [59]	12.76g	152mm	65°	12.1Hz	Leaf springs	0.7mNm/deg	–	–
Madangopal's rigid-body mechanism [49]	–	600mm	90°	0.8Hz	Coil Spring	51.3N/m	–	(12% torque)
Tantanawat's rigid-body mechanism [50]	–	660mm	44.8°	4Hz	Coil Spring	38N/m	–	42% predicted
Hines' Direct Motor-Driven Flapping [47]	2.7g	140mm	40-160°	2-20Hz	Coil Spring	2.83mNm/rad	0.12g ^d	-
Roll's Electromagnetic Actuator [85, 87]	5.2g ^e	90-140mm	68-107°	30-71Hz	Magnet	0-5.64mNm/rad	-	-

^a Estimated from the scale of photo image

^b Estimated for a 5.5mm-diameter coil spring with 10 turns, made of a bronze wire of 0.44mm diameter and spring free length of 21.5mm. The phosphorous bronze Grad A has a Young modulus of 103GPa, and a density 8860kg/m³

^c Estimated for a 5.5mm-diameter coil spring with 8 turns, made of a bronze wire of 0.65mm diameter and spring free length of 21.5mm.

^d Estimated by the difference between the wing-motor mass and the total system mass.

^e Estimated based on two actuators each with a single wing.

Chapter 7 Bistable flapping-wing mechanism

This chapter introduces a simple bistable thoracic mechanism for flapping wings inspired by the Dipteran's click mechanism, as proposed by Boettiger in 1952. The principles of the mechanism is first explained. Then the effect of the bistable mechanism is shown by comparing to a rigid body counterpart. The bistable mechanism is then scaled up to flap a pair of wings with half span of 14cm. This larger bistable mechanism can generate up to 30g of thrust, therefore showing a potential to take off.

Figure 7.1 (a) The assembly of bistable click mechanism wing flappers (b) a Mylar wing with carbon-fiber reinforcement and elastic hinge.

7.1 The dipteran flapping mechanism

Various wing mechanisms were proposed to account for the observed Dipteran wing movements [20, 60, 88, 89, 90]. Among them, a classical click mechanism was well accepted to account for bi-stable non-harmonic wing movements given the sinusoidal input force [60] [20, 60, 91]. It increases the maximum velocity on the downward stroke to give greater lift [57, 92]. Yet, there is still debate about the existence of click mechanism [89, 90, 92, 93, 94]. So far, flapping-wing micro-air vehicles has yet adopted bi-stable elastic mechanisms, like Dipteran insects' click mechanism, despite some initial attempts [46, 79, 95, 96]. The existing motorized flapping-wing micro air vehicles often adopted mono-stable mechanisms with either discrete springs [46, 47, 56, 84] or elastic hinges [97]. These reported mono-stable elastic mechanisms are found to be able to recover wings' kinetic energy and reduce the reactive load seen by the driving motor [46, 47, 56, 84, 97]. They tend to return to the mono-stable position (often at the middle position).

In the classical click mechanism model [20, 90], the wing is driven like a toggle switch and is stable at either the wing-up or wing-down positions, but meta-stable at the middle position. Contraction of dorso-ventral muscles pulls the scutum plate down and drives the mechanism into upstroke. In the reverse stroke, dorso-longitudinal muscles contracts. When moved slightly beyond the meta-stable position, the wings tend to snap to the opposite extreme position. Effect of click on wing movement depends on system geometry [20]. The click mechanism store elastic energy in the deformed thorax (i.e. scutal spring and pleural spring in particular) during a quarter stroke from the extreme to middle position. The stored elastic energy is released upon click so that the wing moves at fairly constant speed in the next quarter stroke [20]. In addition, its elastic storage brings about rapid passive wing rotation during stroke reversal [90].

Elastic snap could help accelerate wings. However, it is not clear how the bi-stable elastic storage differs from mono-stable elastic storage in recovering the wing's kinetic energy and its release to affect wing kinematics. This investigation will investigate the effect of bistable elastic storage on the energetic cost for wing flapping. We shall show in the subsequent sections that the motor's power requirement for wing flapping reduces with bi-stable elastic storage. We shall also show that the same motor power can drive a bistable elastic mechanism into faster wing-flapping to produce higher thrust, as compared to that generated by the rigid-body counterpart.

7.2 Principle and Design of a Simple Bistable Flapping Mechanism

This work develops a simple bi-stable elastic mechanism as inspired by Dipteran insects' click mechanism (see Figure 7.2). This bi-stable elastic mechanism differs from the Dipteran insects' click mechanism in that: 1) it drives two wings at the same time, unlike a Dipteran click mechanism that drives a wing each; 2) its elasticity is contributed by elastic hinges between rigid plates, instead of the deformable thorax of Dipteran insect.

This mechanism consists of a symmetric pair of four-bar linkages as shown in Figure 7.2. The four-bar linkages are: a tergal joint (i.e. a slider), a wing base, a pleural process, and a pleural wall (i.e. an anchor). A wing is attached to the wing base which sweeps a wing stroke ϕ about a wing hinge upon tergal depression or elevation. The wing hinge and a pleural hinge, at the tip and the root the pleural process respectively, form a lateral spring to support the wing base.

This bi-stable elastic mechanism has two stable positions, namely 1) the wing-down position with two wing bases forming a 'Λ' shape (see Figure 7.2a left) upon the elevation of tergal joint; and 2) the wing-up position with the two wing bases forming a 'V' shape (see Figure 7.2a right) upon depression of the tergal joint. Elevation and depression of the tergal joint are achieved by

reciprocation of a motorized crank-slider mechanism along a vertical slot. A force is required to move the mechanism from the extreme position up to the meta-stable middle position before switching (snap) to the opposite extreme position.

Figure 7.2b shows that the linkages are made of carbon-fiber reinforced polymer plates; an elastic hinge is made of a polyimide film while the revolute joint is made of an acrylic tube and a steel pin. The wing hinges are designed to be soft such that the wing base can undergo a large-angle rotation during elastic toggling, but they do not store much elastic storage. The pleural hinges are designed to be stiff to store elastic storage upon its deformation. Then, the deformed pleural hinges provide a large lateral compression to snap the two aligned wing bases at the meta-stable position. There are two designs of elastic bi-stable mechanism developed: i) A soft design with a 2-mil-thick wing hinge and a 3-mil-thick pleural hinge; and ii) a stiff design with a 1-mil-thick wing hinge and a 5-mil-thick pleural hinge. They can produce the same wing stroke of nearly 110° upon tergum reciprocation for 10mm (using a 5mm long crank), but with different capacity for elastic storage.

Figure 7.2 (a) Principle of bi-stable elastic mechanism as inspired by Dipteran's click mechanism and (b) its construction using carbon- fiber reinforced plates, polyimide film hinges, and revolute tergal joint

The flapper is equipped with two identical wings, which are made of $25\mu\text{m}$ thick Mylar foil, which is reinforced at its leading edge and the root and its chords (see Figure 7.1). Each of the wings has a 6cm long leading edge, 2.1 cm long chord, and a moment of inertia of $244 \times 10^{-9} \text{kgm}^2$ about the root chord. It is attached to a wing base of this elastic mechanism. Passive wing rotation about the leading edge, is possible using a polyimide film hinge like that reported in Refs. [47, 98]. The maximum pitch rotation of the wing is limited not more than 60° by an

acrylic stopper at the wing root. This wing-flapper assembly (see Figure 7.2) weighs about 3.7-3.8 gram in total.

7.3 Static Analysis

Toggling of a bi-stable elastic mechanism requires force acting on the tergum joint. A pseudo-rigid-body model as shown in Figure 7.3 is developed to analyze the mechanism's static force-displacement behavior. It represents a half of the bi-stable elastic mechanism. It consists of a wing-base linkage of length L_D , a pleural-process linkage of length L_l , a wing spring of rotational stiffness K_1 , and a pleural spring of rotational stiffness K_0 , and a tergal joint (i.e. a slider). Rotational stiffness of the torsional spring is calculated from the dimensions and property of polyimide film hinge, following $K=EI/l$ [69] where l is the hinge length, I is the second moment of cross-sectional area about the film's neutral plane, and E is the Young's modulus of polyimide ($E=2.5\text{GPa}$).

(a) Pseudo-rigid model

(b) Free body analysis of linkages

Figure 7.3 (a) Pseudo-rigid model of a half of the bi-stable elastic mechanism and free body diagram of the individual linkages in the bistable mechanism half thorax in static equilibrium analysis. (b) Free body analysis of the linkages.

When not loaded, the tergal joint is located at a peak height y_0 measured from the anchor of the pleural hinge. Then, the pleural hinge is undeformed, and the pleural wall is vertically

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aligned with the symmetric plane at a distance W . The unloaded tergal height y_0 is related to the linkage dimensions following: $y_0 = \sqrt{l_b^2 - W^2} + l_p$. When depressed by a force F , the tergal joint moves downwards by a vertical displacement dy while the wing base rotates by an angle ϕ below the horizon and the pleural process rotates outwards by an angle α . As such, the tergum depression varies with the linkage rotations following:

$$dy = y_0 + l_b \sin \phi - l_p \cos \alpha \quad (7-1)$$

while the linkage rotation must satisfy the lateral dimension constraint:

$$W = l_b \cos \phi - l_p \sin \alpha \quad (7-2)$$

The wing mechanism has the following design parameters: $l_b=6\text{mm}$, $W=4\text{mm}$ and $l_p=4\text{mm}$ so that it produces 110° wing stroke when the tergum moves $dy=10\text{mm}$ between the two extreme positions, i.e. either wing up or down. The pleural walls remain vertical ($\alpha=0$) at the two extreme positions, but they are bent most at $\alpha=30^\circ$ at the middle position where the wings are orientated horizontally (see Figure 7.2a).

Consider a pseudo-rigid-body model (see Figure 7.3) that represents a symmetric half of the bi-stable elastic thoracic mechanism subjected to depressive force F acting on the tergum. Reactions at the wing hinge 1 are R_{x1} , R_{y1} and Γ_{x1} ; reactions at the pleural hinge 1 are R_{x0} , R_{y0} and Γ_{x0} ; and horizontal reaction at the tergal joint is R_{x2} .

Force equilibrium of the free-body link l_b yields:

$$R_{y1} = F/2 \quad (7-3)$$

$$R_{x1} = R_{x2} \quad (7-4)$$

Moment equilibrium of the link l_b about wing hinge 1 gives:

$$Fl_b \cos \phi + R_{x2}l_b \sin \phi - \Gamma_{K1} = 0 \quad (7-5)$$

The moment induced Γ_{K1} to the wing hinge is:

$$\Gamma_{K1} = K_1 \left(\frac{\pi}{2} + \phi + \alpha \right) \quad (7-6)$$

Force equilibrium of the link l_p yields:

$$R_{y0} = R_{y1} = F/2 \quad (7-7)$$

$$R_{x0} = R_{x1} = R_{x2} \quad (7-8)$$

Moment equilibrium of the link l_p about pleural hinge 0 gives:

$$R_{x1}l_p \cos \alpha + R_{y1}l_p \sin \alpha - \Gamma_{K1} - \Gamma_{K0} = 0 \quad (7-9)$$

The moment induced Γ_{K0} to the pleural hinge

$$\Gamma_{K0} = K_0 \alpha \quad (7-10)$$

The force required to depress the tergum of this bi-stable elastic mechanism is obtained as

$$F = \frac{2K_1 \left(\alpha + \phi + \frac{\pi}{2} \right) - 2 \frac{l_b \sin \phi}{l_p \cos \alpha} \left[K_0 \alpha + K_1 \left(\alpha + \phi + \frac{\pi}{2} \right) \right]}{l_b (\cos \phi - \tan \alpha \sin \phi)} \quad (7-11)$$

from the free-body analysis of the pseudo-rigid-body model.

Figure 7.4 shows the force required to buckle a stiff design of bi-stable click mechanism, from the tergal top position to the bottom position. The two extreme positions are subjected to zero force but stable with positive stiffness. The perturbed tergum tends to return to the stable position upon the perturbation removal. A positive external force (downward) is required to depress the tergum from the top stable position up to a meta-stable position. The mechanism has a negative stiffness at the meta-stable position. When perturbed beyond the meta-stable position, the mechanism snaps to the opposite stable position without additional external force. If the tergum is restrained from snapping, it will induce a negative force acting on the external restraint. Figure 7.4 shows that the theoretical prediction agrees in trend with the measured force-displacement curve but does not account for actual friction and free-play of the tergal joint.

Figure 7.4 Force required to depress a stiff bi-stable mechanism.

7.4 Rigid-body Benchmark

For comparison, we also designed a rigid-body wing mechanism that moves the same wing-stroke angle as the bi-stable elastic mechanism does. It has the same kinematics, and linkage dimensions as the elastic mechanism, but it has revolute joints replacing the elastic hinges of the elastic mechanism. This rigid-body-mechanism is incapable of elastic energy storage, and it is subjected to joint friction. It can stay at any intermediate position without elastic resistance. Linkages of this rigid-body mechanism are made of acrylic rods laser-cut from a 3mm plate.

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Their revolute joint consists of a drilled hole through the rod and a steel pin. This wing-flapper assembly weighs 3.3 gram in total.

Figure 7.5 (a) The rigid-body mechanism made of acrylic linkages and frame, and pin joints; (b) its assembly in a wing flapper.

7.5 Results and Discussions

The static force required to depress a bi-stable elastic mechanism varied with the tergal position and the stroke direction. When not loaded, the tergal is located at its top stable position. A downward force, as exerted and measured by the INSTRON, is required to depress the tergal towards the middle position, doing positive work. The elastic force however vanished at the middle meta-stable position. Beyond the middle position, the elastic mechanism tends to pull the INSTRON towards the bottom stable position. If not stopped by the INSTRON, the tergal would have snapped to the bottom stable position. The restrained tergal induces a negative force to pull the INSTRON, resulting in a negative work over the quarter stroke where the snap could have occurred if the tergal were free.

In a complete cycle of wing stroke, this elastic mechanism is subjected to hysteresis (see Figure 7.6) due to the friction and free-play at the tergal joint. In the half cycle for tergal depression, the magnitude of positive work done (from the top end to the meta-stable) is higher than the magnitude of the negative work done (from the meta-stable position to the bottom end). A similar trend of positive and negative works was observed in the reverse half cycle for tergal elevation. In addition, the magnitude of positive works does not change much with respect to the stroke direction (either tergal depression or tergal elevation).

Figure 7.6 (a) the measured force as a function of tergum position for a complete wing stroke, i.e. tergum depression and elevation; (b) snapshots of the stiff bistable mechanism during tergum depression. (c) Snapshots of the stiff bistable mechanism during the snap.

Figure 7.7 (a) Wing angle and tergum displacement over time during the snap of the bistable mechanism. (b) Wing angular velocity over time during the snap. (c) High speed snapshots of the stiff bistable mechanism during the snap.

If the tergum is free to snap (see Figure 7.7), the elastic storage resulted from the positive work will be released to accelerate the wing, converting into wings' kinetic energy. For example, the 1.43mJ positive work done on the stiff mechanism is released to accelerate the wing up to

a maximum wing speed of 42.28rad/s. Similarly, the 1.08mJ work done on the soft mechanism produces a speed gain of up to 30.56 rad/s.

As designed, these mechanisms, either bi-stable elastic ones or rigid-body one, sweep approximately the same wing stroke of 110° when they are driven by the same motorized crank-slider mechanism. Figure 7.8 showed the wing-stroke angles do not change much with increasing wingbeat frequency.

Figure 7.8 Comparison of wing stroke variation along frequency of the flappers.

Figure 7.9 showed the wing kinematics of flappers a 13-14Hz. Their time profiles of wing angular position were observed to be similar at 13-14 Hz wingbeat frequency. It is noted that the maximum wing speed generated by the bi-stable elastic mechanism is higher than that generated by the rigid-body mechanism. The release of elastic energy is confirmed to lead to the speed gain. Unlike the asymmetrical stroke predicted for Dipteran click mechanism [89], this bi-stable elastic mechanism exhibited symmetrical stroke at 13-14Hz, where the down stroke duration is same as upstroke duration. The stiff bi-stable elastic mechanism produced a stronger passive wing rotation (larger amplitudes for longer duration) as compared to the soft one or rigid-body one does. However, this stronger passive wing rotation yield higher thrust (See Figure 7.12) because its phasing did not always favor thrust generation. For the best thrust generation, the translating wings should have symmetric rotation [27, 63].

Capacity for elastic storage is constant for a design of bi-stable elastic mechanism. The elastic energy (calculated from Equation (4-3)) is independently of the wing beat frequency given a frequency-independent tergal stroke. In contrary, kinetic energy of flapping wings (calculated from the measured wing speed) increases quadratically with the wingbeat frequency (see Figure 7.10). The elastic storage helps recover the wings' kinetic energy. This is expected to help reduce the reactive load and save energetics cost to the driving motor. With its capacity of elastic storage, the soft mechanism can recover average kinetic energy of 16Hz flapping wing; whereas, the stiff mechanism can recover higher wings' average kinetic energy at 18Hz wingbeat. The maximum kinetic energy occurs as the wing speed peaks as a transient. When

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the wingbeat frequency is higher than 13Hz, the maximum kinetic energy of the flapping wings can well exceed the capacity of elastic storage.

Figure 7.9 (a)-(d) shows the snapshots of a wing on the stiff bistable mechanism during its downstroke at 14.60Hz wingbeat frequency. (e) The corresponding wing chord attitude of the stiff bistable mechanism, as viewed from A in the snapshots. (f) stroke angle (g) stroke velocity (h) wing pitch (i) wing pitch velocity of the flappers in a wingbeat cycle at 13-14Hz.

Figure 7.10 Average wing kinetic energy and maximum kinetic energy along frequency for each flapper, compared to the elastic energy storage in the bistable mechanism.

Figure 7.11 (a) Wingbeat frequency as a function of electrical power input to motor for driving various flapping mechanisms. (b) Instantaneous power requirement over a cycle of wingbeat (at 13.3Hz frequency).

Figure 7.11 shows that the wingbeat frequencies of these wing mechanisms increase at decreasing rates with increasing power input to the driving motor. As the wing beat frequency increases above 10 Hz, the wings' inertia loading increases and the elastic storage becomes necessary to saving inertial power. As a result, 13.3Hz driving of the elastic mechanisms requires much less power (1.17W for the stiff click and 1.36W for the soft click). In contrary, driving of the rigid-body mechanism require more energy, i.e. 2.16W at the same frequency (196% of the stiff click power). As a result of reduced reactive load, the motor can drive the elastic mechanism up to a higher frequency at its maximum capacity at 5.4V though a higher starting torque (i.e. a higher starting voltage) is required to start the mechanism.

Figure 7.12 (a) Mean thrust generation along wingbeat frequency (b) Mean thrust generation along electrical power input to the motor of the bistable and rigid body mechanisms.

Figure 7.12 shows that the average thrust generation increases in a nearly quadratic trend with increasing wingbeat frequency. The various wing mechanisms produce almost the same average thrust at the same wingbeat frequency due to similar wing kinematics and nearly the same average wing speed. However, the amount of elastic energy storage has an impact on the maximum motor rpm and thus the maximum thrust produced by flapping wings. The elastic mechanisms can beat the wings faster and produce a higher thrust than the rigid-body mechanism does when they are driven by the same motor at the maximum voltage. Among the two elastic bi-stable mechanisms, the stiff elastic mechanism performs better than the soft one in terms of the maximum achievable wingbeat frequency and thrust generation. For example, the stiff elastic mechanism can produce a maximum thrust of 2.18 gram force at 5.4V, as compared to 1.45 gram force achieved by the rigid-body mechanism and 1.85 gram achieved by the soft elastic mechanism. In short, the elastic energy storage helps the wing mechanism achieve a higher thrust-to-power ratio.

7.6 Comparison with Mono-stable Elastic Storage

This stiff design of bi-stable elastic mechanism can store nearly 3 times higher elastic energy at lesser tergum force as compared to the reported monostable elastic mechanism [97], does at the same tergal displacement of 5mm. This means a higher bi-stable elastic storage does not required a higher motor torque. After all, the maximum inertial power and maximum thrust generated are limited by the motor capacity. Interestingly, this bi-stable mechanism appears good for ‘slow’ wing flapping towards the maximum thrust generation. It can drive 3-times heavier wings to deliver the maximum wing kinetic energy at a lower maximum wingbeat frequency.

Table 7 Performance comparison between the mono-stable mechanism [97] and this stiff bi-stable mechanism.

Characteristics	Mono-stable mechanism [97]	Stiff bi-stable mechanism
Tergum reciprocation	5mm × 2	5mm × 2
Maximum tergum force	490.5mN	440 mN
Positive work done on Tergum	0.49 mJ	1.43mJ
Rotational Inertia of each wing	$72.1 \times 10^{-9} \text{ kg.m}^2$	$244 \times 10^{-9} \text{ kg.m}^2$
Max. wingbeat frequency @ 5.4V	33Hz	17Hz
Average wing speed @ 5.4V	102.35 rad/s	74.85 rad/s
Average wing kinetic energy @5.4V	0.405mJ	1.151mJ
Maximum thrust generated @ 5.4V	26.6 mN	21.8 mN
Maximum power required @5.4V	1.55W	2.25W
Average Wing KE/power@5.4V	0.261mJ/W	0.512mJ/W

7.7 Conclusion

Bi-stable elastic mechanism can store higher elastic strain energy at the lesser tergum force, as compared to the mono-stable elastic mechanism does at the same tergum displacement. The higher elastic energy storage helps recover the wings' kinetic energy and enable faster wing speed to generate a higher thrust, given the same motor capacity. Though the bi-stable elastic mechanism was inspired by the click mechanism of tiny Dipteran insects, it could possibly work towards the benefit of larger flapping wings.

7.8 Future Work

The bistable elastic mechanism has potential for large wing beat mechanism for two wings. Figure 7.13 shows a stiffer bistable mechanism was built and driven by brushless motor to flap two 14cm-long wings with 51mm-long root chord at a large stroke of 107° . It generates a maximum 30g of thrust at 13.7Hz when tethered. The wings were slacked like that of Nano-hummingbird to allow for passive wing rotation, but the root chord is fixed at 90° angle of attack. However, this two-wing flapper creates a large oscillatory drag force that rocks the body in opposite. Without tethering, the fuselage body containing the motors and mechanism rock violently forth and back in reaction to the wing drag. A future hovering prototype will consider active wing rotation mechanism, so that wings pitch to lower angle of attack to minimize drag. Another approach to avoid body rocking would be the use of X-wing in the next chapter.

Figure 7.13 (a) A bistable mechanism flapping a pair of 14cm long wings. (b) The stable wing down position Δ (c) The meta-stable position where the wings could snap to either Δ or V position. (d) The stable wing up position V. (f) The wing stroke cycle at lowest starting wingbeat frequency of 5.2 Hz and highest achievable frequency 13.7 Hz. Frequency-dependent performance: (g) The wing stroke 2Φ , (h) mean thrust, (i) mean electrical power and (j) Thrust to mean electrical power ratio. (k) Mean thrust generated by mean electrical power input.

Chapter 8 X-wing with Polyimide Film Hinges

Most of the hovering FWMAVs flap four wings. The four wings are often made of two wing planes that crisscross in an X-shape, like the projection of two tandem wings on to the same stroke plane. These X-wing FWMAV adopt insect's feature to enable hovering flight. First, the X-winged MAVs have the same number of wings as dragonfly's. Second, the X-wing span as large as the Atlas moth's wings (250-300mm) [99, 100]. Third, the X-wing claps and fling like a butterfly's wings [1, 9]. In addition, X-wing FWMAVs show good flight stability by rotating two pairs of wings in countering directions, minimizing body rocking, which is more pronounced in two-winged ornithopters.

In this study, we investigate whether partial elastic storage can help a motorized transmission to save power for flapping flight by four wings (see Figure 8.1). This partial elastic storage is realized in the form of a pair of film hinges, each of which support a diagonal wing pair. The stiffness of the film hinge will be designed such that the natural frequency of the hinged wings is lower than the operational wingbeat frequency. We shall show the effects of partial elastic storage on wing kinematics, thrust generation, and power expenditure. Finally, we shall benchmark the performance of this spring-assisted flapping-wing transmission against a propeller under the same motor drive.

Figure 8.1 X-wing prototype with spring-assisted motorized transmission: (a) a complete assembly without tail; (b) weight components breakdown (c) angled view and (d) side view photographs of the wing transmission (e) CAD model of the wing transmission in angled view; (f) side view (g) front view and (h) rear view of the schematic drawings of the wing transmission

This 13.4 gram prototype of fwMAV is powered by a 3.18g brushless motor, a 2.72g lithium-polymer battery, and a 2.72g electronics. The wing-transmission include a 1.42g transmission gearbox (inclusive of a double crank-rocker mechanism), two elastic hinges of 0.8g in total, and four flexible wings of 1.8g in total, and a 1.18g fuselage. This prototype of fwMAV can only perform a guide-wire take off with onboard battery but not a free flight yet without a tail. Details of wing-transmission design are elaborated in the following subsections while and experimental setup is elaborated in Section 5.2 in reference to Figure 5.2.

8.1 Ultra-light Flexible Wing Design

Figure 8.2 Design and construction of a light wing plane: (a-b) schematic drawing of the wing film and spar assembly; (c) photograph of two overlapped wing planes with carbon spar and rib reinforcements; (d) design dimensions of the wing film; (e) photographs of the components and its weight.

Figure 8.2 shows construction of four vein-reinforced wings from two wing planes but in a complex assembly. Major components of the four wings are two leading-edge spars of full span, and two planforms of wing foil of full span. The wing foil is made of 23 μm thick Mylar film. The leading-edge spars are 1mm diameter carbon rods while the chordwise reinforcement veins are 0.5 mm diameter carbon rods.

The two leading spars are staggered axially (front and rear) to allow room for criss-crossing upon crank-rocker reciprocation. A common wing planform was used to make two wings (say the top ones) but the left and right leading ledges were supported by two separate leading-edge spars, front and rear. The top wing pair form a V shape when the leading-edge spars cross. Similar construction was applied to the bottom wing pair that form a Λ shape when opened;

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Figure 4d shows the planform of a monolithic Mylar film that constitutes a wing pair, either top or bottom. The planform has the left leading edge tilted by an angle from the right one. The leading edges of the wing planform were folded and adhesively pasted into sleeves. Alignment of these two angled sleeves by slotting two parallel leading-edge spars introduces a slack to the wing film.

Reinforcement veins of a wing foil were layed out like bat wings' skeletal structure. The chordwise ribs radiate from nearly a quarter span from the leading-edge spar, one being parallel to the chord and one being 45° off the chord. The chordwise ribs are minor carbon rods adhesively taped onto the wing film. They are elastically disconnected from the leading-edge spar. Such assembly [38, 101] allows passive rotation. This flexible wing shows a spanwise distribution of spanwise distribution of wing chord rotation: being zero the root chord but being the maximum at the tip chord [64, 67]. This is unlike a constant chordwise rotation of rigid flapping wings [102, 103, 104].

Each of the flexible wings weighs approximately $m=0.635\text{g}$. It consists of a leading-edge spar of an $R=120\text{mm}$ half span and a $m_{c0}=0.145\text{g}$ weight, two chordwise ribs of less than 80mm long and $m_{c1}=m_{c2}=0.06\text{g}$ each, and a Mylar film of a half span and $m_f=0.37\text{g}$ each. Approximated by a prismatic shape, the rotational inertia J_0 of this flexible wing in the stroke plane is calculated to be:

$$J_0 = \frac{1}{3}m_f R^2 + \frac{1}{3}m_{c0} R^2 + m_{c1} \left(\frac{R}{2}\right)^2 + m_{c2} \left(\frac{3R}{4}\right)^2 \quad (8-1)$$

which is equal to $3.093 \times 10^{-6} \text{kgm}^2$.

Load capacity of this flexible wing (of half span) comes from the rigidity of leading-edge reinforcement (major spar) and the root chord reinforcement (fuselage). While the wing sweeps along the stroke plane, the inertial load of wing foil acts on the leading-edge spar. Hence, the leading-edge spar can flex like a cantilever under a distributed inertial load. Here, the transverse (out-of-stroke-plane) linear stiffness was measured to be $k_y=6.74 \text{ N/m}$ while the mass m is contributed by the whole vein-reinforced wing. To avoid excessive vibration, the leading-edge spar should not be reciprocated faster than the natural frequency $f_{n,\text{spar}}$:

$$\begin{aligned} f_{n,\text{spar}} &= \frac{1}{2\pi} \sqrt{\frac{K_y}{m/3}} \\ &= \frac{1}{2\pi} \sqrt{\frac{6.74 \text{ N/m}}{0.635 \times 10^{-3} \text{kg}/3}} = 28.0 \text{Hz} \end{aligned} \quad (8-2)$$

This estimate however neglects the effect of film tension and air's added mass. Prediction of wing flutter is beyond our scope.

8.2 Hinge Design

This experimental study investigates the effect of elastic storage on thrust generation by four flapping wings and power expenditures incurred to the motor that drives the wing transmission. Here, we tested two levels of elastic storage, soft polyimide hinges of 10 mm wide polyimide hinges and moderate-stiffness polyimide hinges of 20 mm wide, both of 5 mils thick and 1mm long. To isolate the spring effect, the same motorized transmission was used in the static flight test. The wing-transmission were tuned to produce the same wing kinematics by shifting the pivot arm, d of wing base. Nevertheless, later experiment shows elastic storage does impact on wing kinematic due to transverse flexibility of film hinge and passive wing rotation.

Figure 8.3 A prototype X-wing clapper with elastic hinge. Top row – closing position of two wing leading edge spar. Bottom row – opening position of two wing leading edge spar.

Motivated by the success of DelFly [38] and Mentor [43, 105] in achieving hovering flight, we adopted four flexible flapping wings for this study. Figure 8.1 shows a spring-assisted wing-transmission for driving four flapping wings of 240mm span. These four wings are grouped to into two diagonal pairs, each of which share a common leading-spar of full span. These four flexible wings can open and close upon reciprocation like scissor wings of Luna [73] or DelFly. When being opened, the four wings altogether form a X shape; when being closed, they become parallel.

Figure 8.3 shows two pair of hinged wings. These pivotal hinges consist of a mirror pair of polyimide film hinges which provide elastic storage for flapping wings. Each film hinge can be idealized as a pivot with a torsional spring. These film hinges are compact (1mm long) and lightweight (0.8 gram) to fit axially along the fuselage, as compared to a more conventional coil spring that has a much larger outer diameter that would not be a compact pivot. [46, 56, 84,

97]. The film hinge is adjustable by width. The unloaded position of the hinged wing is located near the horizontal plane towards which the spring recoil accelerate the closing of wings.

Here, a film hinge was used to provide a pivotal support and elastic storage to a diagonal pair of flexible wings, which shared a common leading-edge spar. The film hinge is connected between a movable swing plate and an anchor plate. The film hinge consists of a strip of a 0.127 mm thick polyimide film (5 mils thick DuPont Kapton HN05) [70] and two carbon-fiber reinforced patches [97, 98]. The non-reinforced film makes a flexible hinge while the carbon-fiber reinforced patches make the plates. Here, a polyimide film (Kapton HN05) was selected because it is tough with high folding endurance and can withstand up to 72% ultimate elongation.

Here, we designed a 1 mm long ($L=1$ mm) hinge using a polyimide film of 5 mil thick ($h=0.127$ mm) to achieve an elastic bending of:

$$\begin{aligned}\beta_h &= \pm \frac{2l}{h} \varepsilon_{yield} \\ &= \pm \frac{2 \times 0.03 \times 1 \text{ mm}}{0.127 \text{ mm}} = \pm 27^\circ\end{aligned}\tag{8-3}$$

based on 3% yield strain. In order to adjust the neutral wing-closing position to be the horizon, the film hinge was pre-bent for 90° and plastically set (without post annealing). Once set, bending about the new set position can be reversible within the elastic limit as calculated above. For example, a 20° wing stroke amplitude should be within the elastic limit.

Level of elastic storage is adjustable by changing the width of film hinge, without compromising the angle of elastic bending. For example, a design with a 20mm wide film was found to be adequately stiff while not impeding the electric motor from starting the flapping wing transmission. The torsional stiffness of a 20mm film hinge is calculated from (4-1) to be 8.53×10^{-3} Nm/rad. Transverse stiffness of this film hinge is much smaller than the rigidity of leading-edge spar.

When dislodged from a motorized transmission, a hinged wing pair is free to oscillate. The natural frequency of this hinged wing is estimated to be:

$$\begin{aligned}f_r &= \frac{1}{2\pi} \sqrt{\frac{K}{2 \times J_0}} \\ &= \frac{1}{2\pi} \sqrt{\frac{8.53 \times 10^{-3} \text{ N/m}}{2 \times 3.093 \times 10^{-6} \text{ kgm}^2}} = 5.9 \text{ Hz}\end{aligned}\tag{8-4}$$

When lodged to a crank-rocker mechanism, the hinged wing is not free to oscillate; it can only follow the rocker motion under harmonic drive by the motorized transmission. Though

resonant drive may not cause magnify amplitude to the hinged and constrained wing pair, it may help reduce the load-torque requirement according to Equation (3-34).

8.3 Transmission Design

A brushless motor can directly drive of a propeller for rotary flight; but its torque generation is not enough to for direct drive of flapping wings, which are larger than propeller. To the solution, a transmission mechanism can be used to magnify the output torque for indirect drive of flapping wings. Here, the transmission we designed consists of a two-stage compound gear train and a crank-rocker mechanism (see Figure 8.2 g-h). In this assembly of transmission, gears were slotted into and rotate about fixed pins of gearbox housing while each wing base reciprocates about a film hinges following the rocker motion. Toggling of the crank-rocker mechanism does not encounter any singularity with the aids of film hinge flexibility.

A two-stage compound gear train was used to provide the primary speed reduction. It consists of two single stages in series. Components of the gear train are: a 9-tooth pinion on an input shaft, a compound gear which has a 24-tooth gear, an 8-tooth gear fixed on the same shaft and finally a 32-tooth gear on the output shaft. The first stage has the input pinion driving the 24-tooth gear of the compound gear. In the second stage, the 8-tooth gear of the compound gear drives the output gear. This gear train has a total gear ratio of:

$$G. R. = \left(\frac{24}{9}\right)\left(\frac{32}{8}\right) = 10.67 \quad (8-5)$$

This reduces the wing beat frequency to be $1/G.R.$ of the rate of motor revolution.

The double crank-rocker mechanism was used to convert a rotary motion into a rocking motion [38, 43]. Its effect on torque magnification is however often neglected. Upon a half turn by the output gear, the rocker reciprocates the wing for a $2\Phi=40^\circ$ stroke angle in a half wingbeat cycle (i.e. either opening or closing). Based on the principle of virtual work [35], a crank-rocker angular ratio for torque magnification is:

$$C. R. = \frac{\pi}{2\Phi} = \frac{\pi}{\frac{40}{180} \times \pi} = 4.5 \quad (8-6)$$

Let us consider the use of a 3.1g brushless motor (AP03-7000). A reference torque-speed curve for this motor at a rated voltage of 4V is marked by a stall torque of 2.8mNm and a no-load speed of 28,000 rpm [61]. Due to $C. R. \times G. R. = 48$ times magnification, the maximum output torque of the rocker is 134.4mNm. Meanwhile, the no-load rocking frequency was estimated to be 43.7Hz based on $G.R. = 10.67$.

While supporting four X wings, two film hinges present a maximum static elastic load of

$$\begin{aligned} Q_{elastic} &= 2K \times 2\beta_h \\ &= 2 \times 8.53 \times 10^{-3} \times 2 \times 0.47 \text{ rad} = 16.0 \text{ mNm}, \end{aligned} \tag{8-7}$$

to stall a driving motor. With the speed reduction described above, the apparent elastic load is small relative to the motor's stall torque. However, the elastic load adds to the normal loads between gears and thus unavoidably increase the friction load. Our initial tests show that this motor (AP03-7000Kv) cannot start a 30mm-wide film hinged transmission with four flapping wings.

8.4 Test Procedures

First we measure static deformation of a film hinge by applying increments of deadweight at the quarter span of the leading edge spar, which was supported by the hinge. In this case, the hinged wing was dislodged from the crank-rocker mechanism while the anchor of the hinge was clamped together with the gearbox housing. A digital single-lens reflex (DSLR) camera (Canon) was used to picture the static deformation. Bending angle of the film hinge was measured between the inclination of leading-edge spar and a vertical reference of the hinge anchor. The applied moment is calculated as the applied weight and the moment arm distance.

Second, we measure transient recoil of a pair of hinged wings as well as harmonic reciprocation of four wings. A high speed camera (Photron FASTCAM 1024) was used to record high-speed footage of dynamic wing motion. Frame rate of the high-speed footage was set to be 2000 frames per second while each frame had 512×512 pixels. To assist motion tracking, the leading-edge spar of flapping wing was marked with two white dots. The stroke angle was measured between the inclination of leading-edge spar and a horizontal reference toward which wings close. Pitch angle of wing rotation is calculated from the arcsine function of relative projected chord length at the quarter span.

Third, we measure the static thrust generation by a tethered wing-transmission powered by external supply. The experimental setup is described in Section 5.2. Steady-state reading during the static thrust test was logged for at least 8 seconds after 3-5 seconds drive of the wing transmission. This yielded repeatable steady-state readings with little variation. The recorded signals for power and thrust measurement will be presented unfiltered.

Fourth, we test the fwMAV prototype for a vertical takeoff along a guiding wire. This test is powered by an onboard lithium polymer battery of 2.30g. Voltage output by the lithium polymer battery drop while it discharges. Output voltage of a fully charged battery is 4.2V, whereas the nominal voltage output is lower at 3.7V. Hence, an FWMAV powered by onboard lithium

polymer batter may produce less thrust as compared to its ground flight test with a constant voltage supply.

8.5 Elastic Hinge Properties

Four hinged wings are closed near the horizontal plane when they are dislodged from the crank-rocker mechanism. Figure 8.4(a) shows a diagonal pair of wings (i.e. bottom right and top left one) opening to 40° upon application of a deadweight at a quarter span on the common leading-edge spar. Figure 8.4(b) shows the moment required to bend the hinge for opening the wing pair. Rotational stiffness of a 20-mm wide hinge (of 1 mm long and 0.127 mm thick) was measured to be 11.20 mNm/rad while that of a 10-mm wide hinge is softer at 3.22 mNm/rad.

Recoil happens to the hinged wing pair when the deadweight was removed suddenly. Figure 6c show the bottom right wing returned to the spring's unloaded position. Figure 6d shows the recoil and subsequent damped free-oscillation of the hinged wing pair. Damped natural frequency of this free oscillation was measured to be 5.0Hz for 20mm-hinged wings. In comparison, recoil of the 10mm-hinged wings is slower, taking double time to overshoot. In short, 20mm hinged wings are subjected to higher elastic load but stronger recoil as compared to the 10mm hinged ones.

Figure 8.4 Static deformation and recoil of a polyimide film hinge that supports a wing pair: (a) Static deformation under a deadweight; (b) the applied moments required to bend the film hinge; (c) a snap shot of the recoil post the dead weight release; (d) transient of recoil in terms of wing stroke angle.

8.6 Wing Kinematics

Figure 8.5 shows four flexible wings opening and closing like scissors under reciprocation. The wing opening (e.g. from a low stroke angle to a high one) takes longer than the wing closing. This duration asymmetry increases with increasing the wing beat frequency. For example, as shown in Figure 8.5(b), the opening duration took 52% cycle for 13.0 Hz wing beat; it increased to 63% cycle for 17.09 Hz wing beat. This is due to the load asymmetry which will be confirmed by power measurement between wing opening and closing (see Figure 8.7(d)).

Figure 8.6 shows the effect of elastic storage on wing kinematics. Two levels of elastic storage were tried, one using 10mm wide hinges and the other using 20mm wide hinges. The 20-mm-wide hinged wings was tuned to produce almost the same dynamic wing stroke angle as the 10-mm-wide hinged wings do. As observed, dynamic stroke amplitude taper down to 20° with increasing the wing beat frequency. Due to wing reciprocation and inertial effect, passive wing rotation rises up to 30° with increasing the wing beat frequency. In general, both the stroke speed and pitch speed are proportional to the wing beat frequency.

The elastic load and recoil were found to change the full-throttle wing beat frequency under indirect drive by the motor. For example, full-throttle wing beat frequency achieved by the 20mm hinged wings is slightly lower than that achieved by the 10mm hinged ones. Interestingly, more the forceful recoil of the 20mm-wide hinges apparently increases the pitch speed of flapping wings. For example, the peak pitch speed of 20-mm hinged wings is up to 90 rad/s at 17 Hz while that of 10-mm hinged wings is up to 80 rad/s at above 20 Hz

Figure 8.6(e,g) showed that increased elastic load leads to more obvious duration asymmetry between wing opening and closing. For example, the 10mm-wide hinged wings takes 0.56 cycle at 17.1 Hz for wing opening; whereas, the 20mm-wide hinges wing takes 0.63 cycle. This is similar to the effect of increased aerodynamic load (by faster wing beat) as seen in Figure 8.5(b,d). This cross examination suggests that the elastic storage to flapping wings could act analogously to a gear, reducing speed and increasing torque magnification. In this way, elastic storage helps indirect drive of flapping wings generate a higher thrust at a lower wing beat frequency.

Figure 8.5 (a) Wing angular position and (b) Wing angular velocity in a wingbeat cycle at different wingbeat frequencies. (c) Wing stroke along wingbeat frequency for 2cm hinges and 0.5cm hinges. (d) Pitch angle and (e) speed as measured from the mid-chord rib of one of the four wings.

Figure 8.6 The effect of hinge stiffness on frequency-dependent wing kinematics: (a) amplitudes of stroke angle and (b) speed at approximately 17Hz; (c) amplitudes of pitch angle and (d) speed at approximately 17Hz; frequency-dependent amplitudes of (e) stroke angle and (f) speed; frequency-dependent amplitudes of (g) pitch angle and (h) pitch speed.

8.7 Thrust Generation and Energetic Cost

Figure 8.7 shows the influence of hinge stiffness on the drive of four flapping wings for thrust generation. As a motor indirectly drives four hinged wings beating faster, it consumes more electrical power to generate higher thrust. As described earlier, elastic storage presents asymmetric elastic load to the driving motor; it slows down the wing opening but accelerates the wing closing. As expected, a higher power is required to drive the wings opened but a lower power is required to drive the wings closed. Interestingly, this load asymmetry does not raise

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the mean driving power requirement and the spring recoil indeed helps enhance thrust generation by flapping wings.

Figure 8.7(b, d) shows the transients of thrust generated and power consumed while four wings on 20mm-wide hinges beat at 17.0 Hz. Then, the four wings can generate an 18.8 g mean thrust when beating at approximately 17.34 Hz under a 4.17 W mean driving power. Figure 8.7(a) shows the full-throttle drive of these flapping wings can generate a maximum mean thrust of 21.2 g and consumes 5.8 W of electrical power. Then, the thrust per unit electrical power decreases from 4.5 g/W to 3.7 g/W as the thrust generation increases from 18.8 g to 21.2 g.

Figure 8.8 shows the frequency dependence and transients of power components incurred by a motor for driving a transmission with 20mm-wide hinged wings. The mean shaft power is found to be only a fraction (not more than 60%) of the total electrical power. As thrust generation increases, resistive power loss can be more than the mean shaft power output. Figure 10b shows that the power components incurred for two cycles of wing beats. The power transient shows that wing opening requires more shaft power than wing closing. This power variation indicates the load variation within a wing beat cycle.

To show the effect of elastic storage, Figure 8.7(a, c) also compared the performance of a four-winged 10mm-wide hinged mechanism with that of the 20mm-wide hinged one. It is noted that the 10mm-wide hinged unit produces less thrust but consume more power under the same indirect drive by the motor. For example, the 17 Hz beating of the 10mm-wide hinged wings produced merely a 15.2 g mean thrust at a higher driving mean power of 4.2 W. The maximum thrust generation by 23 Hz flapping increased to 18.3 g and consumed 6.23 W of electric power. While the thrust generation increased from 15.2 g to 18.3 g, the electric-power specific thrust generation decreases from 3.6 g/W to 2.9 g/W. Towards full throttle, this wing transmission with soft hinges was observed to undergo gear slippage as plastic gears warped.

Figure 8.7 (a) Mean lift or thrust generation along frequency (b) Time history of lift measurement at 17 Hz. (c) Mean electrical power input along frequency (d) Time history of electrical power input at 17 Hz.

Figure 8.8 Power components incurred by a brushless motor for driving a transmission with 20mm-wide hinged wings: (a) frequency dependence, (b) transient powers incurred for beating wings at 17.0 Hz for two cycles.

Figure 8.9 The PI-hinge X-wing clapper is able to takeoff and climb along guide wire, with a weight of 13.4g including a 2 g 70mAh Li-Po battery.

At a maximum thrust of 20.7g, the X-wing with 2cm hinges is able to produce sufficient thrust to lift off and climb. In a guide-wire flight with onboard ESC, receivers and battery, the X-wing could lift its weight of 13.4g and climb, as shown in Figure 8.9.

8.8 Challenges Identified

Despite showing the benefit of elastic storage, the PI hinge clapper did not generate high lift at high thrust-to-power efficiency compared to propellers. Its maximum lift generation is only about 20 gram-force, half of the maximum 40 gram-force generated by the direct-driven 3020 prop of the same motor. We reasoned that the wing stroke is small at about 40° per wing and the wings are not large enough to generate higher lift at this low wingbeat frequency.

To hover freely, the X-wing have to carry additional structures such as servos and tails. We estimate a total body mass of 26g for a hovering X-wing with a 6g battery included. This mass exceeds the maximum 20g lift capability of this 20mm-wide-hinged X-wing driven by the AP03-7000kv brushless motor. It is reasonable to expect a flapping wing can at least generate same amount of lift as propeller given the same motor capability. However, this requires proper sizing of the wing span and motor load matching.

In addition, we need to redesign the transmission to be stronger to hold gears in alignment. This is critical to prevent gear slippage in the gearbox at high throttle and transmit the motor output power effectively to the wing.

These challenges are overcome in the next chapter.

Chapter 9 Overcoming the Energetic Limit of Four-Wing Motorized Ornithopter to perform Powerful Aerobatics

This chapter presents the first elastic hinged clap-and-fling X-wing which is capable of hovering freely and perform aerobatic flight. This X-wing has a crisscross pair of 280mm-span wing, each supported by a pair of parallel nylon spring hinges. A cross tail with elevator and rudder control surfaces controls the pitch up and down, yaw left and right of the X-wing. The X-wing with these control surfaces can turn sharply and recover from nose-down dive.

There are few strategies to make flapping flight efficient with increased propulsion. First, toggle from energetic costly hovering to forward flight promises power saving or lift enhancement. This power saving is analogous to the toggle between vertical takeoff with powerful propeller and forward flight with fixed wing. Yet, the actual power saving by toggling the modes of flapping flight was not reported. Second, aerodynamic efficiency improves with the low disk loading [43, 106]. Meaning, large slowly-flapping wings requires less aerodynamic power to generate the same thrust as compared to small fast-flapping wings. Of course, use of more number of small wings do help reduce the disk loading. However, multiple or large wings add weight and they incur more inertial power loss when wings decelerate. In addition, they require high leverage of gear trains to avoid stalling the driving motor.

Third, partial elastic storage helps minimize flight energetic costs by recovering partial inertial power of flapping wings [21] even for ornithopters [107]. However, full elastic storage may stall the motor [107]. Recently, a Dipteran-insect inspired thoracic shell was proposed to alleviate the elastic load, by having nonlinear stiffness which is low at the mid-stroke but higher at the end stroke. However, such thoracic shell is not compact enough to fit within the wing transmission. It is not clear if a simple radial stop found in Dipteran insect thoracic mechanism [89, 95, 108] can help brake or recoil the wings at the end of wing stroke like linear elastic storage does.

Here, we investigate feasibility of multi-modal flight and thrust requirement by a tailed ornithopter with four flapping flexible wings. Clap and fling effect was exploited to maximize the propulsion by four flapping wings. In addition, we greatly improve the energetic efficiency of the motorized wing transmission by using the elastic storage and miniaturized bearing. We shall show Nylon-hinged transmission outperforming the radial-stopped transmission, by avoiding active braking and the contact impact loss. Finally, we shall shows this 26.24g ornithopter with Nylon elastic storage being powerful (with up to 39g thrust) to perform the aerobatics, such a turning a tight corner and pulling-up from a dive despite the fact that flapping flexible wings risked fluttering during the dive.

9.1 Multi-Modal Flight: Feasibility and Thrust Requirement

Figure 9.1 Dynamics of the multi-modal flight of an X-wing with a cross tail and elevator control surfaces.

Figure 9.1 shows free-body force analyses of a tailed ornithopter during multiple modes of flapping flight, which include powered hover, powered forward flight, unpowered glide or dive. This analysis reveals the thrust requirement while the tail elevator controls the body attitude of various flying modes.

During normal hover, the thrust generation by flapping wings are equal to the weight. Then, the ornithopter body is orientated vertically with the stroke plane of flapping wings being horizontal and the elevator orientation being aligned to the body spine. For example, a Cornell's 3D printed ornithopter is capable of normal hover with a passive stabilizer. Other hovering ornithopters with elevator and rudder control include: Mentor [43], DelFly II [37] and FlowerFly [101, 109].

An elevator hinged at the tail can adjust the ornithopter body orientation from normal hover to forward or backward flight. For example, when the elevator is deflected right (off the body), the ornithopter body inclines towards the right (off the initial position). This action of elevator surface control is similar to a nose down (pitch down) of an airplane, albeit this ornithopter fuselage being vertical. This tilts the stroke plane and thus vectors the thrust for forward flight. However, this thrust vectoring causes a dip in the vertical force component and result in a drop in altitude as it does to a helicopter or quadcopters. To maintain a leveled forward flight, a higher magnitude of thrust is required during the angled thrust vectoring. For example, tailed ornithopters like DelFly and Mentor are capable of such 'tilted' forward flights.

An ornithopter capable of hovering should have no problem in performing a turning flight, albeit by body tilting and robust flight control. High thrust is required to power a tight and fast turn while the ornithopter is aloft in air. In turn, strong downwash is flowed across a tilted elevator and it helps generate a high pitching moment. Meanwhile, the wind induced by turning

is relatively slow as compared to the wing stroke speed and thus its effect is trivial to thrust generation.

Unpowered glide by four-wing ornithopter is rarely reported due to the risk of a sudden plummet at a slow forward speed. Then, the wings flutter because the angular limiter to flexible wings is nosed down to cause a negative angle of attack. As such, the flexible wings are prone to vortex-induced vibration and cannot generate a net lift to overcome the weight. Powered flight by flapping wings at an elevator up is required to pull up the ornithopter from the dive. A successful pull-up requires the high thrust generation to avoid a plummet.

In short, force analysis above showed that multi-modal flight by a four-wing ornithopter is feasible, provided that the motorized wing-transmission can generate high-thrust propulsion relative to the ornithopter bodyweight.

9.2 Method

Figure 9.2 Overview of the nylon-hinged X-wing with flexible veined membrane wings, cross tail and control surfaces.

To achieve high thrust generation given a lightweight motor, we integrated elastic storage to the transmission for flapping four membrane wings. A brushless motor indirectly drives the four wings through a double-crank-rocker mechanism. This elastic storage has the form of a pair of Nylon flexures, acting as an elastic pivotal support to the rocker link (i.e. the fused base of two flapping wings). The power transmission is a gear box which consists of rigid-gear box housing, shaft bearing, and gear trains of high gear ratio to drive the double crank-rocker mechanism. Here, we chose a lightweight brushless motor (AP03-7000kv from HobbyKing) of 3.1g, which is recommended to directly drive a propeller for a 40g maximum thrust generation at full throttle of 4.1V. Design and construction of the components are elaborated as followed.

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Figure 9.2 shows this ornithopter having four membrane wings configured into two diagonal pairs, each of which share a common leading-edge carbon spar of full span. The pair of wing planes, each consists of two diagonal wings, cross like scissors in an X shape. They are opened and closed in the stroke plane to produce the chordwise clap and fling effect that could enhance the thrust generation. Each diagonal pair of the wings span 280mm and weigh 1.82g (with moment of inertia being $13.073 \times 10^{-6} \text{ kgm}^2$); it is reciprocated for a 50° stroke. The indirect drive by motor at full throttle is designed to beat the wings at 14-15 Hz for maximum thrust generation.

Here, veined membrane wings consists of elastic membrane (23 μm polyurethane film, ArgoGuard 46510) and wing veins (carbon-rod reinforcement) adhesively taped on in a lay-out like bat's digital skeletons. The leading edge spar is a D-shape carbon spar of 1mm diameter while the radial chordwise ribs are 0.5mm diameter carbon rods. Chordwise flexibility of the membrane wings, which was fixed at the root chord, come from the membrane flexibility and freedom between the veins and leading-edge spar, which are only connected by wing membrane. Tension in the membrane and elasticity in the leading edge can limit the passive wing rotation. Due to a slack in the membrane wing, the mid-span chordwise rib is subjected to gravity induced rotation of 11° about the leading edge. Further wing rotation of up to 24° can be induced by a 3-g load acting on the tip of mid-span chord rib.

Figure 9.3 (a) Simple schematic showing the principle of the spring hinge and a pin jointed mechanism with radial stop. (b) CAD model showing the design of spring hinge with nylon and the pin-jointed mechanism

with radial stop. (c) Test setup to measure static torque and free oscillation with the nylon hinged wing pair disengaged from the double crank-rocker drive mechanism. (d) Static loading and the corresponding stroke angle. (d) Free oscillation of the wing upon release from a stroke angle.

Figure 9.3 shows the Nylon flexure hinge is used to provide elastic storage of wings' kinetic energy. As used for a fishing line [110], Nylon is well known of high toughness, high yield strain, and high ultimate elongation. Two pieces of nylon flexures, which were staggered at a distance, are used as hinges to provide a pivotal support to the leading edge spar of a diagonal wing pair. In this way, the twin nylon flexures can avoid fatigue from twisting while the wings are reciprocated and rotate passively. Design of each Nylon hinge is 0.5 mm thick, 1.5 mm long, and 3mm wide; it can be bent elastically for up to 68.8° . Our initial experiment shows this Nylon hinge can provide an elastic pivot support to a diagonal pair of membrane wings, sweeping 50° at full motor throttle (14 Hz) for 10 minutes without fatigue failure. In comparison, flexure hinges of Delrin (a brand of acetal resin, Polyoxymethylene, POM) break within a minute for 15Hz sweeping the same wing pair.

A high torque is required to drive this elastically pivoted pair of membrane wings. A lightweight motor (e.g. AP-03-7000kv) cannot produce enough torque to directly drive the large wings. Instead, indirect drive is possible with the help by a gear train (of 21.33 gear ratio) a crank-rocker mechanism (of 3.6 crank ratio). This indirect drive can magnify the motor's output torque by 76.8 times. Here, the low-loss transmission is a gearbox consisting of a rigid carbon-fibre housing, shaft bearings, brass compound gear for intermediate transmission, and two Delrin spur gears for output transmission. The brass compound spur gear has a 32 outer teeth and 8 pinion teeth. Each of the Delrin spur gears has 48 teeth while Delrin is good for self-lubrication. The shaft bearing (DDL301 miniaturized ball bearing) was supplied by International Bearing Pte. Ltd, Singapore. Each of the bearing weighs merely 0.04 g, with a 3 mm outer diameter and 1 mm thick. Rigidity of carbon-fibre plate housing and brass gear ensure low deformation under high load; while the shaft bearing ensure better shaft alignment reduce friction (see Figure 9.4).

A normal rigid body mechanism with pin joints has no means of elastic storage, unlike a compliant mechanism. For comparison with the compliant mechanism, we have double crank-rocker mechanisms made of elastic linkages of Delrin with self-lubrication, which reduce the friction reduce the friction between pin and linkage bore. In addition, we include a radial stop (i.e. a protrusion as shown in Figure 9.3 (b)) at the root of the crank link to prevent the toggling of crank-rocker mechanism from being jammed to a dead position. This radial stop does not brake the wing when it is being free of contact at the mid-stroke where the wing stroke speed peaks. When the wing stroke exceeds 50° , the radial stop collides with the ground body frame.

The elastic contact stops the wings towards the end stroke; and it exhibits nonlinearly increasing stiffness (see Figure 9.3 (e)) due to elastic deformation in the linkages and ground frame.

Figure 9.4 (a) Problem of previous gearbox design (b) Proposed solution using bearing supported gear shaft with end support. (c) Assembly of a gearbox with acrylic housing (carbon fiber housing is used in actual prototype, acrylic housing is shown for photo clarity). (d) Caged ball bearing DDL310.

9.3 Results

Figure 9.3(d-e) shows the two wing planes on the support of Nylon hinges. One diagonal wing plane (the spar which spans from bottom-left to top-right) is disengaged from the double-crank-rocker mechanism but held stationary by the tip of a wooden rod. Upon removal of the wooden rod, the elastic torque from the bent hinges rotated the wing to close at the horizon which corresponds to the no-load position of nylon hinges. Static opening of the wings into an X or scissor shape requires application of a static moment to each spar. For torque measurement, this system of nylon-hinged wings was mounted of a 6-axis force/torque sensor (ATI Nano 17). Static loading (by using the stationary wooden rod) on the bottom-left leading edge spar induces a counterclockwise rotation. This pair of twin nylon flexures was a measured stiffness of 12.89 mNm/rad. After release of a preload, this hinged wings undergoes a damped free oscillation with a natural frequency close to 5Hz. This suggests that this design of elastic storage can fully recover the wings' kinetic energy up to the 'resonant' frequency of 5Hz, but partial kinetic energy at a higher wing beat frequency. Cyclic loading (by hand slowly) of the pair of Nylon flexures however shows substantial hysteresis loss, which may compromise the benefit of elastic storage.

Figure 9.5 Wing kinematics of the Nylon hinged and pin jointed X-wing. (a) Snapshots of the wing opening stroke on Nylon hinged X-wing at 15 Hz wingbeat frequency (maximum throttle); (b) Wing stroke, 2Φ (c) Pitch amplitude, B (d) Ratio of closing duration in a wingbeat cycle period along wingbeat frequency; Time history at maximum throttle: (e) Stroke angle (f) Pitch angle (g) Stroke velocity (h) Pitch velocity.

The X-wings were tested using the experimental setup in Figure 5.3.

Figure 9.5 shows wing kinematics of four wings during a static thrust test. Snapshots of the four wings that open and close in a wingbeat cycle is viewed from the top and side. As the wings stroke following the leading-edge spar, the wing membrane with chordwise ribs rotates about the leading edge at a time lag. Video tracking of the leading-edge spar and the projected length of mid-chord rib yields the determination of average wing stroke (2Φ from the horizon where the wings close to) and the wing pitch amplitude B . Measurement shows the wing stroke amplitude is almost independent of the wing beat frequency, but within the allowable range given by the double-crank-rocker-mechanism design. The duration ratio of wing closing to opening ranges between 0.4 and 0.5. At a low wing beat frequency close to 5Hz, the duration is close to 0.4. Yet, above 5Hz, the duration ratio approaches to 0.5 for symmetric opening and closing due to higher wing inertia. As a result of dynamic stroke reciprocation, the amplitude of passive wing rotation increases with increasing the wing beat frequency; but it tapers towards an elastic limit allowable by the veined membrane wing construction.

While the compliant mechanism (with Nylon flexures) is designed to produce the same static wing stroke as the rigid-body mechanism (with pin joints and a radial stop), their dynamic wing stroke differ slight as wings beat faster. In the absence of flexure joints, the rigid body

mechanism can produce slightly more stroke and higher passive wing rotation amplitude as compared to the compliant mechanism. Surprisingly, speed of passive rotation for the Nylon hinged wings are higher when the compliant mechanism is driven at the motor's full throttle. This higher wing-pitch speed could be assisted by spring recoil in the presence of elastic storage of compliant mechanism.

Figure 9.6 Static thrust and power measurement of Nylon hinged and pin jointed wings and direct driven propeller Prop 3020 by the same motor. (a) Mean thrust along driving frequency (b) Mean thrust along electrical power input to ESC (c) Mean electrical power input to ESC along driving frequency. (d) Ratio of thrust to electrical power input along the mean thrust output. (e) Thrust force variation in a cycle (f) electrical power input to ESC in a cycle.

Static thrust measurement shows that the when beating at 15Hz four membrane wings on Nylon joints can produce a maximum thrust of 39.91 g under indirect drive by the motor at full throttle. This maximum thrust generation by flapping wings is as much as that generated by a motor-manufacturer-recommended propeller (Prop 3020) of 80.2 mm diameter that spins at the full-throttle frequency of 234 Hz. Interestingly, the electrical power expenditure for indirect drive of flapping is 6.69 W, only 78% of that for direct drive of spinning propeller, both at the motor full throttle. Hence, given the same electric power, the flapping wings with Nylon elastic storage can produce more thrust than the propeller. This suggests a higher energetic efficiency. Impressively, electric-power specific thrust generated by four Nylon-hinged flapping wings is 6.3 g/W at motor full throttle for nearly 40g thrust generation and shows an increasing trend. In comparison the electric-power specific thrust of a propeller under direct drive is only 4.58 g/W for the same thrust generation and shows a decreasing trend.

Time-resolute measurement shows that thrust generation and power expenditure change from peaks to valleys for every half cycle of wing beat. Given the same wing kinematics,

elastic storage shows little effect on thrust generation. However, it shows significant effect on saving the electric power to drive the four flapping wings. For example, indirect drive using a rigid-body mechanism is subjected to a major peak in electric power expenditure for opening (flinging) the wings and a minor peak for closing (clapping) the wings. With the help of elastic storage in Nylon joints, the major peak of electric power expenditure is greatly subdued. This Nylon elastic storage leads a 6.3% power saving at the motor full throttle. The power saving is lowered than expected. It is speculated that the radial stop in the acetal rocker link of the rigid-body mechanism could acts like an elastic radial stop of Dipteran insect to help recover part of the kinetic energy.

Figure 9.7 (a) Mean shaft power calculated from the average motor speed and measured current along motor frequency (b) Motor efficiency in converting electrical power input to shaft power output along motor frequency. (c) Thrust generated along shaft power output. (d) Ratio of thrust to shaft power output along thrust generation.

Figure 9.7(a) showed the maximum shaft power output by the motor at full throttle varies in the range from 4.0 W to 4.14 W, being slightly dependent on the propulsion load. However, it is noted that the motor's maximum spin frequency at full throttle is very sensitive to load. High gear ratio and elastic storage for indirectly drive of flapping wings greatly reduces the load torque such that the motor can spin up to 319 Hz. In comparison, the motor's full-throttle

spin frequency is merely 234 Hz for direct drive of propeller due to a higher apparent load. Figure 9.7(c) showed the indirect drive of flapping wings can achieve a full-throttle efficiency higher than direct drive of propeller due to former's higher gear ratio. Interestingly, elastic storage was found to improve the maximum motor efficiency by reducing the load-torque requirement for indirect drive through the same crank-rocker mechanism.

Correlation of between the thrust generation and motor shaft power confirmed that aerodynamic efficiency decreases with increasing the thrust generation following Froude's disk actuator theory. Interestingly, aerodynamic efficiency (i.e. thrust to mechanical shaft power) of Nylon hinged flapping wings are comparable to that of a recommended propeller. This is a benefit from the low disk loading of four flapping wings of 280mm span, and the increased motor efficiency by using a high-gear-ratio power transmission. In comparison, an 80.2mm two-blade propeller (Prop 3020) recommended by the motor manufacturer is subjected high disk loading due the need of very high spin frequency to generate the same thrust as the slowly beating flexible wings.

Analysis shows that indirect drive of flapping wings is benefited from the use of high gear ratio and elastic storage in the wing transmission, which reduces the apparent torque to load the driving motor. Similar, indirect drive by using high-gear ratio transmission can improve the motor efficiency while driving a much larger propeller for the same thrust generation by direct drive of a small propeller. As such, flight by flapping four flexible membrane wings promises to fly longer with this higher electrical-power specific thrust generation.

9.4 Multi-modal Flight Demonstration

A prototype of four-wing ornithopter of 27 g was remotely controlled (by a pilot) for a multi-modal flight test in a closed space as shown in Figure 9.8. Motion tracking based on the video leads to the determination of projected travel, speed and acceleration. At time $t=0$, the ornithopter took off vertical from the ground; subsequently, it ascends to a height where it hovers before it transits to a fast forward flight and almost leveled for a moment. The demonstrated forward speed is as much as 7 m/s given this limited flight range. The ornithopter is pulled backward (i.e. the reverse of forward flight) and thus ascend with a maximum tangential deceleration as much as 30 m/s^2 . Subsequently, it flew a tight turn of 30mm radius with the tail swung high up. The return is piloted for a dive to avoid hitting the ceiling and a subsequent climb. Towards the end of this flight test, it gradually descends but swings a bit like a pendulum. Finally, the ornithopter landed up right on ground.

Figure 9.8 Free flight of nylon hinged X-wing demonstrating (a) hover, fast forward flight, short turn, backward dive, descent and landing. (b) Snapshots of the backward turn and the radius of turn along time. (c) The body pitch angle (d) Tangential speed, horizontal and vertical speed of X-wing ($v, \dot{x}, \dot{z}$) along time.

Figure 9.9 demonstrates the ability of this ornithopter to recover from a steep dive with the action of an elevator at the tail and high thrust. Without power and elevator up action, Figure 9.9(a) shows that the ornithopter dives steeply nose down from a low height of about 2.4 meters, to land at the cushion in a short time of 0.56s. Figure 9.9(b, d-g) shows that, the elevator up action creates a pitching up moment⁵ that causes the ornithopter to pitch up its nose and glide further during descent. In this test, the ornithopter is dropped from a height of 3.15 meters and landed on the cushion in 1.07s. The ornithopter is however unable to climb because of there is no thrust.

To maneuver the ornithopter for a successful climb, the remote pilot needed ample room of descent height above the ground as shown in Figure 9.9(c) to accommodate pilot reaction delay. At the beginning of the dive test, the ornithopter was released still from three-stories-height from the ground (about 8.3 meters). Subsequently, the unpowered ornithopter nosed down under gravity and free fall with gaining descent speed. To make a climb from the dive, the ornithopter was nosed up with the elevator up and thrust generation on. A strong pitching

⁵ The pitching moment is calculated from pitching acceleration derived from the pitch angle and the body pitching inertia about center of mass. The body pitching inertia of this X-wing is measured from pendulum oscillation to be $284.8 \times 10^{-6} \text{ kg} \cdot \text{m}^2$. Center of mass is measured at 168.26 mm from the tail along the fuselage axis. The weight of this prototype at the moment of test is 28.04 g. The X-wing is pin jointed at the end of the tail and let oscillate freely like a pendulum to obtain a natural frequency of 6.55 Hz.

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moment to nose up the ornithopter is only possible with high thrust generation, which accelerates the descent for a while before making it a climb.

In a separate test for flight endurance, this four-wing ornithopter was piloted for continuous hover. Hover of the ornithopter lasts for 8 minutes when being powered by a fully charged battery (of 3.7V, 200mAh, 5.21 gram).

Figure 9.9 Demonstration of dive recovery. Indoor - (a) Free, unpowered dive without any elevator action. (b) Free, unpowered dive with elevator up. (c) Powered dive with elevator up to recover from dive and hover in outdoor. Time history of (d) height position, (e) horizontal position (ground distance traveled), (f) Body pitch angle, and (g) pitching moment about the center of mass of the X-wing in the unpowered dive with elevator up and neutral elevator action.

Chapter 10 Conclusion and Future Work Recommendation

In summary, this work presented the development of FWMAVs from scratch to a final product to achieve efficient flapping flight. We started experimenting with flapping wing prototypes from insect scale to bird scale, i.e. 100mm-span to 280mm-span. An analytical model of the flexible wings was also developed to predict lift and energetic costs for the flapping of flexible wing that twist linearly from root to tip.

Our analysis showed that at small insect scale (100mm-140mm span), the wing inertia is higher than the aerodynamic forces because of the relatively rigid wing used to beat large wing stroke at high wingbeat frequency. Wing inertia cannot be reduced much at this scale due to limits of materials and manufacturing methods. Therefore small FWMAV benefits significantly from elastic storage, up to 31% power savings.

When sized up (240mm-280mm span), the wings can become relatively flexible and lightweight. The aerodynamic drag is now able to decelerate the wing inertia adequately, therefore there is less excess of wing inertial power to recover by elastic storage. As a result, the elastic storage yield less power savings. At this size, transmission and aerodynamic efficiency are more significant in saving power and maximizing lift generation. For example, beating four large wings through small strokes with clap and fling is more aerodynamically efficient to generate same thrust as two large wings beating large strokes, because of lower wing speed. In addition, the crank-rocker mechanism used to drive the wings could only produce limited swing stroke and is more efficient for small stroke. Proper gear ratio is necessary to efficiently match the motor's output torque to the wing torque required at the operating frequency. The elastic storage helped in reducing the torque requirement so that the motor can run more efficiently.

In summary, we find that the key contributing factors to successful flight are 1) Large flexible wings that claps and flings at a low wing tip speed. 2) Speed-reduction transmission that matches the load closely with the motor's peak efficiency load. 3) Ultralight construction of large flexible wings to reduce the inertial power loss. 4) Elastic storage to recover the wing's inertial power. 5) Miniature bearing supports to reduce friction and support alignment of rotating shafts. The design and analysis methods presented here are useful for future design of platforms that can perform untethered hovering flapping flight. A remote controlled X-wing can serve as a platform to study free insect flight, instead of ground experiments.

A final product is a 27-g, 28cm-span X-wing micro air vehicle that can hover for more than 8 mins with a single charge. It is the world's first hovering FWMAV with elastic hinge mechanism. It can also perform fast forward flight and turn agilely with a tail control surface.

10.1 Original aspects:

The following parts of this work represent an original contribution:

- Design of compliant hinge for the storing of energy for flapping wing flight
- Development of an analytical model to predict passive wing rotation with a rigid chord.
- Development of an analytical model to predict the time-variant lift, aerodynamic power, inertial power and the elastic power for the elastic-hinged clap-and-fling X-wing with flexible wings that twist linearly.
- Development and fabrication of a fully clap and fling mechanism with staggered wing leading edges.
- Accounting the crank-rocker as an additional speed reducer that reduces torque load on the motor, to yield to actual speed reduction ratio in the transmission design.
- Use of nylon as the elastic hinge.
- Use of polyurethane film to make flexible and elastic wing.
- Demonstration of agile flapping flight by manual control from takeoff to landing.

The design and analytical methods presented here are useful for designing flapping wing prototype to size the wings and gearbox ratio according to the motor power and torque output. These methods had been used by Final Year Project students to design their flapping wing prototypes. The students were able to build prototypes that generated thrust of desired magnitude according to the motor power. Measurements of power and lift shows that the model prediction were in similar magnitude, just as demonstrated in this thesis (See section 3.5 and 6.5).

10.2 Future Work

The current maximum hovering endurance of 8 minutes of the X-wing can be further improved with reduction of structural weight and friction. A structural weight reduction can allow larger battery capacity, provided the flapping mechanism is not affected. Power loss due to friction is still quite high currently, more than the mechanical power to move the wing. Friction loss can be reduced in a few ways: i) fabricate the mechanism with stronger material which can hold better alignment between the moving parts. ii) reduce number of moving parts in design or make use of flexures. iii) make use of bearings for rotating linkages and self-lubricating but strong materials for the gears, such as PEEK. However, these ideas require better manufacturing methods and machines, subjected to available resources.

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Aerodynamically, we could explore whether larger-stroke flapping is more efficient than a small-stroke flapping with clap and fling. More experiments on different flexible wings need to be tested to identify the design parameters. Flow visualization techniques such particle image velocimetry may help to provide insight. Rigid wings are known produce highest lift at 45° angle of attack, but they are too heavy and need to be controlled actively with extra servos, which overloads FWMAV.

It is also interesting to investigate how different configurations of wings may affect lift generation and energetic costs. For example, some has studied the aerodynamics of tandem wing design like a dragonfly. We have a preliminary tandem wing design which stacks two X-wings vertically and is able to produce 70g of thrust and hover, at a total mass of 52 g (Figure 10.1).

Research on micro components to support flight is also vital. Lightweight servos and sensors are required to reduce body weight and allow control of more degree of freedom. Autopilots can be introduced for self-navigating flight. One of the bottlenecks in downscaling of flapping flight is the power source. Innovative solutions can be in the form of wireless powering or alternative fuel that packs higher power density than the current Li-Po battery.

Figure 10.1 A 50g tandem-wing capable of hovering flight.

List of Publications

1. Yao-Wei. Chin, Yukai Luo, Ziyuan Ang, Woei-Leong Chan, Javaan S. Chahl and Gih-Keong Lau, "Spring-Assisted Motorized Transmission for Efficient Hover by Four Flapping Wings," *Journal of Mechanisms and Robotics.*, vol. 10, no. 6, pp. 0161014-061014-12, 2018.
2. Yao-Wei Chin, Yu-Kai Luo, Zi-Yuan Ang, and Gih-Keong Lau, "Spring for clapping X-wing Micro Air Vehicles", *International Micro Air Vehicle Conference and Competition 2016 (IMAV 2016)*, Beijing, China.
3. Gih-Keong Lau, Yao-Wei Chin, Joel T. W. Goh and R.J. Wood, "Dipteran-Insect-Inspired Thoracic Mechanism With Nonlinear Stiffness to Save Inertial Power of Flapping-Wing Flight," in *IEEE Transactions on Robotics*, vol. 30, no. 5, pp. 1187-1197, Oct. 2014.
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6. Qican Zhang; Lei Huang ; Yao-Wei Chin ; Lau-Gih Keong ; Anand Asundi, "4D metrology of flapping-wing micro air vehicle based on fringe projection," *Proc. SPIE 8769, International Conference on Optics in Precision Engineering and Nanotechnology (icOPEN2013)*, 87692Y (June 22, 2013)
7. Yao-Wei Chin; Gih-Keong Lau, "Is clicking mechanism good for flapping wing micro aerial vehicle?", *Proc. SPIE 8686, Bioinspiration, Biomimetics, and Bioreplication 2013*, 86860W (April 8, 2013)
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Unpublished conference paper:

Yao-Wei Chin; Goh, J.T.-W.; Gih-Keong Lau, "Compliant Mechanisms for Flapping Wing Micro Aerial Vehicles", *8th International Conference on Intelligent Unmanned Systems (ICIUS 2012)*

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Appendix

A. Wing rotation with different phase: Variation of tip pitch angle $\beta(R, t)$ variation by phase φ and shifting of mid-pitch B_0 for X-wing clap and fling

Figure A.1 Illustration of the wing kinematics (position and attitude of a wing segment) due to different phase lead and shifting of the mid-pitch angle. The wing chord segment leading edge is denoted by a filled circle. Stroke amplitude $\Phi=25^\circ$, mid stroke angle $\Phi_m=25^\circ$, pitch amplitude $B(r)=50^\circ$ and mid pitch angle $B_0=0$. Each wing position is plotted at equal time intervals for the fore stroke and back stroke. (a) Advanced rotation with phase lead of $3\pi/4$. (b) Symmetric rotation with phase lead of $\pi/2$. (c) Delayed rotation with phase lead of $\pi/4$.

(d) Advanced rotation with phase lead of $3\pi/4$ and mid-pitch angle shifted to -10° . This negative shift reduces peak positive pitch angle and increase the value of negative peak of the pitch angle.

Figure A.2 Calculated time variations of lift components, total lift, mechanical power components, and total mechanical power with and without elastic storage for different wing kinematics due to different phase of wing rotation: (a-c) Advanced rotation with phase lead of $3\pi/4$ (d-f) symmetric rotation with phase lead of $\pi/2$ (g-i) delayed rotation with phase lead of $\pi/4$. Legends: L_{trans} - translational lift, L_{rot} - rotational lift, L_{fling} - lift to clap and fling, P_{aero} - aerodynamic power, P_{acc} - inertial power, P_{elas} - elastic power.

A phase lead of $\pi > \varphi > \frac{\pi}{2}$ advances the chord pitch angle to be positive at the start of a stroke and then negative at the end of a stroke, yielding an advanced rotation relative to the stroke angle. A phase lead of $\varphi = \frac{\pi}{2}$ synchronizes the chord pitch angle to start and end at zero angle in a stroke, yielding a symmetric rotation. A phase lead of $0 \leq \varphi < \frac{\pi}{2}$ delays the wing pitch to start at a negative pitch angle and end at a positive pitch angle in a stroke, yielding a delayed rotation.

B. Propeller Aerodynamic Lift and Power

Figure B.3 Illustration of (a) a spinning propeller blade and (b) the blade section flow conditions during hovering.

The fixed wing aerodynamics can be applied to propellers which spins wing a constant speed to generate lifting thrust for hovering. Propeller blades are usually rigid and long with a short chord length (high aspect-ratio). Let us consider a simple propeller which consists of two rigid rectangular plate of finite thickness. Each of the rectangular blade has a length R and a uniform chord width c , as shown in Figure B.3. For simplicity, assume that the blade has a uniform pitch angle α with respect to the horizontal plane where the blades spin. During hover, the climb rate is zero and the small induced downwash (v_i) is assumed negligible compared to the blade speed. Hence, the angle of attack is approximately equal to the blade pitch angle.

With the small-angle approximation, the thrust induced by spinning this blade at angular speed Ω is approximately equal to the lift:

$$\begin{aligned} T \approx L &= \int \frac{1}{2} \rho (r\Omega)^2 C_L c \, dr \\ &= \frac{1}{6} \rho \Omega^2 R^3 c C_L \end{aligned} \quad (B-1)$$

Its aerodynamic power to overcome the total drag torque which includes the induced and profile drags, is

$$\begin{aligned} P_{aero} &= \int \Omega r (\alpha_i dL + dD) \, dr \\ &= \int \Omega r \frac{1}{2} \rho (r\Omega)^2 (\alpha_i C_L + C_D) c \, dr \\ &= \frac{1}{8} \rho \Omega^3 R^4 c (\alpha_i C_L + C_D) \end{aligned} \quad (B-2)$$

The total thrust is equal to the sum of thrust forces generation by N_b number of blades, i.e. $N_b T$; whereas the total power is $N_b P_{aero}$. The thrust to aerodynamic power ratio is

$$\frac{T}{P_{aero}} = \frac{4}{3} \frac{1}{\Omega R} \left(\frac{C_L}{\alpha_i C_L + C_D} \right) \quad (B-3)$$

This shows that a lower tip speed, ΩR , leads to higher aerodynamic efficiency. At same low tip speed (efficiency), a larger propeller can generate much more thrust than a small propeller.

For example, we can calculate the thrust and power of a two-blade propeller 3020 Prop, which has a blade length 41mm and chord length of 12mm. The lift coefficient is $C_L = 0.5279$ and total drag coefficient (induced and profile drags) is $\alpha_i C_L + C_D = 0.1309$, from later measurements in experiment. To show the size effect, other blade designs are considered, doubling or halving the size of 3020 Prop blade.

Figure B.3 shows the thrust generation (T) increases quadratically with the spin frequency, while the aerodynamic power increases cubically with spin frequency. This suggests that the aerodynamic power (P_{aero}) increases at a faster rate than the thrust generation as the spin frequency increases. Given the same spin frequency, the double size propeller has the tip speed doubled, the thrust generation 8-fold higher, and the aerodynamic power 16-fold higher, as compared to the reference size propeller. At first sight, longer blades cannot be efficient given the same spin frequency, but it produces more thrust. However, to achieve the same thrust generation, the smaller blades need to spin much faster than larger blades.

At the same tip speed (efficiency), larger wings can generate more thrust. Figure B.5 shows that spinning longer wing blades at same low tip speed can generate higher thrust while achieving high efficiency (thrust per power). To hover at a total mass of 20g, the 3020 Prop needs to spin at 177Hz to a tip speed of 45 m/s and achieve a nominal thrust-to-aerodynamic power (T/P_{aero}) of 12.1 g/W. A propeller with double the radius needs only spin at 61Hz to a tip speed of 30.7 m/s with a higher T/P_{aero} of 17.8g/W. This shows that a longer wing is more aerodynamically efficient to produce the same lift. However, the wing cannot be infinitely long because of large wing mass and low wing rigidity. Alternatively, the blade chord can be increased to widen the blade area. This increases the thrust at the expense of lower aspect ratio and higher tip losses due to larger induced downwash. Another option is to increase the number of blades within allowable space on the hub. This reduces the blade loading with careful considerations of structural integrity and dynamic vibration issues. As the spacing between blades becomes closer, the effect of tip vortex of a blade becomes stronger on the succeeding blade.

Figure B.4 Theoretical calculation of (a) thrust along spin frequency (b) aerodynamic power along spin frequency and (c) thrust generation along aerodynamic power for two-blade propellers (3020 Prop) with half blade length (0.5R), normal blade length (R) and double blade length (2R).

Figure B.5 Thrust generation (left) and efficiency (right) along tip speed with increasing blade radius.

C. DC brushed motors from Didel, gearmotor from Precision MicroDrives and brushless motors (outrunners) from HobbyKing

DC Brushed Motors

MK04S-10
1.2V 10-12 Ohm
Ø 4 x 8mm 0.46 g
shaft 0.60mm

MK04-10
1.2V 13-15 Ohm
Ø 4 x 11mm 0.66 g
shaft 0.70mm

MK06-30
2.4V 30-35 Ohm
Ø 6 x 12mm 1.32 g
shaft 0.70mm

MK06-10
1.2V 10-12 Ohm
Ø 6 x 12mm 1.32 g
shaft 0.80mm

MK06-04.5
0.8V 4.5 Ohm
Ø 6 x 12mm 1.32 g
shaft 0.80mm

Precision MicroDrives
206-102 gearmotor
3V 8 Ohm 25:1 gear ratio
Ø 6 x 15.9mm 1.2 g
shaft 1.5mm
Output 152mW Max

Micro Brushless Motors (Outrunners) from HobbyKing

A05-2900KV
10V 3A
Ø 17 x 14mm 4.2 g
shaft Ø 1.5 x 5mm
Prop 2510,3020

A05-3700KV
10V 3A
Ø 17 x 14mm 4 g
shaft Ø 2mm
Prop 2510,3020

ADH30S-6100KV
4.2V 2.7A
Ø 17 x 13mm 2.6 g
shaft Ø 1 x 5mm
Prop 3020 34g 2.7g/W

ADH30S-7500KV
4.2V 2.4A
Ø 17 x 13mm 2.6 g
shaft Ø 1.5 x 5mm
Prop 2510 32g 3.17g/W

AP02-7000KV
3.7V 1.8A
Ø 13 x 8mm 2.3 g
shaft Ø 1.5mm
Prop 3020 27g 4.05g/W

AP03-4000KV
7.4V 1.7A
Ø 13 x 10mm 3.1 g
shaft Ø 1.5 x 6mm
Prop 3020 61g 4.85g/W

AP03-7000KV
3.7V 2.1A
Ø 13 x 10mm 3.1 g
shaft Ø 1.5 x 6mm
Prop 3020 44g 5.66g/W

D. List of electronic components for the flight control of FWMAV

Components

Receiver – Deltang Rx43d-3

Specification

Size: 11.8 x 12.9 x 2.2mm
 Weight: 0.34g
 Pad Spacing: 1.27mm
 Voltage: 3-6V
 Current: 13mA
 Modulation: 2.4GHz compatible with 22ms DSM2 Air and 16.5ms DSM2 Surface. No 'Quick Reconnect' and only uses 1 frequency (same as DSM2 Surface)
 Channels: 7 max
 Supplier: micron radio control (micronradiocontrol.co.uk)

SuperMicro Systems - Brushless ESC - 3A Mi-3A

Service Voltage: 3.7v
 Operating Current: 3A (maximum 5A for 5 SEC)
 Size: 12.9x11.6x3.4mm
 Operating Voltage: 3.3-5.0v
 Weight: 0.36g
 Input supported: PPM and PWM 1KHz 2KHz 3KHz 4KHz
 Supplier: HobbyKing

1.7g Rotary Micro Servo

Size: 18.7mm x 13.5mm x 6.2mm
 Torque: 0.065 ± 0.01 kg.cm @3.7V
 0.075 ± 0.01 kg.cm @4.2V
 Speed: 0.07 ± 0.01 s /60° @3.7V
 0.05 ± 0.01 s /60° @4.2V
 Weight: 1.7g (wires not included)
 Supplier: TaoBao

Li-Po Battery: LP90-FR Bahoma Cell with 10mm Bahoma

Capacity: 90mAh
 Voltage: 3.7v Single Cell
 Weight: 3.0g
 Dimensions: 14.5mm x 23mm x 4.8mm
 Max Discharge: 15C (1350mA)
 Connector Type: 10mm Bahoma
 Supplier: Plantraco Microflight (www.microflight.com)

Li-Po Battery: 200mAh 3.7V 35C high quality Nano CPX use

Capacity: 200mAh
 Voltage: 3.7v Single Cell
 Weight: 5.2g
 Dimensions: 12 x 44mm x 6mm
 Max Discharge: 35C (7.0A)
 Connector Type: microJST
 Supplier: overskyrc.com

E. Propeller and Brushless motor performance data from www.flybrushless.com

Motor: Medusa MR-012-030-4000, 17.0g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS EDF40 x6 1.6x3	1	6.8	1.95	13	27630	46	96%
GWS EDF40 x6 1.6x3	1	7.7	2.3	17	31170	64	96%
GWS EDF40 x6 1.6x3	1	8.7	2.8	24	34500	76	94%
GWS EDF40 x6 1.6x3	1	9.7	3.2	31	37800	94	92%
GWS EDF50B x5 2x3	1	6.9	2.7	18	25668	62	88%
GWS EDF50B x5 2x3	1	7.9	3.25	25	28656	76	86%
GWS EDF50B x5 2x3	1	8.9	3.85	34	31656	92	84%
GWS EDF50B x5 2x3	1	9.9	4.5	44	34600	110	83%
GWS EDF50A x3 2x3	1	6.9	2.2	15	27120	52	93%
GWS EDF50A x3 2x3	1	7.9	2.65	20	30540	66	92%
GWS EDF50A x3 2x3	1	8.9	3.15	28	33760	84	90%
GWS EDF50A x3 2x3	1	9.9	3.6	35	36680	100	88%
GWS EDF50A x3 2x3	1	10.8	4.15	44	39720	114	87%
GWS EDF 40 x6 2x3	1	6.9	1.6	11	24730	44	85%
GWS EDF 40 x6 2x3	1	7.9	1.9	15	27750	56	83%
GWS EDF 40 x6 2x3	1	8.9	2.25	20	30620	68	82%
GWS EDF 40 x6 2x3	1	9.9	2.6	25	33450	82	80%
GWS EDF 40 x6 2x3	1	10.9	3	32	36250	95	79%
GWS DD 2.5x3	1	7	1.05	7	27630	36	94%
GWS DD 2.5x3	1	7.4	1.15	8	29160	40	93%
GWS DD 2.5x3	1	8	1.25	10	31350	45	93%
GWS DD 2.5x3	1	9	1.45	13	35190	56	93%
GWS DD 2.5x3	1	10	1.65	16	38910	67	92%
GWS DD 2.5x3	1	11	1.9	20	42480	79	92%
GWS DD 3x2	1	6.5	3.45	22	21480	108	78%
GWS DD 3x2	1	7	3.7	25	22470	112	76%
GWS DD 3x2	1	7	3.7	25	22740	120	77%
GWS DD 3x2	1	7	3.7	25	22740	120	77%
GWS DD 3x2	1	7.4	3.95	29	23610	130	76%
GWS DD 3x2	1	7.4	3.95	29	23610	130	76%
GWS DD 3x2	1	7.4	4.05	29	23250	121	74%
GWS DD gray 3x2	1	7	3.95	27	22200	107	75%
GWS DD gray 3x2	1	7.4	4.1	30	23340	124	75%
GWS DD gray 3x2	1	9	5.2	46	27330	171	72%
GWS DD gray 3x2	1	9.9	6	59	29310	199	70%
GWS HD 3x3	1	6.5	4.7	30	19410	106	71%
GWS HD 3x3	1	7	4.9	34	20220	115	68%
GWS HD 3x3	1	7	4.95	34	20600	115	70%
GWS HD 3x3	1	7	4.9	34	20220	115	68%
GWS HD 3x3	1	7.4	5.3	39	21120	126	68%
GWS HD 3x3	1	7.4	5.25	38	21220	125	68%
GWS HD 3x3	1	7.4	5.3	39	21120	126	68%
Graupner CAM Speed 3x3	1	7	4.7	32	20190	109	68%
Graupner CAM Speed 3x3	1	7.4	5.1	37	21000	118	67%
GWS DD 4x2.5	1	6.5	5.25	34	17970	150	66%
GWS HD 4.5x3	1	6.5	6.5	42	15570	171	57%
GWS HD 7x3.5	5.86	6.7	0.9	6	5100	44	106%
GWS HD 7x3.5	3.5	6.7	2.25	15	7620	110	94%
GWS HD 7x3.5	4.14	6.7	1.65	11	6780	86	99%
GWS HD 7x3.5	5.86	7.6	1.05	7	5850	62	107%
GWS HD 7x3.5	3.5	7.8	2.7	21	8610	152	92%
GWS HD 7x3.5	4.14	8	2	16	7650	108	94%
GWS HD 7x3.5	4.14	8.8	2.4	21	8490	142	95%
GWS HD 7x3.5	3.5	8.9	3.25	28	9510	184	89%
GWS HD 7x3.5	5.86	9.2	1.2	11	6510	76	98%
GWS HD 7x3.5	3.5	9.5	3.7	35	10320	212	90%
GWS HD 7x3.5	4.14	9.8	2.75	26	9360	176	94%
GWS HD 7x3.5	5.86	10	1.4	14	7230	96	100%
GWS HD 7x3.5	5.86	10.6	1.6	16	7920	120	104%
GWS HD 7x3.5	4.14	10.6	3.2	33	10080	204	93%
GWS HD 7x3.5	3.5	10.7	4.3	46	11100	262	86%
GWS HD 8x4	3.5	6.8	4	27	6540	198	80%

GWS HD 8x4	4.14	6.9	3.05	21	6030	166	86%
GWS HD 8x4	5.86	6.9	1.6	11	4860	110	98%
GWS HD 8x4	7	7	1	7	3840	70	91%
GWS HD 8x4	9.67	7.1	0.7	4	3120	42	101%
GWS HD 8x4	9.67	7.5	0.8	6	3570	54	109%
GWS HD 8x4	3.5	7.6	4.85	36	7170	252	78%
GWS HD 8x4	4.14	7.8	3.7	28	6720	210	85%
GWS HD 8x4	5.86	7.9	1.9	15	5490	140	97%
GWS HD 8x4	7	8	1.2	9	4350	95	90%
GWS HD 8x4	4.14	8.6	4.3	36	7350	258	84%
GWS HD 8x4	5.86	8.7	2.3	20	6120	170	98%
GWS HD 8x4	3.5	8.8	5.55	48	7740	290	73%
GWS HD 8x4	9.67	8.9	0.9	8	4020	70	104%
GWS HD 8x4	7	8.9	1.4	12	4830	112	90%
GWS HD 8x4	9.67	9.5	1.05	9	4410	88	106%
GWS HD 8x4	4.14	9.8	5	49	7950	300	80%
GWS HD 8x4	7	10	1.6	16	5370	144	89%
GWS HD 8x4	5.86	10	2.7	27	6720	206	93%
GWS HD 8x4	5.86	10.6	3.1	32	7260	250	95%
GWS HD 8x4	4.14	10.7	5.7	60	8520	346	78%
GWS HD 8x4	9.67	10.8	1.2	12	4890	106	104%
GWS HD 8x4	7	10.9	1.8	19	5790	165	88%
GWS HD 9x5	5.86	6.9	2.6	17	4500	174	91%
GWS HD 9x5	7	7	1.55	10	3600	120	85%
GWS HD 9x5	7	7.9	1.8	14	4020	154	84%
GWS HD 9x5	5.86	8.1	3.1	25	5010	224	86%
GWS HD 9x5	5.86	8.9	3.7	32	5490	270	86%
GWS HD 9x5	7	8.9	2.1	18	4440	189	83%
GWS HD 9x5	5.86	9.9	4.35	43	6000	324	84%
GWS HD 9x5	7	9.9	2.45	24	4860	227	81%
GWS HD 9x5	7	10.9	2.8	30	5250	265	80%
GWS HD 9x5	5.86	10.9	4.95	53	6390	368	81%
GWS HD 10x6	5.86	6.7	3.3	22	4170	216	86%
GWS HD 10x6	7	6.9	1.95	13	3360	155	81%
GWS HD 10x6	9.67	6.9	1.3	8	2970	106	99%
GWS HD 10x6	7	6.9	1.95	13	3360	155	81%
GWS HD 10x6	5.86	7.8	4	31	4650	268	83%
GWS HD 10x6	7	7.9	2.35	18	3750	198	79%
GWS HD 10x6	7	7.9	2.35	18	3750	198	79%
GWS HD 10x6	9.67	8.1	1.6	12	3390	138	96%
GWS HD 10x6	5.86	8.8	4.65	40	5100	324	80%
GWS HD 10x6	7	8.9	2.75	24	4140	240	77%
GWS HD 10x6	7	8.9	2.75	24	4140	240	77%
GWS HD 10x6	9.67	8.9	1.9	16	3750	170	97%
GWS HD 10x6	9.67	9.8	2.25	22	4140	208	97%
GWS HD 10x6	7	9.9	3.15	31	4500	280	75%
GWS HD 10x6	7	9.9	3.15	31	4500	280	75%
GWS HD 10x6	9.67	10.8	2.6	28	4500	248	95%
GWS HD 10x6	7	10.9	3.6	39	4800	323	73%
GWS HD 10x6	7	10.9	3.6	39	4800	323	73%
GWS HD 11x7	9.67	6.8	1.9	12	2820	160	95%
GWS HD 11x7	8.6	6.8	2.35	15	3090	190	93%
GWS HD 11x7	7	6.9	2.6	17	3030	202	73%
GWS HD 11x7	7	6.9	2.6	17	3030	202	73%
GWS HD 11x7	9.67	7.7	2.35	18	3180	204	95%
GWS HD 11x7	8.6	7.7	2.85	21	3450	238	91%
GWS HD 11x7	7	7.9	3.1	24	3360	250	71%
GWS HD 11x7	7	7.9	3.1	24	3360	250	71%
GWS HD 11x7	8.6	8.8	3.4	29	3810	290	88%
GWS HD 11x7	7	8.9	3.6	32	3660	296	68%
GWS HD 11x7	7	8.9	3.6	32	3660	296	68%
GWS HD 11x7	9.67	9.1	2.75	25	3540	252	89%
GWS HD 11x7	9.67	9.8	3.25	31	3870	296	90%
GWS HD 11x7	8.6	9.9	3.95	39	4140	344	85%
GWS HD 11x7	7	9.9	4.1	40	3900	342	65%
GWS HD 11x7	7	9.9	4.1	40	3900	342	65%
GWS HD 11x7	7	10.9	4.6	50	4140	382	63%
GWS HD 11x7	9.67	10.9	3.75	40	4170	350	88%

GWS HD 11x7	7	10.9	4.6	50	4140	382	63%
GWS HD 11x7	8.6	11.3	4.7	53	4530	422	82%

Motor: AEO-C10-2100, 10g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS DD 3x2	1	7	0.9	6	12930	34	84%
GWS DD 3x2	1	8	1	8	14580	44	82%
GWS DD 3x2	1	8.9	1.2	10	16050	56	82%
GWS DD 3x2	1	9.9	1.3	12	17460	65	80%
GWS DD 3x2	1	10.9	1.55	16	18810	75	78%
GWS DD 4x2.5	1	7	1.35	9	11400	60	74%
GWS DD 4x2.5	1	7.4	1.45	10	11910	66	73%
GWS DD 4x2.5	1	8	1.55	12	12690	74	72%
GWS DD 4x2.5	1	9	1.8	16	13920	90	70%
GWS DD 4x2.5	1	10	2.05	20	15060	105	68%
GWS DD 4x2.5	1	11	2.3	25	16080	120	66%
GWS HD 4x4	1	6.9	1.8	12	9900	55	65%
GWS HD 4x4	1	7.4	1.9	14	10290	59	63%
GWS HD 4x4	1	8	2.1	16	10830	66	61%
GWS HD 4x4	1	8.9	2.4	21	11670	78	59%
GWS HD 4x4	1	10	2.75	27	12480	90	56%
GWS HD 4x4	1	11	3.05	33	13080	102	54%
GWS HD 4.5x3	1	7	1.65	11	10410	75	67%
GWS HD 4.5x3	1	7.4	1.8	13	10920	83	67%
GWS HD 4.5x3	1	8	1.95	15	11400	91	64%
GWS HD 4.5x3	1	8.9	2.25	20	12330	107	63%
GWS HD 4.5x3	1	10	2.6	26	13140	121	59%
GWS HD 4.5x3	1	10.9	2.9	31	13890	139	58%
GWS HD 4.5x4	1	7	2	14	9300	76	60%
GWS HD 4.5x4	1	7.4	2.15	15	9720	82	59%
GWS HD 4.5x4	1	7.9	2.3	18	10170	89	58%
GWS HD 4.5x4	1	8.9	2.6	23	11040	108	56%
GWS HD 4.5x4	1	9.9	2.9	28	11640	121	53%
GWS HD 4.5x4	1	10.9	3.25	35	12210	135	51%
Gunther 4.92x4.33	1	7	2.5	17	7830	94	51%
Gunther 4.92x4.33	1	7.4	2.6	19	8130	97	50%
GWS HD 5x3	1	7	1.85	12	9930	91	64%
GWS HD 5x3	1	7.4	2	14	10320	99	63%
GWS HD 5x3	1	7.9	2.15	16	10800	108	62%
GWS HD 5x3	1	9	2.5	22	11730	129	59%
GWS HD 5x3	1	10	2.8	28	12480	145	56%
GWS HD 5x3	1	10.9	3.1	33	13110	163	54%
GWS HD 6x3	1	7	2.4	16	8250	111	53%
GWS HD 6x3	1	7.4	2.55	18	8430	120	52%
GWS HD 7x3.5	1	7	2.6	18	7050	108	46%

Motor: GPS - HXT C2020-31

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS DD 4x2.5	1	7	3.2	22	15840	111	70%
GWS DD 4x2.5	1	7.4	3.4	25	16710	122	69%
GWS DD 4x2.5	1	7.9	3.7	29	17610	135	69%
GWS DD 4x2.5	1	8.9	4.35	38	18960	159	66%
GWS DD 4x2.5	1	9.9	4.95	49	20490	186	64%
GWS DD 4x2.5	1	10.9	5.5	59	21750	211	61%
GWS HD 4x4	1	7	4.5	31	13500	102	59%
GWS HD 4x4	1	7.4	4.8	35	13920	109	58%
GWS HD 4x4	1	7.9	5.2	41	14520	118	57%
GWS HD 4x4	1	8.9	6	53	15420	137	53%

Motor: Medusa - MR-012-030-5300, 17g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS EDF40 x6 1.6x3	1	6.8	3.6	24	34480	82	96%
GWS EDF40 x6 1.6x3	1	6.9	3.2	22	32720	66	89%
GWS EDF40 x6 1.6x3	1	7.7	4.45	34	38420	100	94%
GWS EDF40 x6 1.6x3	1	7.9	3.8	30	36740	86	88%
GWS EDF40 x6 1.6x3	1	8.7	5.2	45	42250	122	92%
GWS EDF40 x6 1.6x3	1	8.9	4.35	38	40650	106	86%
GWS EDF40 x6 1.6x3	1	9.7	6.2	60	45600	142	89%
GWS EDF40 x6 1.6x3	1	9.9	5.3	52	44500	130	85%
GWS EDF50B x5 2x3	1	6.9	4.85	33	31572	94	86%
GWS EDF50B x5 2x3	1	7.9	5.85	46	34956	118	83%
GWS EDF50A x3 2x3	1	6.9	4.2	28	33060	86	90%
GWS EDF50A x3 2x3	1	7.9	5	39	36700	106	88%
GWS EDF50A x3 2x3	1	8.9	5.85	52	40560	128	86%
GWS EDF50A x3 2x3	1	9.9	6.85	67	44040	150	84%
GWS EDF50A x3 2x3	1	10.8	7.4	79	48200	170	84%
GWS DD 3x2	1	6.9	6.8	46	27720	135	76%
GWS DD 3x2	1	7.4	7.4	54	28950	149	74%
GWS HD 3x3	1	6.9	8.9	61	24180	124	66%
GWS HD 3x3	1	7.4	9.5	70	24930	132	64%
Gunther White 4.92x4.33	3.5	10.9	5.6	61	14130	254	86%
GWS HD 5x4.3	3.5	7	2.65	18	9540	105	90%
GWS HD 5x4.3	3.5	8	3.2	25	10800	134	89%
GWS HD 5x4.3	3.5	8.9	3.85	34	11970	163	89%
GWS HD 5x4.3	3.5	9.9	4.4	43	13140	198	88%
GWS HD 5x4.3	3.5	10.9	5.4	58	14190	232	86%
Zagi Carbon 5.1x4.9	3.5	6.9	3.1	21	9300	112	89%
Zagi Carbon 5.1x4.9	3.5	7.9	3.75	29	10380	140	87%
Zagi Carbon 5.1x4.9	3.5	8.9	4.65	41	11580	175	86%
Zagi Carbon 5.1x4.9	3.5	9.9	5.5	54	12660	208	84%
Zagi Carbon 5.1x4.9	3.5	10.9	6.3	68	13740	247	83%
APC E 5.25x6.25	3.5	10.9	8.6	93	12420	206	75%
APC E 5.5x4.5	3.5	10.9	7.2	78	13200	247	80%
GWS HD 7x3.5	4.14	6.9	2.6	17	8040	139	91%
GWS HD 7x3.5	4.14	7.9	3.25	25	9090	184	90%
GWS HD 7x3.5	4.14	8.9	4	35	10020	227	88%
GWS HD 7x3.5	4.14	9.9	4.8	47	11040	280	87%
GWS HD 8x4	5.86	6.9	2.4	16	5760	165	92%
GWS HD 8x4	7	7	1.8	12	4920	121	93%
GWS HD 8x4	5.86	7.9	3	23	6480	216	91%
GWS HD 8x4	7	8	2.2	17	5610	159	93%
GWS HD 8x4	5.86	8.9	3.6	32	7260	269	90%
GWS HD 8x4	7	9	2.65	23	6270	198	92%
GWS HD 8x4	5.86	9.9	4.3	42	7920	325	88%
GWS HD 8x4	7	10	3.1	31	6960	243	92%
GWS HD 8x4	7	10.9	3.6	39	7560	289	92%
GWS HD 8x4	5.86	10.9	5	54	8610	389	87%
GWS RS 8x4.3	7	6.9	2.3	15	4830	154	92%
GWS RS 8x4.3	7	7.9	2.8	22	5490	198	92%
GWS RS 8x4.3	7	8.9	3.35	29	6060	245	90%
GWS RS 8x4.3	7	9.9	4	39	6720	300	90%
GWS RS 8x4.3	7	10.9	4.6	50	7320	355	89%
GWS HD 9x5	7	6.9	2.9	20	4710	204	90%
GWS HD 9x5	5.86	6.9	4	27	5340	268	86%
GWS HD 9x5	5.86	7.9	4.9	38	5970	335	84%
GWS HD 9x5	7	7.9	3.6	28	5310	264	89%
GWS HD 9x5	5.86	8.9	5.85	52	6600	414	82%
GWS HD 9x5	7	8.9	4.3	38	5880	319	87%
GWS HD 9x5	7	9.9	5.05	49	6390	394	85%
GWS HD 9x5	5.86	9.9	6.9	68	7110	495	79%
GWS HD 9x5	7	10.9	6	65	6930	471	84%
GWS RS 9x7	7	6.9	4.75	32	4320	259	83%
GWS HD 10x6	8.6	6.9	2.6	17	3870	196	91%
GWS HD 10x6	8.6	7.9	3.3	26	4380	260	90%
GWS HD 10x6	8.6	8.9	3.8	33	4860	312	89%
GWS HD 10x6	8.6	9.9	4.6	45	5370	384	88%

GWS HD 10x6	8.6	10.9	5.3	57	5820	455	87%
GWS HD 11x7	8.6	6.9	3.95	27	3660	288	86%
GWS HD 11x7	8.6	7.9	4.8	37	4080	369	84%
GWS HD 11x7	8.6	8.9	5.7	50	4500	442	82%
GWS HD 11x7	8.6	9.9	6.7	66	4890	531	80%

Motor: AX - AX-1406N 2000, 11 g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS EP 4x2.5	1	7	1.37	9	11788	61	80%
GWS HD 4.5x3	1	7	1.68	11	11086	79	76%
GWS EP 5x3	1	7	1.91	13	10607	100	72%
GWS EP 5x4.3	1	7	2.58	18	9380	106	64%
GWS EP 6x3	1	7	2.61	18	9126	125	62%

Motor: HobbyKing - 1811-2000, 12g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS HD 5x3	1	7.93	3	23	7500	112	47%
GWS HD 5x3	1	8.09	2	16	6700	88	41%
GWS HD 5x3	1	8.22	1	8	5260	50	32%
GWS HD 5x3	1	10.88	3	32	7800	123	36%
GWS HD 5x3	1	11.01	2	22	7046	100	32%
GWS HD 5x3	1	11.14	1	11	5570	55	25%
GWS EP 6x3	1	7.43	3	22	8600	110	58%

Motor: CustomCDR - Baby Bullet Hot, 12g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
Graupner CAM 3x3	1	6.6	4.3	28	19230	97	77%
Graupner CAM 3x3	1	6.8	4.35	29	19800	101	77%
Graupner CAM 3x3	1	7	4.6	32	20310	106	77%
Graupner CAM 3x3	1	7.2	4.8	34	20820	113	77%
Graupner CAM 3x3	1	7.4	5	37	21270	119	76%
GWS DD 4x2.5	1	6.6	4.9	32	18330	151	74%
GWS DD 4x2.5	1	6.8	5.3	36	19080	165	75%
GWS DD 4x2.5	1	7	5.55	38	19470	170	74%
GWS DD 4x2.5	1	7.2	5.7	41	19770	175	73%
GWS DD 4x2.5	1	7.4	6	44	20130	185	72%
Graupner CAM Speed 4x3	1	6.6	6	39	17130	159	69%
Graupner CAM Speed 4x3	1	6.8	6.4	43	17730	166	69%
Graupner CAM Speed 4x3	1	7	6.7	46	18150	176	69%
Graupner CAM Speed 4x3	1	7.2	6.9	49	18510	181	68%
GWS HD 4.5x3	1	6.6	7	46	16560	194	67%
GWS HD 4.5x3	1	6.8	7.15	48	16770	197	66%
GWS HD 4.5x3	1	7	7.5	52	17070	206	65%
GWS HD 4.5x3	1	7.2	8	57	17340	217	64%
GWS HD 4.5x3	1	7.4	8.3	61	17610	221	63%

Motor: CustomCDR - Baby Bullet Cool, 12g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
Graupner CAM 3x3	1	6.6	1.7	11	14160	53	85%
Graupner CAM 3x3	1	6.8	1.75	11	14580	54	85%
Graupner CAM 3x3	1	7	1.8	12	14880	57	84%
Graupner CAM 3x3	1	7.2	1.95	14	15270	61	84%
Graupner CAM 3x3	1	7.4	2.1	15	15540	62	83%
GWS DD 4x2.5	1	6.6	2	13	13590	83	81%
GWS DD 4x2.5	1	6.8	2.05	13	14010	89	81%
GWS DD 4x2.5	1	7	2.15	15	14400	92	81%
GWS DD 4x2.5	1	7.2	2.25	16	14760	97	81%

GWS DD 4x2.5	1	7.4	2.3	17	15030	101	80%
Graupner CAM Speed 4x3	1	6.6	2.5	16	12930	90	77%
Graupner CAM Speed 4x3	1	6.8	2.65	18	13230	93	77%
Graupner CAM Speed 4x3	1	7	2.75	19	13500	97	76%
Graupner CAM Speed 4x3	1	7.2	2.8	20	13800	100	76%
Graupner CAM Speed 4x3	1	7.4	2.9	21	14070	106	75%
GWS HD 4x4	1	6.6	3	19	12090	86	72%
GWS HD 4x4	1	6.8	3.1	21	12450	90	72%
GWS HD 4x4	1	7	3.25	22	12690	92	72%
GWS HD 4x4	1	7.2	3.35	24	12900	96	71%
GWS HD 4x4	1	7.4	3.5	25	13230	100	71%
GWS HD 4.5x3	1	6.6	2.75	18	12480	106	75%
GWS HD 4.5x3	1	6.8	2.9	19	12780	110	74%
GWS HD 4.5x3	1	7	3	21	12930	114	73%
GWS HD 4.5x3	1	7.2	3.1	22	13350	121	73%
GWS HD 4.5x3	1	7.4	3.3	24	13620	127	73%
GWS HD 5x3	1	6.6	3	19	12120	133	73%
GWS HD 5x3	1	6.8	3.1	21	12450	141	72%
GWS HD 5x3	1	7	3.25	22	12660	146	71%
GWS HD 5x3	1	7.2	3.4	24	12960	156	71%
GWS HD 5x3	1	7.4	3.5	25	13230	159	71%
GWS HD 5x4.3	1	6.6	4.15	27	10290	138	62%
GWS HD 5x4.3	1	6.8	4.3	29	10560	141	61%
GWS HD 5x4.3	1	7	4.4	30	10740	147	61%
GWS HD 5x4.3	1	7.2	4.6	33	10950	154	60%
GWS HD 5x4.3	1	7.4	4.7	34	11130	162	59%
Zagi Carbon 5.1x4.9	1	6.6	4.75	31	9570	133	57%
Zagi Carbon 5.1x4.9	1	7	5.1	35	9840	140	56%
GWS HD 6x3	1	6.6	4.15	27	10140	169	61%
GWS HD 6x3	1	6.8	4.35	29	10440	180	61%
GWS HD 6x3	1	7	4.6	32	10650	191	60%
GWS HD 6x3	1	7.2	4.75	34	10830	198	59%

Motor: Komodo - KH2208-8, 41 g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
Zagi Carbon 5.1x4.9	1	11	3.5	38	11430	186	92%
Zagi Carbon 5.1x4.9	1	14.7	5.45	80	14940	322	90%
MAS 3-blade 6x4	1	7	2.8	19	7260	104	92%
MAS 3-blade 6x4	1	7.9	3.4	26	8140	132	92%
MAS 3-blade 6x4	1	8.9	4.05	36	9060	168	90%
MAS 3-blade 6x4	1	9.9	4.8	47	10000	208	90%
MAS 3-blade 6x4	1	10.9	5.5	59	10880	247	89%
MAS 3-blade 6x4	1	14.6	8.5	124	14040	436	85%
APC E 6x5.5	1	10.9	6.05	65	10770	264	88%
APC E 6x5.5	1	14.6	9.1	132	13890	454	85%
GWS HD 7x3.5	1	7	2.2	15	7380	121	94%
GWS HD 7x3.5	1	8	2.6	20	8310	154	92%
GWS HD 7x3.5	1	9	3.1	27	9330	206	92%
GWS HD 7x3.5	1	10	3.65	36	10290	252	91%
GWS HD 7x3.5	1	10.9	4.2	45	11220	302	91%
APC SF 7x4	1	6.9	3.3	22	7110	196	92%
APC SF 7x4	1	7.9	4.1	32	7980	253	90%
APC SF 7x4	1	8.9	5.05	44	8850	319	88%
APC SF 7x4	1	9.9	6	59	9690	382	87%
APC SF 7x4	1	10.9	7	76	10470	454	85%
MAS 3-blade 7x4	1	7	4.15	29	6960	167	88%
MAS 3-blade 7x4	1	7.9	5	39	7800	219	88%
MAS 3-blade 7x4	1	8.9	5.95	52	8620	276	86%
MAS 3-blade 7x4	1	9.9	6.95	68	9500	354	85%
MAS 3-blade 7x4	1	10.9	8	87	10280	427	84%
MAS 3-blade 7x4	1	14.6	12.2	178	13020	736	79%
APC E 7x5	1	6.9	4.1	28	6900	210	89%
APC E 7x5	1	7.9	5	39	7770	267	87%
APC E 7x5	1	8.9	6	53	8640	340	86%
APC E 7x5	1	9.9	7	69	9450	410	85%

APC E 7x5	1	10.9	8.1	88	10200	488	83%
APC E 7x5	1	14.6	12.85	187	12840	795	78%
MAS 7x5.5	1	6.9	5.5	37	6660	210	86%
MAS 7x5.5	1	7.9	6.7	52	7440	268	84%
MAS 7x5.5	1	8.9	8.05	71	8190	326	82%
MAS 7x5.5	1	9.9	9.25	91	8880	387	80%
MAS 7x5.5	1	10.9	10.6	115	9570	453	78%
APC SF 7x6	1	6.9	4.9	33	6780	223	87%
APC SF 7x6	1	7.9	6	47	7590	276	85%
APC SF 7x6	1	8.9	7.3	64	8310	340	83%
APC SF 7x6	1	9.9	8.7	86	9030	404	81%
APC SF 7x6	1	10.9	10.1	110	9690	469	79%
APC Sport 7x6	1	6.9	4.35	30	6870	172	89%
APC Sport 7x6	1	7.9	5.3	41	7740	220	87%
APC Sport 7x6	1	8.9	6.25	55	8610	272	86%
APC Sport 7x6	1	9.9	7.35	72	9390	330	84%
APC Sport 7x6	1	10.9	8.4	91	10110	393	82%
APC Sport 7x6	1	14.6	12.4	181	12960	700	79%
APC Sport 7x7	1	7	4.65	32	6840	202	87%
APC Sport 7x7	1	7.9	5.6	44	7650	253	86%
APC Sport 7x7	1	8.9	6.6	58	8460	309	84%
APC Sport 7x7	1	9.9	7.75	76	9240	374	83%
APC Sport 7x7	1	10.9	9.05	98	10020	448	82%
APC Sport 7x7	1	14.6	13.8	201	12540	804	76%
GWS HD 8x4	1	6.9	4	27	6900	257	89%
GWS HD 8x4	1	7.9	4.95	39	7830	327	88%
GWS HD 8x4	1	8.9	5.9	52	8610	403	86%
GWS HD 8x4	1	9.9	7	69	9450	489	85%
GWS HD 8x4	1	10.9	8.1	88	10230	574	83%
MAS 8x4	1	6.9	6.2	42	6450	279	83%
MAS 8x4	1	7.9	7.5	59	7170	351	81%
MAS 8x4	1	8.9	8.85	78	7920	430	79%
MAS 8x4	1	9.9	10.2	100	8550	529	77%
MAS 8x4	1	10.9	11.8	128	9240	618	75%
MAS 8x5.5	1	6.9	7.05	48	6270	287	81%
MAS 8x5.5	1	7.9	8.5	67	6990	356	79%
MAS 8x5.5	1	8.9	10	89	7650	436	76%
MAS 8x5.5	1	9.9	11.65	115	8280	513	74%
MAS 8x5.5	1	10.9	13.25	144	8820	584	72%
GWS HD 8x6	1	6.9	6.3	43	6450	303	83%
GWS HD 8x6	1	7.9	7.7	60	7140	374	80%
GWS HD 8x6	1	8.9	9.2	81	7830	452	78%
GWS HD 8x6	1	9.9	10.8	106	8460	533	76%
GWS HD 8x6	1	10.9	12.5	136	9090	613	74%
Kavan 9x4	1	6.9	5.8	40	6510	337	84%
Kavan 9x4	1	7.9	7.1	56	7290	424	82%
Kavan 9x4	1	8.9	8.55	76	8040	521	80%
Kavan 9x4	1	9.9	10	99	8670	617	78%
Kavan 9x4	1	10.9	11.55	125	9270	724	76%
APC E 9x4.5	1	6.9	6.5	44	6420	389	83%
APC E 9x4.5	1	7.9	7.9	62	7140	485	80%
APC E 9x4.5	1	8.9	9.45	84	7800	587	78%
APC E 9x4.5	1	9.9	11.1	109	8430	695	76%
APC E 9x4.5	1	10.9	12.75	138	9000	804	73%
APC SF 9x4.7	1	6.9	7.35	50	6240	424	80%
APC SF 9x4.7	1	7.9	8.75	69	6900	514	78%
APC SF 9x4.7	1	8.9	10.3	91	7530	622	75%
APC SF 9x4.7	1	9.9	12	118	8160	728	73%
APC SF 9x4.7	1	10.9	13.5	147	8730	830	71%
GWS HD 9x5	1	6.9	6.75	46	6330	401	82%
GWS HD 9x5	1	7.9	8.2	64	7020	498	79%
GWS HD 9x5	1	8.9	9.8	87	7710	606	77%
GWS HD 9x5	1	9.9	11.4	112	8310	705	75%

Motor: Precision Aerobatics - Thrust 20, 73g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
GWS HD 9x5	1	6.9	6.4	44	6330	390	89%
GWS HD 9x5	1	7.9	7.95	62	7110	496	87%
GWS HD 9x5	1	8.9	9.5	84	7800	606	85%
GWS HD 9x5	1	9.9	11.25	111	8550	729	84%
APC E 10x5	1	6.9	8.5	58	6000	486	84%
APC E 10x5	1	7.9	10.5	82	6720	614	83%
APC E 10x5	1	8.8	12.7	111	7380	752	81%
APC E 10x5	1	9.8	15	147	8010	887	79%
APC E 10x5	1	10.8	17.35	187	8630	1029	78%
GWS HD 10x6	1	6.9	8.5	58	6030	491	85%
GWS HD 10x6	1	7.9	10.5	82	6720	619	83%
GWS HD 10x6	1	8.9	12.5	111	7410	749	81%
GWS HD 10x6	1	9.9	14.7	145	8040	890	79%
GWS HD 10x6	1	10.8	17.1	184	8640	1015	78%
APC SF 11x3.8	1	6.9	11.7	80	5610	643	79%
APC SF 11x3.8	1	7.8	14.4	112	6510	793	81%
APC SF 11x3.8	1	8.8	17.45	153	6660	960	73%
APC SF 11x3.8	1	9.8	20.6	201	7110	1126	70%
APC SF 11x3.8	1	10.7	23.6	252	7470	1255	68%
APC E 11x5.5	1	6.8	10.8	73	5700	604	81%
APC E 11x5.5	1	7.8	13.4	104	6360	755	79%
APC E 11x5.5	1	8.8	16	140	6900	905	76%
APC E 11x5.5	1	9.8	18.8	184	7410	1040	73%
APC E 11x5.5	1	10.7	21.6	231	7860	1202	71%

Motor: Justgofly - 500XT (12-magnet), 68g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	RPM as % of Kv*V
APC SF 7x6	1	7	2.75	19	5790	157	92%
APC SF 7x6	1	7.9	3.4	26	6480	199	91%
APC SF 7x6	1	8.9	4.25	37	7230	250	90%
APC SF 7x6	1	9.9	5.1	50	7860	304	88%
APC SF 7x6	1	10.9	6	65	8490	355	87%
APC Sport 7x7	1	6.9	2.75	18	5760	136	93%
APC Sport 7x7	1	7.9	3.3	26	6510	178	92%
APC Sport 7x7	1	8.9	3.9	34	7260	222	91%
APC Sport 7x7	1	9.9	4.6	45	7980	270	90%
APC Sport 7x7	1	10.9	5.3	57	8670	320	88%
APC Sport 7x7	1	14	7.9	110	10710	489	85%
APC Sport 7x7	1	14.6	8.55	124	11160	534	85%
GWS HD 8x4	1	6.9	2.3	15	5880	178	95%
GWS HD 8x4	1	7.9	2.85	22	6630	232	93%
GWS HD 8x4	1	8.9	3.4	30	7320	284	91%
GWS HD 8x4	1	9.9	4.05	40	8100	347	91%
GWS HD 8x4	1	10.9	4.7	51	8760	409	89%
APC E 8x8	1	6.9	5.55	38	5250	222	85%
APC E 8x8	1	7.9	6.85	54	5850	278	82%
APC E 8x8	1	8.9	8.05	71	6420	337	80%
APC E 8x8	1	9.9	9.5	94	6990	402	78%
APC E 8x8	1	10.9	11.1	120	7500	471	76%
GWS HD 9x5	1	6.9	4.1	28	5520	300	89%
GWS HD 9x5	1	7.9	5	39	6180	381	87%
GWS HD 9x5	1	8.9	6.05	53	6810	464	85%
GWS HD 9x5	1	9.9	7.1	70	7440	557	84%
GWS HD 9x5	1	10.9	8.45	92	8070	661	82%
GWS HD 9x7.5	1	6.9	6.25	43	5100	307	82%
GWS HD 9x7.5	1	7.9	7.75	61	5670	378	80%
GWS HD 9x7.5	1	8.9	9.3	82	6210	456	78%
GWS HD 9x7.5	1	9.9	11.05	109	6750	540	76%
APC E 9x7.5	1	6.9	6.6	45	5040	296	81%
APC E 9x7.5	1	7.9	8.1	63	5550	371	78%
APC E 9x7.5	1	8.9	9.7	86	6090	443	76%
APC E 9x7.5	1	9.9	11.4	112	6600	531	74%

APC E 9x7.5	1	10.9	13.2	143	7050	608	72%
Graupner SF 10x5	1	6.9	7.15	49	4950	446	80%
Graupner SF 10x5	1	7.9	8.65	68	5460	560	77%
Graupner SF 10x5	1	8.9	10.3	91	5970	661	75%
Graupner SF 10x5	1	9.9	12.1	119	6480	777	73%
Graupner SF 10x5	1	10.9	14.1	153	6930	900	71%
GWS HD 10x6	1	6.9	5.55	38	5220	379	84%
GWS HD 10x6	1	7.9	6.85	54	5850	476	82%
GWS HD 10x6	1	8.9	8.15	72	6390	572	80%
GWS HD 10x6	1	9.9	9.65	95	6930	676	78%
GWS HD 10x6	1	10.9	11.2	122	7500	785	76%
GWS HD 10x8	1	6.9	8.4	57	4680	386	75%
GWS HD 10x8	1	7.9	10.2	80	5190	469	73%
GWS HD 10x8	1	8.9	12.2	108	5610	557	70%
GWS HD 10x8	1	9.8	14.2	139	6000	639	68%
GWS HD 10x8	1	10.8	16.4	177	6360	717	65%
Graupner SF 11x5	1	6.8	8.55	58	4650	526	76%
Graupner SF 11x5	1	7.8	10.55	82	5160	646	74%
Graupner SF 11x5	1	8.8	12.45	109	5580	764	70%
Graupner SF 11x5	1	9.8	14.7	144	6000	885	68%
Graupner SF 11x5	1	10.8	17.7	191	6360	1000	65%
APC E 11x5.5	1	6.9	6.75	46	5010	454	81%
APC E 11x5.5	1	7.9	8.35	65	5520	568	78%
APC E 11x5.5	1	8.9	10	89	6030	683	75%
APC E 11x5.5	1	9.9	12	118	6540	814	73%
APC E 11x5.5	1	10.9	14.1	153	6960	933	71%
GWS HD 11x7	1	6.9	7.9	54	4800	508	77%
GWS HD 11x7	1	7.9	9.65	76	5310	623	75%
GWS HD 11x7	1	8.9	11.6	103	5790	746	72%
GWS HD 11x7	1	9.9	13.5	133	6210	855	70%
GWS HD 11x7	1	10.9	15.6	170	6540	964	67%
GWS HD 12x8	1	6.9	10.5	72	4380	598	71%
GWS HD 12x8	1	7.4	11.7	86	4530	666	68%
GWS HD 12x8	1	7.8	12.7	99	4740	720	68%
GWS HD 12x8	1	8.8	15.1	132	5100	832	64%
GWS HD 12x8	1	9.8	17.8	174	5370	945	61%
GWS HD 12x8	1	10.8	20.3	219	5580	1030	57%

Motor: E-max - BL 2810/12, 86g

Propeller	Gear Ratio	Volts	Amps	Watts	RPM	Thrust (g)	Thrust (oz)	RPM as % of Kv*V
APC E 9x6	1	6.9	13.2	91	7050	576	20.32	76%
APC E 9x6	1	7.9	15.85	125	7770	706	24.9	73%
APC E 9x6	1	8.9	18.6	165	8490	843	29.74	71%
APC E 9x6	1	9.9	21.6	213	9120	974	34.36	68%
APC E 11x5.5	1	6.9	14.4	99	5910	643	22.68	63%
APC E 11x5.5	1	7.9	16	126	6450	775	27.34	60%
APC E 11x5.5	1	8.9	18.1	161	6870	892	31.46	57%
APC E 11x5.5	1	9.9	21.7	214	7350	1055	37.21	55%
APC E 11x5.5	1	10.9	24.6	268	7740	1184	41.76	53%