Electric-plastic fracture mechanics investigation on offshore structures and materials

Mohammad Zaidi Ariffin

2018

Mohammad Zaidi Ariffin. (2018). Electric-plastic fracture mechanics investigation on offshore structures and materials. Doctoral thesis, Nanyang Technological University, Singapore.

<https://hdl.handle.net/10356/87649>

<https://doi.org/10.32657/10220/46779>

**NANYANG
TECHNOLOGICAL
UNIVERSITY**

SINGAPORE

**ELASTIC-PLASTIC FRACTURE MECHANICS
INVESTIGATION ON OFFSHORE STRUCTURES AND
MATERIALS**

MOHAMMAD ZAIDI BIN ARIFFIN

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JOINT PH.D. PROGRAMME WITH UNIVERSITY OF SOUTHAMPTON

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A thesis submitted to the Nanyang Technological University & University of Southampton

in partial fulfilment of the requirement for the degree of

Doctor of Philosophy (Mechanical Engineering)

2018

Abstract

Failure of offshore structures and materials, with complex defects such as multiple cracks and crack-in-corrosion (CIC) that are exposed to extreme tension and bending loads accompanied by high internal pressure, could pose serious complications if not assessed accurately. Existing failure-fracture assessment procedures widely used in the industry are not accurate for situations with large plastic deformation as they are load-controlled based mainly on linear elastic fracture mechanics. In addition, there are no handbook solutions and standards on CIC cases. The thesis demonstrates the novel and high potential applications of nonlinear elastic-plastic fracture mechanics methodologies and assessment protocols to assessing 3-D crack-like defects in offshore structures and pipelines under large strain loading. This project is also a pioneering attempt to use full-field measurement technique (using Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC)) for failure analysis of corroded and CIC offshore materials which might allow for in-situ fracture assessment of defects. Currently there is no standard technique to obtain the J-integral. The advantage of our technique is that the precise location of the crack tip is not required. In addition, the failure analysis on pipelines containing multiple coplanar cracks has been carried out comprehensively with a newly accurate strain-based CTOD estimation schemes proposed (with a predicted error limit of $\pm 5\%$). For offshore pipelines, there are significant differences for failure assessment of cracks with/without corrosion damage. The new findings and the proposed simplified corrosion numerical defect model can provide a quick decision tool based on based on load and strain demand/capacity (CTOD and critical CTOD), defects number, size and locations.

Acknowledgements

Firstly, I would like to express my deepest gratitude to my supervisor, Assoc. Prof. Xiao Zhongmin, for his patient guidance, enthusiastic encouragement and useful advice in this beginning of my graduate research. In addition, I would like to show my greatest appreciation to my co-supervisor, Prof. Ajit R. Shenoi, for his guidance while I was in University of Southampton.

Next, I would like to thank Dr. Wang Yikun for her guidance in doing my corrosion experiments and all the advice she gave me to ensure that I was able to do the experiments and testing. In addition, I would like to express my gratitude to TSLR lab personnel and peers especially Dr. Andrew for teaching and demonstrating the techniques for me. I would also like to thank Dr. Zhang Yanmei, my Ph.D. senior, for her advice and assistance in this field. I am grateful for the CAE Lab 1 technicians for the use of the computers and resources in running the programs that I require for this project.

The authors gratefully acknowledge the insights gained into TSA, DIC concepts and interpretation of experimental results from discussions with Professor J.M Dulieu-Barton of the University of Southampton.

Finally, I wish to thank my fellow NTU graduate colleagues and my parents for their support and encouragement throughout this starting phase of my graduate research.

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List of Abbreviations

2-D	Two-Dimensional
3-D	Three-Dimensional
API	American Petroleum Institute
BS	British Standard
B	Bending
CIC	crack-in-corrosion
CTOA	Crack Tip Opening Angle
CTOD	Crack Tip Opening Displacement
CTOD _s	Semi-elliptical surface Crack Tip Opening Displacement
CTOD _L	Elliptical embedded Left Crack Tip Opening Displacement
CTOD _R	Elliptical embedded Right Crack Tip Opening Displacement
DIC	Digital Image Correlation
DNV	Det Norske Veritas
EPFM	Elastic-Plastic Fracture Mechanics
FE	Finite Element
HRR	Hutchinson, Rice and Rosengren
IP/ P	Internal Pressure
LEFM	Linear Elastic Fracture Mechanics
NDE	Non-destructive Examination
NDT	Non-destructive Testing
SENB	Single-Edge Notched Bend
SENT	Single-Edge Notched Tension
SIF	Stress Intensity Factor
T	Tension
T + IP/ T + P	Biaxial Loading (Tension)
T + IP/ T + P	Biaxial Loading (Bending)
TSA	Thermoelastic Stress Analysis

List of Notations

δ	Crack Tip Opening Displacement(CTOD) value
$\delta_{critical}$	Critical CTOD of the material containing the crack
ΔK	Stress intensity factor range
ΔT	Change in temperature
ε_0	Reference strain
ε_g	Global strain
ε_{unc}	Strain of un-cracked body
ε_{xx}	Normal Strain in x direction
ε_{xy}	Strain in x-y direction
ε_y	Yield strain
ε_{yy}	Normal Strain in y direction
γ	Interaction factor
σ	Applied stress
σ_0	Reference stress (equals to yield strength)
σ_{ij}	Stress tensor
σ_H	Hoop stress
σ_{max}	Maximum stress applied in fatigue cycle
σ_{min}	Minimum stress applied in fatigue cycle
σ_{unc}	Stress of un-cracked body
σ_x	Principal stresses in the x
σ_y	Stresses in y direction
σ_Y	Yield stress
θ	Angle in polar coordinates
ν	Poisson's ratio
w	Strain energy
a	Minor axis length of ellipse, crack length (for SENT specimen)
a_e	Elliptical embedded crack height
a_{eff}	Re-characterized effective surface crack depth

a_s	Semi-elliptical surface crack depth
c	Major axis length of ellipse
c_e	Elliptical embedded crack length
c_{eff}	Half-width of effective re-characterized crack
c_s	Half-width of semi-elliptical surface crack
$\frac{c}{a}$	Crack Shape Ratio
d	Depth of corrosion
D	Outer diameter of pipe
E	Young's modulus
k	Thermoelastic constant of the material
l	Length of the corrosion
l_s	Length of SENT specimen
L	Length of segment of pipe
J_R	Critical fracture toughness of the material
K	Stress intensity factor
$K_{I,A}$	Mode I stress intensity factor for a pair of cracks
$K_{I,A,emb}$	Mode I stress intensity factor for a pair of embedded cracks
$K_{I,A,surf}$	Mode I stress intensity factor for a pair of surface cracks
K_{in}	Stress intensity factor with the interaction influence
K_{is}	Stress intensity factor without the interaction influence
M	Ratio for a pair of cracks that represents the stress reduction effect of finite crack spacing
n	Strain-hardening exponent
P	Internal pressure / Load
r	Length in polar coordinates, the distance away from the crack tip
s	Separation distance between the crack
S_{crack}	Crack area
S_{pipe}	Cross-sectional area of the pipe
t	Wall thickness of the pipe / thickness of the material
t_s	Thickness of SENT specimen
T, T_0	Mean temperature of the surface

T_i	Dot product of traction,
u_{y_i}	Displacement in y direction
w	Width of the corroded area
w_s	Width of SENT specimen

Chapter 1

1 Introduction

1.1 Background

With the increasing global demand for oil and gas over the past few decades, more and more offshore structures and platforms are installed to extract these hydrocarbons. However, due to the recent oil price moderation, building such structures proved to be an economical challenge. As such, offshore structures must be designed to be cost effective that would allow these structures to withstand the potential risks of damage due to corrosion, waves, sea conditions, soil instability and other hazards. In addition, quite a number of these offshore structures are near to its end of operational lifetime. To enhance asset management of ageing systems, measurement and prediction of damage development and the strength resilience during service life are crucial. Offshore structures particularly subsea pipelines and platforms are exposed to the seawater environment which leads to corrosion damage. NACE International reported that the global cost of corrosion is estimated to be US\$2.5 trillion (4% of the global 2013 GDP) [1]. Proper corrosion assessments in terms of mechanical integrity are critical for safe, reliable, efficient and cost savings of these corroded structures [2]. This causes huge downtime which plagues the oil and gas industries today.

One vital component of the offshore structures is the offshore pipelines which are the major transportation tools for oil and gas. These pipelines have been utilised since the 1860s as they provide economical high capacity transportation of natural gas, oil and other products. These

pipelines have spent several decades in a seawater environment which results in corrosion damage. Thus, it is more pertinent for an improved integrity assessment for defects such as corrosion, cracks or dents. Some crack defects are due to coating or cathodic protection degeneration. Corrosive environment and damage during fabrication are also factors that may lead to pipeline defects. In addition, the extreme loading conditions, such as tensile load accompanied by high internal pressure, could cause severe damage and possible failure for offshore pipelines used for oil and gas transportation. It is widely recognized that existing fracture assessment procedures which are based on load-controlled method are not explicitly designed for situations where the structures (materials) are under large plastic deformation. These current failure assessment methods make the estimation of failure unreliable.

1.2 Motivation

The study of complex defects such as multiple interacting cracks and crack-in-corrosion (CIC) in offshore engineering structures and materials has become increasingly important.

In particular, various cracks may be introduced to the pipeline due to imperfect manufacturing processes [3]. As such, under complicated loading conditions, small closely distanced cracks can grow and coalesce into a large one which poses serious threat to the integrity and safety of the structures. The fracture behavior due to interacting cracks, when the distances among cracks are close, needs to be studied to improve the accuracy of failure prediction [4]. Furthermore, due to pulling, bending and twisting of pipelines during the construction phase, offshore pipelines are exposed to large plastic loads which pose tremendous challenges to the structural integrity of pipelines [5]. These large plastic strains could be up to a global strain of 3% in a typical reeling

situation [6]. Majority of the codes and standards employ load-based criteria for their failure analysis and as mentioned, it is crucial to develop strain-based criteria instead based on the strain loads these structures are exposed to.

Offshore structural platforms are exposed to harsh environments. One major threat to the structural integrity is corrosion, which may occur due to inadequate coating applications or inspections. Furthermore, localised corrosion such as pitting causes high stress intensity regions that possibly develop into cracks [7] which has severe and occasionally catastrophic consequences. Since corrosion can lead to material degradation and cracking, the integrity of such structures can be considerably reduced [2]. The understanding of the effects of structural integrity due to corrosion and corrosion induced cracks is important but continues to be a challenge in both measurement and assessment.

Therefore, determining the exact limits for fitness-for-service of these offshore structures is especially important for the offshore industry where pipes are continually subjected to tremendous sea-wave loading and pressures at great depths. This is done by:

1. Based on the operational loads and allowable crack size, **critical material toughness** is to be determined.
2. To check for **critical crack size** that will lead to failure due to a known loading.
3. To check for **critical load** that will lead to failure with a known crack size from NDT and material toughness.

The current research focuses on calculating and estimating the critical **crack size** and **critical load** (especially strain- loading).

1.3 Problem Statement

There is a high demand to determine a correct and uniform safety level when corrosion and cracks exist in these structures. Thus, monitoring the life limits in terms of fitness-for-service based on failure analysis for offshore pipelines will be the focus of the current study. This project's problem statement targets on how to mechanically evaluate complex defects such as multiple interacting cracks and crack-in-corrosion (CIC) damage in offshore pipelines based on elastic-plastic fracture mechanics. Emphasis will be placed on 3-D elastic-plastic fracture mechanics analysis on offshore structures and materials under large plastic loading. Damage-tolerant designed structures are intended to ensure that should degradation occur during the operational life, typically by fatigue cracking, corrosion or accidental damage, then the remaining structure can withstand expected loads without failure until the damage is detected. It is worth mentioning that the present study aims at developing a failure analysis protocol that can be applied to the offshore pipelines industry based on fracture mechanics.

1.4 Objectives

With reference to the problem statement highlighted in the previous section, the framework of this thesis is prepared to fulfil the following objectives:

- To analyse and interpret existing industrial design codes and recommended practice standards for offshore pipelines, especially for three-dimensional corrosion and crack defects on pipelines.
- To utilise full-field measurement techniques for failure analysis of corroded offshore materials.

- To perform elastic-plastic fracture and fatigue investigations for 3-d multiple interacting cracks and crack-in-corrosion (CIC) cases for offshore pipelines.
- To develop strain-based failure assessment procedures for fracture limit state evaluation of complex defects in offshore pipelines with high plastic deformation.

Ultimately, this project targets to mechanically evaluate complex 3-d defects in offshore steel structures and materials based on elastic-plastic fracture and fatigue analysis.

1.5 Study Approach

To narrow the scope of the project, failure analysis for offshore pipelines is the focus, as there is a high demand to determine a correct and uniform safety level when cracks exist in these structures. The research is conducted in 3 main stages as shown in Figure 1-1 to address the problem statement.

Figure 1-1 Overview of Study Approach

Since large-scale experiments and testing of damaged offshore pipelines segments are limited by the resources available, the project started with Single-Edge Notched Tensile (SENT) specimens to observe material behavior and explore non-destructive examination methods for crack parameters which were then validated with SENT numerical models. This can be visualized in Figure 1-2. It is then scaled up for offshore pipelines by numerical models.

Figure 1-2 SENT Specimen from offshore pipeline

The first stage was to utilize full-field experimental techniques to analyse crack-in-corrosion cases. Next, a fracture mechanics-based numerical approach was developed to quantify the influence of crack-in-corrosion defects on the structural integrity using Single-Edge Notched Tension (SENT) specimen. It was then scaled up to conduct 3-d non-linear elastic-plastic fracture mechanics analysis for corroded offshore pipelines with surface cracks subjected to biaxial loadings. The last stage was to study the elastic-plastic fracture behavior for 3-D interacting cracks (Surface and embedded cracks) in the flawed pipelines. The results from these stages were compared with handbook solutions and industrial codes and recommended practice standards for offshore

pipelines. Overall, this thesis provides a novel fracture mechanics-based approach for assessing multiple interacting 3-d cracks and crack-in-corrosion damage of offshore steel structures.

1.6 Outline of Thesis

The report begins with the introduction of the research project in this chapter. Chapter 2 presents a literature review on the fracture analysis and assessment for offshore structures. Existing fracture assessment, industrial codes and standards, numerical and experimental fracture research studies, on offshore structures, mainly on the offshore subsea pipelines, were summarized. Studies done on complex defects such as multiple cracks and crack-in-corrosion cases were critically evaluated and described.

Chapters 3 and 4 explore experimental investigations on failure of high strength steel with corrosion defects. These two chapters examine the capability of the full-field experimental techniques of Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC), respectively, to evaluate corrosion and crack-in-corrosion cases on the failure of these materials using a SENT specimen. In Chapter 5, 3-d elastic-plastic stress investigation has been conducted for cracks embedded in corrosion damaged zones of offshore steel structures. Here, an assessment protocol is proposed using numerical method on the SENT specimen from an offshore steel structure to perform failure analysis of these corroded materials. Chapter 6 provides a non-linear elastic-plastic stress analysis for corroded offshore pipelines with 3-d surface cracks subjected to biaxial loadings. Chapter 7 deliberates on the non-linear elastic-plastic stress investigation for two interacting 3-D cracks in offshore pipelines. It presents the findings of this investigation for

interacting cracks under biaxial loading. The thesis concludes with the novelty of the thesis explained in Chapter 8.

Chapter 2

2 Literature Review

The most common causes of damage and failures in offshore oil and gas transmission pipelines are external interference (mechanical damage) and corrosion. Therefore, this chapter reviews the current understanding of mechanics of offshore pipelines with its codes and standards for these offshore pipelines. Then, the current research status on elastic-plastic fracture mechanics investigations for offshore pipelines is evaluated, which covers gaps in complex defects such as multiple cracks interactions and crack-in-corrosion (CIC). Next, experimental techniques to assess the defects on offshore materials are examined. The literature survey provides the critical gap that the research work is embarking on.

2.1 Mechanics of offshore pipelines

Offshore pipelines are usually made of steel alloys with considerable plastic deformation capacity. As pipelines failures occur mainly due to cracking [8], fracture mechanics has to be employed to evaluate the tolerance of potential flaws such as cracks initiated in offshore pipelines. There are two main phases, during installation and in operation, when loads are applied to offshore pipelines. These pipelines are bent, pulled and twisted during the construction phase as seen in Figure 2-1 [5], inducing tension and bending stresses that cause large plastic strains on the offshore pipelines [9].

Figure 2-1 Schematic of S-lay Barge Construction that induces bending & Tension Loading [5]

In the assembly of the pipelines, a girth welding method is used to connect the pipelines together. Mechanical flaws such as surface or embedded cracks are unavoidably introduced into the welding zone during the construction process. Girth welds contain imperfections due to lack of fusion, under-cuts, porosity and slag inclusion which is of certain height and length at certain location along the longitudinal direction of weld. Furthermore, due to the loads mentioned earlier, the girth welds are exposed to large plastic bending loads with high internal pressure which pose tremendous challenges to the structural integrity of pipelines. These large plastic strains could be up to a global strain of 3% in a typical reeling situation [6]. Therefore, failure analysis on girth welded pipelines under large plastic deformation has obtained increasingly significant attention by researchers [10-12].

Also, the topographical features of the seabed create irregularities on the seabed which cause a bent and pulled effect on these pipelines and stresses due to low depressions and elevated obstructions [3]. The schematics in Figure 2-2 show the bending and tension loading due to these irregularities. These created external forces to act on the pipeline. In addition, due to unsupported weight of the pipeline section and dynamic waves and currents loads, pipeline free spanning occurs, which makes the pipeline oscillate at a frequency. This in turn results in excessive yielding and fatigue and eventually lead to catastrophic failure [13]. Investigations have been done to better understand the long-term and rather random environmental loads on the offshore pipelines [14]. Ground movement in the Arctic and sub-arctic regions is so significant where the longitudinal strain generated could be up to 2.5%.

Figure 2-2 Pipe configuration due to low depression and elevated obstruction

During operation, pipelines are loaded by internal pressure from the oil or gas transported, and external pressure from the sea water, as well as stresses induced by temperature changes. For pipeline design, the effect of the internal pressure cannot be neglected, since it changes the effective axial stresses which result in global buckling of pipelines [15]. Research has been

conducted on the effective axial force to account for pressure effects and on the frequency of a span [16].

Apart from these two main phases, pipelines are subjected to accidental loads such as impacts from trawl gear and anchors which cause large global displacements from its original position, i.e., large local strains [17]. As the pipelines are sometimes partly buried on the seabed, sand-pipeline-trench lateral interaction takes place which causes movement of the pipes [18].

2.2 Fracture Mechanics Analysis and Assessment in Offshore structures – pipelines

With the advances in numerical methods, detailed elastic-plastic fracture mechanics simulations can be carried out in practice. This enables fracture mechanics assessments in determining the J integral for various loading conditions, materials and crack geometries. J-integral is a path independent contour integral that quantifies the rate of change in potential energy, with respect to crack length, within a region of material that contained a singularity. Rice [19] showed that J integral is path-independent, it is not necessary to determine the crack tip fields with high accuracy close to the crack tip. EPRI (Electric Power Research Institute) methodology [20], which is based on deformation plasticity and combining linear elastic solutions with elastic-plastic crack problems, has been widely used to analyze the elastic-plastic fracture behaviors. Various modifications have been made for practical problems such as the research work by Zahoor [21] which includes circumferentially and axially cracked pipes under tensile and bending loads. Chiodo and Ruggieri [22] applied a new J-integral formulation that combined an unloading

compliance technique to provide a set of J-integral and crack-tip opening displacement (CTOD) solutions for flawed pipelines with a surface crack under bending moment.

Currently, more complex situations are analysed by finite-element (FE) analysis, applying large-deformation program suites such as ABAQUS. Numerous studies have been performed using 3-D FE analysis on semi-elliptical surface cracks in wide plates [23, 24]. Chen et al. [24] mentioned that the existing criteria for defect assessment based on limit loads may not be optimum for high grade pipeline steel as they do not reflect the actual mechanism of the material (stable crack growth prior to maximum load). Stress intensity factors calculated using global-local FE methodology for pressure vessel as analytical solutions are only valid for a uniform stress distribution [25]. On the other hand, CTOD-based criterion [26] has the advantage of easy implementation and can be applied to variety of crack growth situations especially for elastic-plastic crack growth [27]. The CTOD criterion assumes that fracture occurs when the CTOD reaches or exceeds its critical CTOD. Thus, more research has emerged to analyze the fracture behavior of offshore pipelines using FE analysis to provide a strain-based estimation procedure for CTOD with pipelines subjected to large plastic deformation under tension [28] and bending [22, 29].

Apart from FE analysis, Berg et al. [30] has done a comparison study between a full-scale four point bending test of a tubular pipe and numerical simulations. It shows that the effects of biaxial loading on the strain capacity in both cases are similar. Likewise, to accurately measure the pipeline steel toughness due to its ductile crack propagation, an experimental technique has been conceived to measure crack tip opening angle (CTOA) instead, which proves to be a promising and convenient alternative fracture criterion [31]. For this thesis, CTOD is defined as the

displacement between the intercepts of the two 45° lines (90° vertex) from the blunted tip to the crack flanks as seen in Figure 2-3.

Figure 2-3 Illustrations of crack tip opening displacement(CTOD)

2.2.1 Codes and standards for offshore pipelines

Pipeline codes that deal with service specifications, standards and recommended practices are highly regarded within the international pipeline and offshore community. Major design and fitness-for-service codes provide pipeline design strategy such as API [32] and DNV [33]. As these codes such as DNV-RP-F108 [34] are mostly load-controlled methods, current codes and standards for fracture assessment of offshore pipelines offers a partial representation and proved to be too conservative for engineering practice. DNV-RP-F108 [34] was the first guideline targeting fracture assessment corresponding to large plastic strain in 2006, which recommends the use of BS7910:2013 [35] with some modifications for offshore pipelines under large plastic strains. However, BS7910:2013 [35] is not suitable for such plastic strains since it uses reference stress

methods and consideration of stresses acting on a flawed section. Moreover, non-linear elastic plastic fracture mechanics theory and non-linear stress-strain constitutive relations of materials under large deformation are not reflected in these current codes of practice. Thus, these codes are highly conservative in providing failure assessment on pipelines under strain-controlled loads exceeding initial yield [36].

As mentioned in DNV cutting edge projects 2012, there is an unnecessarily conservative weld defect acceptance criterion. Current DNV-OS-F101 [33] assessment procedure is unnecessarily conservative and that a reliability-based approach would reduce costs due to fewer repairs of weld defects, less intervention work and easier verification by third parties. A reliability based fracture mechanics approach will ensure more consistent results from different standards and codes providers and make it easier to verify the safety and reliability of other assessment procedures [37]. To increase the accuracy of the fracture mechanics analysis, a strain-based method is desirable for fracture assessment on offshore pipelines under large plastic deformation.

In DNV-OS-F101, the definitions of defects are noted. Here, a *Single Defect* is one that does not interact with a neighbouring defect. As such, pipeline burst failure pressure with a single defect is independent of other defects in the pipeline. On the other hand, an *Interacting Defect* is one that interacts with neighbouring defects. Usually, the failure pressure of an interacting defect is lower than the failure pressure of the individual single defects. Thus, it is of interest to study interacting defects to check how does this failure differs and the variables that will affect it. In our cases, 2 interacting cracks and crack-in-corrosion are the interacting defect phenomena that are being investigated.

Current practice employs semi-empirical methods in the assessment of corroded pipeline integrity, coded methods with well-established company methodologies and assessment of the accuracy of semi-empirical methods further increase engineers' confidence in applying them routinely in the field. A couple of these semi-empirical methods are from ASME B31G [38] and DNV-RP-F101 [39]. Here these general corrosion defects are characterised as either rectangular or parabolic shapes. Failure analysis are based on plastic collapse and are widely used in practice providing conservative results.

2.2 Multiple Cracks Analysis

Numerous research efforts have been made to investigate the interaction effect of 3-D multiple cracks through computing numerically the stress intensity factors for the interacting cracks. Body force method is initially used to obtain numerical solutions for various crack configurations and aspect ratios [40]. Isida et al. [41] studied the interaction of two parallel semi-elliptical surface cracks in a semi-infinite solid under tension and proposed dimensionless quantities to quantify these effects as follows:

$$M = \frac{K_{I,A}}{(K_{I,A})_{s \rightarrow \infty}} \quad (2-1)$$

$$R = \frac{K_{I,A,surface}}{K_{I,A,embedded}} \quad (2-2)$$

where $K_{I,A}$ is the mode I stress intensity factor (SIF) for a pair of cracks, M is the ratio for a pair of cracks that represents the stress reduction effect of finite crack spacing while R represents the free surface effect of the semi-infinite solid.

In addition, researchers have employed finite element (FE) analysis to study the interacting 3-D cracks [42-46]. One of them, Moussa et al. [42] proposed an interaction factor defined as:

$$\gamma = \frac{K_{in}}{K_{is}} \quad (2-3)$$

where K_{in} and K_{is} are SIFs with and without the interaction influence, respectively. When $\gamma < 1$, shielding effect occurs for parallel crack interactions. As for coplanar semi-elliptical surface cracks subjected to bending, Sethuraman et al. [46] provided an empirical expression for the estimation of interaction factors γ in terms of non-dimensional crack configuration parameters. It is found that the interaction is negligible when $2c/s$ is less than 0.2 irrespective of crack aspect ratio and crack depth, where c is the half-width of surface crack and s is the distance between the cracks. Most of such studies are done for elastic analysis with SIFs being the fracture parameter.

Analytically, the strip yield model (Dugdale [47] and Barenblatt [48]) proposed in 1960s is extensively applied to solve multiple crack damage problems as it is relatively simple to formulate the stress field of the body. The crack is imposed by a remote tensile stress and both effective crack tips carry the closure yield stress. The stress intensity factors induced by the tensile stress and closure yield stress at the effective crack tips are summed together to give the solution. This model approximates the elastic-plastic behavior of a crack by calculating the plastic zone ahead of the crack tips using the superposition of two elastic problems. With the size of the plastic zone obtained, various methods and techniques have been proposed to approximate the stress intensity factor (K) and CTOD equations [49] such as using the weight function approach [50-52], boundary element method [53] and energy approach [54, 55]. For multiple cracks cases, investigations are

implemented mainly by numerical method. A closed form analytical solution for the simplest multiple-crack case was derived by Collins and Cartwright [56] based on complex stress approach.

Most of the aforesaid research work are restricted to elastic investigations and do not address the elastic-plastic nature of these offshore engineering structures. Recently, there have been more interests addressing elastic-plastic stress analysis in offshore pipelines to address the nonlinear fracture behavior of flawed structures [57]. With 3-D FE simulations, a series of parametric studies have been performed on the CTOD evolution of a cracked pipeline subjected to large plastic bending [29, 58, 59]. In addition, the integrity assessment equation has been improved for elastic-plastic pipe bend or elbow with through-wall circumferential cracks [60, 61].

On the other hand, published design standards and codes focus only on single crack defects and do not cater for multiple cracks which have a larger potential to cause structural failure earlier than expected. One such design standards is the BS7910:2013 [35]. In BS7910:2013, it provides a combination rule for multiple cracks that re-characterized the cracks into one effective crack. BS7910:2013 combination rule of effective crack states that no crack interaction is considered if $s > (a_s + a_e)$. In other words, as can be seen in Figure 2-4, the two cracks case can be regarded as two single cracks without interaction if the two cracks are separated by larger than the critical interaction distance, $s > a_s + a_e$.

Figure 2-4 Schematic diagram of the effective crack height and crack length for two interacting cracks (extracted from BS7910:2013).

2.3 Corrosion and crack-in-corrosion for offshore materials

Offshore structural platforms are exposed to harsh environments. One major threat to the structural integrity is corrosion, which may occur due to inadequate coating applications or inspections [2]. Corrosion is a chemical reaction that decreases the amount of material present and enhances mechanical damage mechanisms. It is often evidenced in steels in the form of rusting or pitting. Corrosion reactions are often shown in terms of chemical reactions where:

Ferrous hydroxide – unstable in oxygenated water – oxides to ferric hydroxide (rust):

Usually, the physical properties of the metal will deteriorate as well. Here, there two main forms of corrosion which are of interest; general/uniform corrosion and pitting corrosion. General/uniform corrosion is a case where there is a uniform layer of penetration over all exposed metal surfaces. It results in one of the greatest destruction of metal on an overall basis. However, it does not cause too much problems, as the amount of corrosion and removal of material is quite small. In pitting corrosion, corrosion is highly localized and takes the form of cavities [62]. It produces sharp and defined pits, with varying cross-sections. The pits formed may be very close to each other such that the surface of the metal seems to be roughened, or may be very isolated. In some cases, pit corrosion forms in sensitive areas like welds or bends increases its complexity and tendency for the whole structure to fail. Furthermore, localised corrosion such as pitting causes high stress intensity regions that possibly develop into cracks [7], and leads severe and occasionally catastrophic consequences. The minute dimensions of pit corrosion might pass undetected and intensifies the stress around it which could lead to fracture even for defects smaller than 1mm [63]. Since corrosion causes material degradation and cracking, the integrity of such structures can be considerably reduced [2]. The understanding of the effects of structural integrity due to corrosion and corrosion induced cracks is important but continues to be a challenge in both measurement and assessment.

As it is understood for pipelines, internal and external pressures and bending moments are regarded as the most important loading conditions. From earlier mentioned, pipelines sustain huge external

pressures from water and soil, internal pressures from transmitted oil or gas and bending moments due to thermal expansion and curved lay-out. When these pipes are corroded externally or internally (or both), burst and bending capacities are decreased; thus, the structural integrity is significantly affected [38]. Therefore, guidelines are needed for the estimation and calculation of these reductions. Several analytical methods are available to assess the failure pressure of pipelines containing corrosion defects in the form of wall thickness reduction. For steel plates, an empirical formula for a strength reduction factor was found in the literature [2]. However, the application of the formula is quite limited. A corroded plate compressive ultimate strength reduction factor is introduced by taking the smallest cross-sectional area into consideration. Furthermore, from experimental and numerical analyses, a plate under pit corrosion is converted to one with an equivalent plate thickness under general corrosion with the same strength reduction [64]. This conversion provides an alternate accounting for corrosion effects.

However, there are limitations to assessing corrosion defects this way. In an assessment of a defect, only the material grade (giving tensile strength and yield strength) will usually be available. The actual material properties at the location of the defect will not be known. Furthermore, the defect sizing will be determined with some level of uncertainty [65]. The defect can be shallower, or deeper, than the measured value. As such, instead of taking into consideration this level of uncertainty, it would be beneficial if there is a direct technique that could calculate the fracture parameters of such defects instead of getting accurate crack and corrosion parameters.

In offshore pipelines, FE approach is also used to assess residual strength and deformation capacity of corroded pipelines. The current practice in engineering for generalised corrosion is to consider

it as wall thickness reduction [66-68]. Plenty of these studies involve predicting the failure pressure using FE analysis [69-72] while studies have been done on complex corrosion that involve corrosion interactions [73, 74]. In addition to failure pressure, bending capacity of these corroded offshore pipelines were investigated analytically [75], using FE analysis [76] and validated with experimental data [77]. On the other hand, plenty of pitting corrosion defects have been investigated using semi-empirical and numerical methods [64, 78]. These pitting corrosion defect studies often look into the evolution of pit-to-crack transition [7, 63, 79] which gives rise to the complex defects (crack-in-corrosion) that we were studying in this thesis.

2.3.1 Stress-Corrosion Cracking

There are chemical reactions occurring between the seawater and pipeline which causes corrosion and thus the pipeline properties became detrimental. Stress-corrosion cracking (SCC) [5] occurs when the combination of tensile stress and corrosion cause the pipeline to crack. Evolution of stress corrosion cracks from pits is important in many industrial applications but continues to be a challenge in both measurement and prediction [63]. A stress-corrosion experimental exposure of 5000 hours was done with artificially etched pits showed that 43% of cracks present extended beyond the base of the pit and also broke the surface in the expected manner [63]. However, 50% broke the surface but the pit base was deeper than the crack, and 7% extended beyond the pit base but did not break the surface. Finite element analysis provided evidence that plastic strain is localized on the pits walls below the pit mouth rather at the pit base which was validated by the experimental findings [7]. Furthermore, there are three factors that are necessary for the SCC

initiation and propagation. Firstly, the chemical content present at the pipe wall. Secondly, the pipe is liable to be influenced by SCC. Lastly is the application of adequate tensile stress on the pipeline.

2.3.2 Crack-in-corrosion (CIC)

However, since the complexity of stress corrosion cracking is a challenge in understanding and addressing the issues relating to it, it might be better to study this complex defect by assuming that it is a static problem first. Thus recently, a hybrid defect known as crack-in-corrosion (CIC) [80] has been identified and investigated to determine the integrity evaluation of corroded pipelines. Studies have shown that corrosion damage is at most 22% of depth of material on average and as such, plenty of research has been done for such depths of corrosion defect [71, 81, 82]. There have been studies conducted on limit and stress analysis of corrosion taken as an axial defect [83] and notch [84]. However, these do not consider the complexity of the interactions of lightly corroded specimens (corrosion depth less than 2.5%) with cracks. As such, these CIC defects have yet to be fully explored as this complex defect may require more attention to assess how the defect may affect the remnant strength of the structure. Thus, the fit-for-service of these lightly corroded structures with cracks has yet to be established. For taking into consideration of such CIC and pit-in-corrosion defects, a fracture mechanics-based approach can be applied to evaluate the effects of cracks in corrosion zone on the failure of these structures.

In practice, corrosion defects are commonly evaluated using the defect geometry and a plastic collapse-based criterion such as DNV-RP-F101 for corroded pipelines [85], while several other codes exist to evaluate cracks including BS7910:2013 [35]. Zhang et al. [86] investigated corrosion

on girth welds of offshore pipelines and compared the results with ASME B31G-2012 [87] and DNV-RP-F101 [85] codes. Recently, hybrid defects known as crack-in-corrosion (CIC) [80] have been identified and investigated numerically to determine the failure pressure of corroded pipelines. CIC defects are cracks that are coincident with corrosion as studied by Bedairi et al. [80] in Figure 2-5. There is an insufficient investigation done for CIC defects, thus there is a need to develop new methods in evaluating pipeline integrity with such defect.

Figure 2-5 CIC flaw under Magnetic particle inspection (magnification x0.8) (left arrow indicates corrosion defect and right arrow indicates cracking) [80]

If safety of corroded marine structures is to be ensured, taking into consideration CIC defects and pitting with high stress concentrations that may develop into cracks, then a fracture mechanics-based approach is a logical way forward. In addition, measurement and prediction of damage development during life is desirable to maximise the asset management of an ageing system [67]. The finite element (FE) method has been extensively utilised to predict the failure pressures in corrosion and crack defects [57]. By modelling the corrosion surface for geometrical structural change consisting of a general thickness reduction combined with an equivalent crack due to localised stress concentration of pitting, the influence of corrosion damage on structural integrity can be quantified using fracture mechanics parameters such as the Stress Intensity Factor, K , and the J-integral.

2.4 Single-Edge Notched Tension (SENT) Specimen

In the oil and gas industry, the Single-Edge Notched Tension (SENT) specimen has been increasingly used to determine fracture toughness and resistance curves for applications in strain-based design and fracture mechanics assessment of offshore pipelines containing cracks. In more recent times SENT testing has been enthusiastically adopted by the pipeline industry (eg in DNV-OS-F101 DNV-RP-F108) and gradually been accepted by the pipeline industry in general and in place of the traditional single-edge notched bend (SENB) specimen used for standard CTOD tests as seen in Figure 2-6 [88].

Figure 2-6 An example of a double clip gauge arrangement at the crack mouth of a fracture toughness test specimen [73]

However, the initial testing procedures for SENT specimens gave the fracture toughness exclusively in terms of J-R curves rather than CTOD. J-R curves plot the applied J Integral versus the crack extension curves which show the resistance to stable crack extension measured in terms of J Integral versus ductile crack extension. Although developed for pipeline installation, there is growing evidence that the biaxial loading experienced by pipelines during operation may also exhibit similar ductile tearing resistance as the R-curve measured from SENT testing [22], making SENT specimens appropriate for analysis of pipeline operation as well. The use of SENT specimens to obtain information of fracture toughness of pipeline steels and girth welds has received great interest in this respect. This has been motivated by the similar geometry constraint levels found in SENT specimens and real pipeline defects [89].

Gentile et al. did a study on experimentally determining the critical CTOD in X65 grade steel using circumferentially cracked bars and SENT specimens which were then compared with FE analysis results [90]. High speed video recording was used to observe the CTOD that produced the same accuracy of other contact techniques. Comparison with FEM provides good agreement for both the circumferentially cracked bars and SENT specimens. Thus, the critical CTOD found in this investigation for SENT specimen was given by 0.45mm. In addition, another study reported that the CTOD values for steels of grade X65 is between 0.33mm (lower bound) and 0.57mm (upper bound) [91]. It is quite interesting that the critical CTOD of 0.45mm mentioned in the former research is the average value of these bounds. As the focus of this thesis is to illustrate the possible application of CTOD fracture criterion for elastic-plastic fracture mechanics, the reference CTOD fracture criterion for this thesis will be assumed to be 0.45mm where fracture occurs at this critical CTOD. In other words, for structural failure due to fracture, CTOD exceeds critical CTOD [92].

2.5 Non-Destructive Examination

Currently, Non-Destructive Examination (NDE) techniques do not have the accuracy to detect both the cracks and corrosion since most of these techniques such as eddy-current and X-rays only detect discontinuities in materials rather than the stresses or strains. Studies have also shown that infrared thermography based techniques are only accurate to quantify pitting corrosion defects when there is at least 20% wall thickness loss due to corrosion [93]. Liu et al. [94] proposed a statistical method based on thermography inspection data to characterise pitting corrosion of ductile iron pipes without the need to fully clean the pipes. It was shown that the presence of pitting corrosion can be detected while quantification of the corrosion depth was challenging. Due to these limitations of the current NDE methods, it is crucial to identify a technique that could both assess the corrosion and be able to predict failure of the material using a stress or strain-based technique. It can then be developed to assess the impact of crack induced damage in corroded pipelines. This procedure will require efficient and effective identification and measurement of the stress intensity factor (SIF), K , of the cracks. For this study, Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC) were utilized and the principles behind these techniques are further explained in the following sections.

2.5.1 Thermoelastic Stress Analysis (TSA)

Thermoelasticity utilises small changes in temperature associated with elastic stress under adiabatic conditions, i.e. no heat flow [95]. The temperature change induced by elastic deformation is proportional to the change in first invariant of the Cauchy stress tensor. Based on

thermoelasticity theory, TSA is a non-contact technique that captures full field stress information from the surface of a cyclically loaded component. Thermoelastic theory states that under adiabatic and reversible conditions (material behavior is linearly elastic and material properties such as Young's modulus are not temperature dependent), the change in temperature of the surface, ΔT , is given by:

$$\Delta T = -k T_0 \Delta(\sigma_x + \sigma_y) \quad (2-5)$$

where σ_x and σ_y are the principal stresses in the x and y direction respectively, T_0 is the mean temperature of the surface and k is the thermoelastic constant of the material. For steel components, its elastic properties are not temperature dependent at room temperature. In the case of materials with cracks, using the cyclic stress field near a crack tip, it is possible to evaluate the crack driving force. A schematic of the TSA experimental setup is shown in Figure 2-7.

Figure 2-7 Thermoelastic Stress Analysis: Schematic

Here, a detector receives the IR radiation caused by the temperature change due to stresses change of the structure. The detector then sends the thermal data to a PC (Personal Computer) which translates the information. The PC also controls the camera in terms of the focus and zoom. Emery et al. [96] provided an overview of the TSA technique and its application on composites. Their research used an Instron 8802 test machine with cyclic displacement at a frequency of 10Hz (sufficient to generate adiabatic conditions in a composite material). The thermal signals are recorded using the Delta-Therm system with 25mm lens, stand-off distance of 500mm and thermoelastic data spatial resolution of 0.5mm. A total of 3000 fatigue cycles were taken for the experiment. On the other hand, more related to crack measurements, Diaz et al. [97, 98] experimented on the evaluation of stress intensity factor, K , for fatigue crack growth using TSA. Matt black paint (type no 496-782, RS components) was sprayed on the specimen to increase surface emissivity of components since black body radiation from the metal surface provides more uniform emissivity. As a calibration measure, a strain gauge rosette with two orthogonal strain gauges was bonded to the specimen in a region of uniform stress. The test specimen was a BS 1501 490LT50 ferritic structural steel that was welded together to make a single-edge notched tensile bars, with the notch spark eroded on the weld centre line. This is to facilitate fatigue crack growth. It uses a frequency of 12Hz for R-ratios ($\sigma_{min}/\sigma_{max}$) ranging from 0.1 to 0.58 and the results are validated with effective K range by considering the change in compliance. Thus far, other TSA studies have been conducted only for un-corroded specimens, providing good validation in estimating stresses [95, 99, 100] and stress intensity factor, K [98, 101-103]. For example, Diaz et al. [98] presented the ability of TSA to quantify the effective stress intensity factor, K by comparing with standard technique for crack closure measurement – the strain-offset technique.

Here the strain-offset technique results showed clearly that there was good agreement with those obtained from TSA, showing confidence in the accuracy of the TSA to estimate K .

Nevertheless, there are some limitations and challenges to using TSA especially for crack measurement at the crack tip region [103]. This causes heat generation due to plastic work with the presence of high stress gradients that leads to a change in phase signal. As a result, data very near the crack tip tends to be blurred due to the plasticity and high stress gradients. Another challenge is finding the real crack tip location, noting that visual inspection of thermoelastic images is not an easy task. Tomlinson et al. [104] developed an algorithm that fits mathematical expressions describing the distribution of the sum of principal stresses at the crack tip to experimental data collected at the crack tip region. Additionally, three regions were observed by Diaz et al. [98]. The first region is where no linear behaviour is observed because of the loss of adiabatic conditions due to high stress gradients and crack tip plasticity. The second region is where the linear behaviour is observed and the model is valid. The third region is where deviations from the linear behaviour can be seen, further away from the crack tip. However, despite its limitations and challenges, this technique allows direct measurement of the stress intensity factor, K , with minimal specimen preparations.

2.5.2 Digital Image Correlation (DIC)

Digital Image Correlation (DIC) allows visualization and quantification of deformation of an object. The main output of a DIC analysis is a displacement field that contains a very large number of degrees of freedom (10^3 to 10^4 of kinematic unknowns for 2-D pictures) [105]. This technique

is a non-contact method that removes the need for physical contact between the sensor and tested structure, it avoids possible damage to the surface of the object and allows for faster inspection and post-test measurement to be analysed from the data with different parameters. DIC permits the direct experimental-numerical computation of fracture parameters such as the crack mouth opening displacement, energy release rate and stress intensity factor. Fundamentally, this technique divides the white light images before, during and after the deformation of the specimens. The images are divided into a grid of smaller interrogation cells, or subsets. The correlation between images is undertaken for each subset, creating a full-field array of displacement vector data points across the image. The spatial resolution and precision of the displacements calculated using DIC is limited by the number of pixels within the image and size of the interrogation cells [106]. However, there is trade-off between spatial resolution and measurement accuracy due to limits in the number of pixels within the image and size of the interrogation cells [107]. Here, LaVision DaVis 7.4 correlation software is used that employs a cross correlation algorithm using inverse fast fourier transforms by fitting 2-D Gaussian parameter to the correlation function peak. The basic algorithm of DIC is described by Crammond et al. [107].

DIC has been used to measure $\Delta CTOD$ where multiple points of local measurements by ‘virtual strain gauges’ can be applied along the crack line with data from these points extrapolated up to the crack tip to obtain CTOD values [108-111]. Becker et al. [112] presented a new methodology for evaluation of the J-integral domain based on DIC full-field displacement measurement. For instance, JMAN, used by both Barhli et al. [108] and Becker et al. [112], is a MATLAB script that takes in DIC obtained displacement vectors as a function of applied load and material properties at location points, and calculates the J-integral based on selecting area contour around a region that

contains the crack tip. Both studies had shown reliable measurement of J-integral using this algorithm.

However, the DIC technique only provides measurements on the surface of the specimen, whereas ductile crack initiation usually happens in the centre of a typical fracture mechanics specimen [89]. Care should be taken when applying 2D DIC analysis to such mechanical tests, as significant necking of the specimen may degrade the measured displacement and strain fields [89]. Strain measurements close to the crack path and crack tip is of vital importance as such 3-D analyses should be implemented instead. When digital image correlation is used for measuring displacements, it is difficult to obtain smooth and highly accurate measurement results near the crack because subsets overlap crack faces that have opposite displacements. Therefore, it has been a practice that the displacements near the crack tip and the crack surfaces are excluded when J-integral is evaluated [112].

2.6 Concluding remarks

In this chapter, the mechanics theory and experimental approaches for fracture assessment of offshore pipelines are briefly reviewed. Following that, the current research status on fracture mechanics investigations for offshore pipelines is evaluated, which covers multiple cracks interactions and crack-in-corrosion study using experimental and finite element (FE) analysis. With the literature survey critically reviewed, there are gaps in research on complex defects failure evaluation based on fracture mechanics which are mainly:

- The codes and standards on offshore pipelines are mostly based on load-controlled methods using linear elastic fracture analysis which proved to be too conservative. It is highly

important to conduct elastic-plastic fracture mechanics analysis with strain-based method for offshore pipelines under high global strain (3 – 4%).

- In offshore pipelines with corrosion damaged zone, complexity occurs when surface cracks are initiated due to the interaction between pitting and general corrosion or existing mechanical flaws such as embedded cracks. These complex defects have not been widely investigated yet. In offshore industry practice, these 3-D defects are simplified and treated as 2-D crack cases by the existing codes and standards. It is highly important to study and understand the fracture and failure mechanism of those complex 3-D defects, especially for multiple interacting cracks and crack-in-corrosion (CIC) zones.
- Non-Destructive Examination (NDE) techniques do not have the accuracy to detect both the cracks and corrosion, it is crucial to identify a technique that could both assess the corrosion and be able to predict failure of the material using a stress or strain-based technique. TSA and DIC have yet to be utilised for CIC analysis but show high potential to address this issue.

The next few chapters describe the new research work done on these gaps and the results obtained during the current PhD study that contribute to this field of research.

Chapter 3

3 Experimental investigation on failure of high strength offshore steel (with corrosion defects) – Thermoelastic Stress Analysis (TSA)

In this chapter, the investigation made on crack-in-corrosion (CIC) defects are presented. These types of CIC defects have not been extensively studied before, very few literatures can be found in open literature. In our study, the CIC defects are initially examined with full-field experimental techniques Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC). Following that, experimental investigation has been conducted on CIC defects in Single-Edge Notched Tension (SENT) specimen.

In our work, experimental stress investigations around the crack tips of cracks in corroded high strength offshore steel had been carried out by utilising a full-field stress experimental technique, Thermoelastic Stress Analysis (TSA) [105]. In our testing, the CIC phenomenon can be better understood through full-field stress measurements. The interactions of the cracks with the corroded surface are explored, which have significant impact on the Stress Intensity Factor (SIF) K of the cracks. The useful results obtained show that TSA as a full-field measurement technique is an excellent failure analysis technique for corroded offshore materials.

3.1 Methodology

Eight specimens (with and without pre-cracks) manufactured from structural steel EN 10225 S460G2, were artificially corroded, with the duration of exposure being varied to produce different degrees of corrosion damage. Figure 3-1 shows the coupon size and dimensions used.

Figure 3-1 Dimensions of the pre-crack corroded Specimen

Table 3-1 shows the material properties with the stress-strain response of the material in Figure 3-2. The engineering stress-strain curve of the steel was obtained by tensile testing using an Instron test machine with strain gauges. After the environmental attack, the specimens were mechanically tested. Post-test measurements of specimen thickness, deepest flaw depth and full-field stress field provided a quantitative assessment of the degree of corrosion damage. The machined dimensions and mass of the 8 specimens were noted in Table 3-2.

Table 3-1 Material properties data

Material	Yield Point (N/mm ²)	Tensile Strength (N/mm ²)	Elongation (%)	Young's Modulus (GPa)	Poisson Ratio
EN 10225 S460G2	575	637	27	211	0.3

Figure 3-2 Engineering Stress vs Strain graph of EN 10225 S460G2

Table 3-2 Measured original specimen properties

Specimen	Original Mass (g)	Density (g/mm ³)	Length, l_s (mm)	Width, w_s (mm)	Thickness, t_s (mm)
1	124.4	0.00770	131.1	30.4	4.05
2	124.2	0.00770	130.8	30.2	4.08
3	120.5	0.00768	130.4	30.5	3.95
4	121.2	0.00769	130.7	30.4	3.96
5	121.7	0.00770	130.2	30.4	4.00
6	120.9	0.00775	130.3	30.3	3.95
7	120.5	0.00775	130.3	30.3	3.94
8	120.2	0.00776	130.1	30.3	3.93

3.1.1 Calculation of thermoelastic constant, k , using TSA

Before starting the experimental testing for each specimen, the thermoelastic signals, the constant k had to be determined experimentally from a standard uniaxial tensile test on a calibration specimen. Once k is known, the TSA data can be transformed to give a full-field stress contour plot. To achieve a result of high accuracy, the loading frequency and coating thickness were applied based on a previous comprehensive TSA study for steel specimens [113].

To begin with, one uncorroded and undamaged sample was sprayed with a thin layer of matt black paint (with three passes of estimated thickness of 16 – 20 microns) and tested under load cyclically

with an amplitude of 5kN (41.7 MPa) at a mean load of 10kN (83.3 MPa) and a loading frequency of 10 Hz, hence the required adiabatic thermal conditions provided. A Cedip Silver infrared detector system was used with the setup as shown in Figure 3-3. The temperature change was captured by an infrared detector with a 320×256 pixel field of view at 383 Hz. The detector is calibrated annually therefore the error in the temperature data could be neglected compared to the system noise. This is to obtain the mean temperature, T_0 , and the change in temperature, ΔT which is then used to evaluate the thermo-elastic constant, k , which is calculated from the average of the change in temperature (ΔT) and mean temperature (T_0) values based on Equation 2-5.

To ensure that the heat loss to the surroundings was minimized and to reduce the effects of surrounding heat sources on the experimental setup, the setup was covered fully with black cloth. In addition, the temperature around the area was measured to ensure that the conditions around the experimental setup was kept as constant as possible. Furthermore, a certain degree of noise always existed in charge-couple device for the detector. This noise was reduced by cooling down the detector for at least half an hour before any readings were taken.

Software Altair was used to process the data. To ensure that the setup was reliable and calibrated, 070M20 steel with known thermo-elastic constant ($3.04 \times 10^{-12} \text{ Pa}^{-1}$) from its material properties was initially used to determine its k . This TSA-obtained k was within 1.6% difference. With this confidence, for this EN 10225 S460G2 high strength offshore steel, the thermo-elastic constant was determined to be $k = 3.31 \pm 0.06 \times 10^{-12} \text{ Pa}^{-1}$.

Figure 3-3 Thermoelastic Stress Analysis: Experimental Setup

3.1.2 Experimental procedure

Firstly, each specimen was weighed with the mass noted. The experimental set-up for the specimens, in terms of preparation, corrosion time and cracking, was recorded in Table 3-3.

Next, specimens were immersed in 3L solution of 3.5wt% Sodium Chloride (NaCl) in distilled water. Only the middle segment of the specimen (50mm by 30mm) as seen in Figure 3-1 was exposed to the solution while the other parts were protected by a layer of lacquer paint. After the specified period, specimen was dried and weighed. Appendix A explores the corrosion setup carried out in more details before the full-field test techniques were utilised. The specimen was then sprayed with a thin layer of black matt paint to standardize the emissivity of the steel surface (high emissivity). A TSA procedure was then carried out to observe the full-field stress

concentrations of the corroded specimen (around the crack for the cracked specimens with stress concentration of corroded specimen).

Table 3-3 Specimen Experimental setup

Specimen	Corrosion Time (Weeks)	Crack length, a (mm)	Accelerated Corrosion (2 hours)
1	0	4.83	No
2	0	5.68	No
3	2	0	Yes
4	2	0	No
5	4	0	No
6	4	0	No
7	4	4.41	Yes
8	4	6.21	Yes

A static load 10 kN was applied on the sample. The cyclic load of 5 ± 1 kN at 10 Hz was then applied and TSA was carried out. Next, the specimen was acetone washed to remove the black matt paint and repainted with black matt paint again. The TSA steps were carried out again. Subsequently, the specimen was then cleaned based on ASTM G1 – 03 [114]. Weight reduction in the specimen was noted. ASTM G1-03 suggests chemical cleaning for removal of corrosion products. This was achieved by mixing 500mL Hydrochloric Acid (HCl) with 3.5g Hexamethylene Tetramine with reagent water to make 1000mL. The specimen was soaked in this solution for

about 10 minutes. The TSA steps were repeated with these chemically-cleaned specimens. Examples of the corroded specimens observed are given in Figure 3-4.

Figure 3-4 Corroded Specimen: a) After corrosion without wash; b) after physical wash; c) after chemical wash; d) black-matt paint

Finally, Specimen 3, 4, 7 and 8 were further corroded for an hour using an accelerated corrosion method. A potentiostat was used for the specimens immersed in solution of 3.5wt% NaCl in distilled water. This enabled more depth to be corroded to examine the accuracy of the TSA method mentioned.

3.1.3 Procedure for evaluating the stress intensity factor (K)

Figure 3-5 Crack tip reference axis (a is the crack length, r is the distance away from the crack tip, θ is the angle with reference from r -axis)

Since TSA produces full-field T_0 , ΔT and stresses, the calculation of the SIF from the experimental data is feasible. Theoretical formulations based on linear elastic fracture mechanics, form of Westergaard's equations [115] for elastic direct stresses around a crack-tip, are shown below:

$$\sigma_x = \frac{K_I}{\sqrt{2\pi r}} \cos\left(\frac{\theta}{2}\right) \left[1 - \sin\left(\frac{\theta}{2}\right) \sin\left(\frac{3\theta}{2}\right)\right] - \frac{K_{II}}{\sqrt{2\pi r}} \sin\left(\frac{\theta}{2}\right) \left[2 + \cos\left(\frac{\theta}{2}\right) \cos\left(\frac{3\theta}{2}\right)\right] \quad (3-1)$$

$$\sigma_y = \frac{K_I}{\sqrt{2\pi r}} \cos\left(\frac{\theta}{2}\right) \left[1 + \sin\left(\frac{\theta}{2}\right) \sin\left(\frac{3\theta}{2}\right)\right] + \frac{K_{II}}{\sqrt{2\pi r}} \sin\left(\frac{\theta}{2}\right) \cos\left(\frac{\theta}{2}\right) \cos\left(\frac{3\theta}{2}\right) \quad (3-2)$$

These equations are only meant for the elastic loading conditions. As our current approach addresses Mode-I loaded cracks (evaluating Mode-I SIF, K_I), $K_{II} = 0$. Summing up Equations 3-1 and 3-2 together and substituting it into Equation 2-5,

$$\frac{\Delta T}{T_0} = K_I \left(-k \cos\left(\frac{\theta}{2}\right) \sqrt{\frac{2}{\pi r}} \right) \quad (3-3)$$

where angles, θ , takes reference from Figure 3-5. The substitution workings are shown in Appendix B-1.

To process and analyse the data, the following steps were taken:

- TSA data points of ΔT and T_0 field around the crack were captured and exported into an excel spreadsheet
- the crack tip was identified by locating the maximum $\frac{\Delta T}{T_0}$
- the crack length, a , was estimated by measuring the number of pixel from the crack-tip to the crack-end and re-scaling it based on the pixels and actual specimen length
- $\frac{\Delta T}{T_0}$ data points away from the crack tip (parallel to x -axis) were plotted against

$$\left(-k \cos\left(\frac{\theta}{2}\right) \sqrt{\frac{2}{\pi r}} \right).$$

- As mentioned earlier by Diaz et al [98], the data plot would not be linear as expected by the equation. As such, only points from the 2nd region mentioned would provide the gradient of the linear fit (least square fitting) for the plot points to give an estimate of K . For initial estimates, the beginning of the 2nd region can be estimated by the plastic zone size, r_p , either based on linear elastic fracture mechanics given by

$$r_p = 2.5 \left(\frac{K_I}{\sigma_Y} \right)^2 \quad (3-4)$$

- where σ_Y is the yield stress, or on Dugdale-Barenblatt cohesive zone model [47] for elastic-plastic cases given by

$$r_p = \frac{\pi^2}{8} \left(\frac{\sigma}{\sigma_Y} \right)^2 a \quad (3-5)$$

where σ is the applied stress and a is the crack length. Based on linear elastic fracture mechanics, J-integral can then be calculated for the elastic region only using

$$J = \frac{K_I^2}{E} . \quad (3-6)$$

In addition, with Equation 2-5, the stresses, $(\sigma_x + \sigma_y)$, were also calculated along the line away from the crack tip. This is then used to compare with the stresses from the estimate K . Various angles, θ ($\theta = -90^\circ, -45^\circ, 0^\circ, 45^\circ, 90^\circ$), were taken to calculate for the SIF, K .

3.1.4 Handbook calculations

As a basis of comparison for these corroded materials with crack, handbook calculation [116] for cracks under Mode I loading for elastic loading conditions is used:

$$K_I = \frac{P}{t_s \sqrt{W}} \left(\frac{\sqrt{2 \tan \frac{\pi a}{2w_s}}}{\cos \frac{\pi a}{2w_s}} \left[0.752 + 2.02 \left(\frac{a}{w_s} \right) \right] + 0.37 \left(1 - \sin \frac{\pi a}{2w_s} \right)^3 \right) \quad (3-7)$$

where P is the load, t_s is the thickness of the specimen, w_s is the width, a is the crack length and E is the young's modulus.

3.2 Results & Discussion

A total of eight specimens were corroded with four specimens being pre-cracked. Table 3-4 shows the various thickness and crack sizes of the specimens. As mentioned earlier, although the thickness reduction in the corroded specimens was less than 2.5%, pitting during corrosion such as seen in Figure 3-6 could lead to cracks that causes complex defects interactions.

Table 3-4 Specimen thickness and crack sizes

Specimen	Original Thickness (mm)	Thickness (after corrosion) (mm)	Thickness (after 1 hour accelerated corrosion) (mm)	Crack length, (mm)
1	4.05	-	-	4.84
2	4.08	-	-	5.68
3	3.95	3.93	3.87	0
4	3.96	3.94	3.91	0
5	4	3.98	-	0
6	3.95	3.93	-	0
7	3.94	3.93	3.92	4.41
8	3.93	3.92	3.90	6.21

Figure 3-6 5x Magnification of the corroded Specimen 3 showing corrosion pits

3.2.1 Evaluation of corroded specimen stresses

Firstly, Specimens 3 and 4 were observed under various conditions (initial corroded surface, acetone washed corroded surface and chemically washed corroded surface) for the precision of the data obtained. This was then compared to the uncorroded back surface of the specimens. The data obtained for Specimens 3 and 4 are plotted in Figure 3-7 and 3-8 respectively. For the $\frac{\Delta T}{T_0}$ of the corroded back surface, both shows similar pattern with a slight increase in $\frac{\Delta T}{T_0}$ for acetone washed specimen. However, $\frac{\Delta T}{T_0}$ is within the same magnitude for both initial and chemical washed specimen (0.00029 for Specimen 3 and 0.00028 for Specimen 4). As such, it is possible to just utilise the back surface initially without any washing to obtain the same $\frac{\Delta T}{T_0}$ analysis.

Figure 3-7 Specimen 3 under TSA (Front and Back of specimen) **Figure 3-8 Specimen 4 under TSA (Front and Back of specimen)**

On the other hand, the value for $\frac{\Delta T}{T_0}$ of the corroded front surface is similar to the value for the $\frac{\Delta T}{T_0}$ of the uncorroded back surface as the corroded surfaces were effectively washed. This is because the corroded product deposits on the surface (seen in Figure 3-4 (a) and (b)) have a different thermoelastic constant, k . Thus, only chemically washed corroded front surfaces were taken into consideration as the $\frac{\Delta T}{T_0}$ obtained is more precise when compared with the $\frac{\Delta T}{T_0}$ of the uncorroded surface.

Next, for corroded Specimens 3 to 6, the experimental cyclic stresses ($\Delta(\sigma_x + \sigma_y)$) were calculated using Equation 2-5, from the $\frac{\Delta T}{T_0}$ information. Theoretically, only $\Delta\sigma_y$ were considered and calculated as the specimen was cyclically loaded in the y -direction. The theoretical stresses were calculated from dividing the applied cyclic load over the cross-sectional area of the specimen (width multiply by thickness). From Figure 3-9, it can be generally commented that the stresses obtained through TSA are higher than the theoretical stresses of the specimen. This difference in stresses is larger for Specimen 5 ($t_s = 3.98$ mm) and 6 ($t_s = 3.93$ mm) with four weeks of exposed corrosion time compared to Specimen 3 ($t_s = 3.93$ mm) and 4 ($t_s = 3.94$ mm). The difference was highest for Specimen 3, with accelerated corrosion ($t_s = 3.87$ mm). The degree of differences between the TSA stresses and theoretical stresses varies, which causes difficulty in having confidence in estimating the extent of corrosion from the TSA data.

On another hand with the multiple runs conducted for the same corroded specimens, the standard deviation of the stresses obtained was on average 0.238% of the stresses (obtained from TSA).

This measurement error can be considered small and we can be confident of the stress values obtained from TSA in this chapter to be precise and consistent for corroded specimens.

Figure 3-9 Thickness vs. Stress for Specimen 3, 4, 5 and 6 (various thicknesses)

Statistical methods with multiple runs on various corroded specimens could provide some form of empirical correlation of $\frac{\Delta T}{T_0}$ with the extend of corrosion. Therefore, TSA might not be that advantageous to only be used to obtain the stresses on the corroded surface in estimating the corrosion depth. As such, the next section will consider TSA as a technique to observe crack-in-corrosion defects.

3.2.2 Calculation of crack stress intensity factor using TSA

From TSA, the experimentally derived $\frac{\Delta T}{T_0}$ can be mapped and contour-plotted in Figure 3-10. It was expected that the stresses would be highest at the crack tip, so enabling the estimation of the crack tip location. The crack length was then estimated by measuring from the location of the crack tip to the edge of the specimen. The estimation of crack length is crucial for accuracy of the estimation of stress intensity value. It is visibly easier to use TSA as a method to detect the crack tip compared to visually checking for the crack tip and measuring the crack length. Then, by using Equation 2-5, Figure 3-11 was obtained to show the stress map around the crack tip.

Figure 3-10 TSA data field from Specimen 1 (within the width of specimen)

Figure 3-11 Stress field from Specimen 1 (within the width of specimen)

With the crack tip known, stresses can be calculated in the r direction away from the crack tip.

Based on Equation 3-3, $\frac{\Delta T}{T_0}$ it can be plotted against $(K \cos(\frac{\theta}{2}) \sqrt{\frac{2}{\pi r}})$ in Figure 3-12. It was observed that there are three main regions:

Region A: There is no linear behaviour observed since there is a loss of adiabatic conditions due to high stress gradients and crack tip plasticity

Region B: A clear linear behaviour is observed. Due to this linearity, this region can be used to estimate the SIF, K .

Region C: A deviation from the linear behaviour observed in region B, which indicates inappropriate use of this mathematical model in this region.

Figure 3-12 TSA data plot from Specimen 1 ($a = 4.84\text{mm}$)

Consequently, SIF, K , can be estimated from region B. The stress distribution along the r direction can be observed in Figure 3-13 compared to the theoretical stresses distribution based on linear elastic fracture mechanics. The regions mentioned also corresponds to the stress distribution along r .

Figure 3-13 Stresses along r away from the crack tip for Specimen 1 ($a = 4.84\text{mm}$)

To further check the validity of the TSA-derived SIFs, TSA measurements were conducted on pre-cracked specimens without corrosion (Specimen 1 and 2). Specimen 1 had a crack of 4.84mm while Specimen 2 had a crack of 5.68mm. Various angles, θ , (0° , 45° , 90° , -45° , -90°) were taken to calculate for the SIF, K . These SIFs were then compared with handbook solutions based on Equation 3-7 in Figure 3-14 and 3-15.

Figure 3-14 Stress intensity factor, K , calculated from TSA against the various angles for Specimen 1 ($a = 4.84\text{mm}$)

Figure 3-15 Stress intensity factor, K , calculated from TSA against the various angles for Specimen 2 ($a = 5.68\text{mm}$)

From Figures 3-14 and 3-15, the experimental SIFs were seen to be within 10% from the handbook solution values. As the cyclic load increased, the difference between the TSA and handbook SIFs increased with the Specimen 2 at 20kN cyclic load having a lower experimental SIF. Since there were variations in the SIFs calculated for various θ , with the highest observed at -45° and lowest at -90° , the TSA technique enables calculation along the r direction at multiple θ which could provide an average estimate of the SIF, K .

The average was calculated and tabulated in Table 3-5 for comparison. The average values for these 2 specimens were lower than the handbook solutions; it seemed that for the shorter crack this difference was lower than the longer crack cases. TSA data thus can provide a good basis for experimental estimation of an accurate K .

Table 3-5 Comparing Average Experimental stress intensity factor with handbook solutions

Specimen	Average Experimental stress intensity factor (Pa.m ^{0.5})			Difference compared to handbook solutions (%)		
	10kN	15kN	20kN	10kN	15kN	20kN
3	13349800	19905800	26217400	0.621	1.21	2.42
4	1.42E+07	2.10E+07	2.77E+07	4.65	5.75	6.79

3.2.3 Comments on the region of interest

Based on Equation 4-3, $\frac{\Delta T}{T_0}$ was plotted against $(K \cos(\frac{\theta}{2}) \sqrt{\frac{2}{\pi r}})$ for Specimen 2 in Figure 3-16.

Then, the stress distribution along r for $\theta = 0^\circ$ was plotted in Figure 3-17. Here, Region A spanned up to 4mm which was larger than the plastic zone size, r_p calculated for such crack sizes. It was observed that the experimental data started to have limitations in Region A where the stresses were not as high as expected based on the linear elastic fracture mechanics (due to crack tip plasticity).

This observation was similar for Specimen 1 seen in Figures 4-12 and 4-13.

Figure 3-16 TSA data plot from Specimen 2 ($a = 5.68\text{mm}$)

Figure 3-17 Stresses along r away from the crack tip for Specimen 2 ($a = 5.68\text{mm}$)

Apart from the possible non-adiabatic conditions that caused the shortfall in stresses, it could also be due to the assumption of crack tip singularity which handbook solutions based on linear fracture mechanics were not able to capture this crack plasticity. It is crucial that Region B is identified to obtain an accurate estimation of the SIF of the crack. As such, for this study, Region B was taken as between 0.2 to 0.4 for all cases with consistency of results as seen in Figure 3-16. This corresponded to 4 to 18mm in Figure 3-17.

On the other hand, Region C was considered far from the crack field for any useful information of stresses to be used where it starts deviating from the expected stresses from 18mm onwards. For

future studies, it might be of interest to explore Region B in relation to the crack length with more crack lengths experimented on.

3.2.4 Assessment of crack-in-corrosion

After the study for cracks without corrosion was completed, the case of crack-in-corrosion was explored. Specimen 7 and 8 with pre-cracked length of 4.41mm and 6.21mm respectively, were corroded and the SIF obtained were plotted against the angle θ for various mean loads of 10, 15, 20kN.

Figure 3-18 Stress intensity factor, K , calculated from TSA against the various angles for Specimen 7 a) Non-corroded and b) Corroded (View: Front and Back)

From Figure 3-18 (b), for Specimen 7, the experimental SIFs were higher for corroded specimen as compared to its handbook solutions K . This can then be made in comparison to Figure 4-18 (a)

where the experimental SIF values for uncorroded specimen was close to the handbook solutions K .

However, for Specimen 8, in Figure 3-19 (a) and 3-19 (b), it was found that both the un-corroded and corroded SIF values were below the handbook solutions K . Nonetheless, the front and back faced experimentally calculated SIFs were similar for both specimens in Figure 3-17 (b) and 3-18(b). This result suggested that with the through-thickness crack-in-corrosion defect, measurement of the crack intensity factor, K , using TSA can be done on the uncorroded side of the specimen. The procedure reduces the need to chemically clean the corroded surface.

Figure 3-19 Stress intensity factor, K , calculated from TSA against the various angles for Specimen 8 a) Non-corroded and, b) Corroded (View: Front and Back)

Accelerated corrosion was done for both specimens. The measured SIFs were plotted in Figure 3-20. It can be observed that overall, the SIF increased with Specimen 7 having higher values than

expected, while the SIF for Specimen 8 was close to the expected values. In addition, for the shorter crack in Specimen 7, the experimentally calculated SIFs seemed to be higher than handbook solutions. While for the longer crack in Specimen 8, this trend seemed to be reversed. This phenomenon could be due to the longer crack having higher stress gradients and crack tip plasticity which caused loss of adiabatic conditions for accurate measurement of the stress-field in Specimen 8.

Figure 3-20 Stress intensity factor, K, calculated from TSA against the various angles for accelerated corroded Specimen a) 7 and b) 8

3.2.5 Corrosion-crack interactions & comparison between TSA calculations and handbook solutions for K

The accuracy and precision of TSA calculated stresses and SIFs in the lightly corroded sample were checked against handbook solutions under different loads (10, 15 and 20kN). For various specimen thicknesses, the SIFs, K , were plotted for the three loads in Figures 3-21 and 3-22 (for Specimen 7 and 8 respectively). It was found that for both specimens the experimentally calculated SIF increases as the corrosion depth increases. On the other hand, for handbook solutions, the reduction in thickness did not cause a substantial increase in SIFs, K . This provided an insight into the CIC interactions which could be substantial for light corrosion.

Figure 3-21 Thickness vs. stress intensity factor, K , for Specimen 7 (10kN, 15kN and 20kN)

Figure 3-22 Thickness vs. stress intensity factor, K , for Specimen 8 (10kN, 15kN and 20kN)

On average, the ratio for the standard deviation to the stress intensity factor obtained from TSA in both specimen was 1.49%. This provides good confidence in the precision of TSA as a measurement tool to provide consistent results which do not deviate from one measurement to another.

As shown by the crack tip region of Specimen 7 in Figure 3-23, pitting around the crack tip could potentially interact and increases the stresses around the crack tip area. Therefore, it is of future interest to explore in depth into this crack-corrosion interaction effect with more cases done for various corrosion and crack parameters in greater details.

Figure 3-23 5x Magnification of the corroded Specimen 7 showing pitting around crack tip

3.3 Concluding Remarks

The Thermoelastic Stress Analysis (TSA) method was applied for the first time to study corroded specimens containing cracks. The interactions between the cracks and the corroded surface were explored. A clear methodology was discussed on calculating the SIF from the TSA data. From the TSA experiments, it was observed and concluded that:

- TSA enables the observation of crack parameters directly without the necessity of knowing the exact thickness and material degradation properties for CIC defects.
- It is much more accurate to use TSA as a method to detect the crack tip compared to visually checking for the crack tip location and measuring the crack length.
- With the TSA data from a region of linearity around a crack tip, the stress intensity factor (SIF) of the crack is calculated accurately.

- In estimating small thickness reduction of uniform corrosion, the data obtained from TSA for the corroded surface might not be reliable.

The experimental testing conducted in this chapter had shown that there might be significant crack-corrosion interaction effect. In the following chapter, we use another full-field non-destructive technique, Digital Image Correlation (DIC), to further our investigations for cracks in corroded steels.

Chapter 4

4 Fracture analysis on corroded high strength offshore steel utilising Digital Image Correlation (DIC) technique

In this chapter, crack-in-corrosion defects were further studied through full-field strain measurements using Digital Image Correlation (DIC) technique. The DIC technique was validated by handbook solutions through certain simple cases. The measured strain and displacement fields were used to calculate the important fracture parameter of CIC, J-integral, without identifying the crack and corrosion parameters accurately. The results provided an estimation of the J-integral from DIC data of corroded Single-Edged Notched Tensile (SENT) specimen for high strength offshore steel. The obtained J-integrals were then compared with handbook solutions. The proposed measurement methodology showed excellent application of non-destructive examination (NDE) technique to quantify corrosion damage and fracture parameters for offshore high strength steel materials and structures.

4.1 Evaluating J-integral

The J-integral characterises the thermodynamic driving force for crack propagation. It is a critical parameter in the study of elastic-plastic fracture. J-integral is the energy release rate calculated by taking contours of the strain field around the crack tip. The J-integral [19] is characterised by:

$$J = \int_{\Gamma} \left(w \, dy - T_i \frac{\partial u_i}{\partial x} ds \right) \quad (4-1)$$

where w is the strain energy, dy is in the y direction, T_i is the dot product of traction, ds is along the contour path. Therefore, DIC has the potential to measure J-integral through the computation of the displacement fields [117]. Digital image correlation analysis provides useful tool for the measurement of the displacement fields associated with cracks, which can be mapped in two or three-dimensions.

The J-integral can be used to calculate the strain energy release rate directly from the displacement field around a crack, using knowledge of the stress-strain properties of the material. In principle, fracture mechanics parameters such as the J-integral can be extracted, given a sufficiently accurate, high resolution mapping of the displacements. Its formulation is defined as a contour integral, which has a zero value if no crack is present in the contour. Often implemented as a line integral, the J-integral can be rewritten as a surface or area integral using Green's theorem, and this formulation is convenient in implement in Finite Element analysis [108]. One such direct evaluation of the J-integral from a measured crack displacement field is the JMAN method [112] which takes its input from full-field displacements of an image correlation analysis. Given that there exists a nonlinear zone at the crack tip and significant DIC projection errors occur near the crack, the measured and assumed stress fields are ideally considered away from the crack tip. However, the effects of image correlation parameters are complex; significant underestimation of the J-integral can occur if the displacements close to the crack tip are not sufficiently measured [112].

4.2 Methodology

4.2.1 Experimental procedure

Four specimens (with pre-cracks) manufactured from high strength structural steel EN 10225 S460G2, were artificially corroded, with the duration of exposure being varied to produce different degrees of corrosion damage. Figure 4-1 shows the coupon size and dimensions used. Fatigue pre-cracks were used in this case it was mentioned that SENT specimens with fatigue pre-cracks produced similar resistance curves [118]. The four specimens machined dimensions and mass were noted in Table 4-1.

Figure 4-1 Dimensions of the pre-crack corroded Specimen

Table 4-1 Dimensions and mass of Specimens

Specimen	Original Mass (g)	Density (g/mm³)	Length, l_s (mm)	Width, w_s (mm)	Thickness, t_s (mm)
1	124.4	0.00770	131.1	30.4	4.05
2	124.2	0.00770	130.8	30.2	4.08
3	120.5	0.00775	130.3	30.3	3.94
4	120.2	0.00776	130.1	30.3	3.93

The isotropic power-law hardening model was adopted to characterize the uniaxial true stress-strain relationship of EN10225 offshore steel grade, written as

$$\sigma = \begin{cases} E\varepsilon & \varepsilon \leq \varepsilon_Y \\ \sigma_Y \left(\frac{\varepsilon}{\varepsilon_Y} \right)^n & \varepsilon > \varepsilon_Y \end{cases} \quad (4-2)$$

where σ_Y is the yield stress, $\varepsilon_Y = \sigma_Y/E$ is the corresponding yield strain, E is the Young's modulus and n is the hardening exponent. In the analyses, the Young's modulus, E , was taken as 211 GPa while the Poisson's ratio ν was assumed to be 0.3. The true stress-strain response for the pipeline material is shown in Figure 4-2. This is done using strain gauges and guidelines of ASTM E8/E8M [119].

Figure 4-2 Stress vs. Strain graph of EN10225 offshore steel

In Figure 4-2, the symbols represent the experimental data, and the solid line denotes the Least Square Fitting of the data. In addition, yield stress (σ_Y) of 585MPa and hardening exponent n of 0.0315 were obtained for isotropic hardening behavior based on the uniaxial tension tests. It should be noted that in the present work, the ratio of the ultimate tensile strength to yield stress (σ_{uts}/σ_Y) was estimated at 1.07 based on the experimental data. The experimental set-up for the specimens, in terms of corrosion time, was recorded in Table 4-2.

Table 4-2 Specimen Corrosion Exposure Time

Specimen	Corrosion Time (Weeks)	Accelerated Corrosion (2 hours)
1	0	No
2	0	No
3	4	Yes
4	4	Yes

Next, specimens were immersed in 3L solution of 3.5wt% Sodium Chloride (NaCl) in distilled water. Only the middle segment of the specimen (50mm by 30mm) as seen in Figure 4-1 was exposed to the solution while the other parts were protected by a layer of lacquer paint. After the specified period, the specimen was dried and weighed. Next, the specimen was cleaned based on ASTM G1 – 03 [114]. This was achieved by mixing 500mL Hydrochloric Acid (HCl) with 3.5g Hexamethylene Tetramine with reagent water to make 1000mL. The specimen was soaked in this solution for about 10 minutes. Weight of the specimen was noted.

The specimens were coated by white paint with black speckles (using a 0.2mm diameter nozzle to give speckles of 0.1 – 0.4mm). The specimen speckles and DIC setup is shown in Figure 4-3 (a) and 4-3 (b) respectively.

Figure 4-3 a) SENT Specimen coated white and black speckles b) DIC Experimental Setup

The specimens were loaded up to 20kN with an interval of 5kN (Mode I). Schematic and setup are shown in Figure 4-4. A 5Mp 12-bit monochromatic LaVision E-lite camera was used to capture the experimental images. A sigma 105mm lens was used to capture the images analysed. A LED ring flash light was used to provide consistent lighting for the data collection. The grey level distribution of the images was maximised over the 12-bit dynamic range of the camera, whilst avoiding saturation of the image. This ensured that any measurement errors would be due to the physical changes to the pattern as opposed to a change in the reduction of the dynamic range of the image, which is known to have an influence on the accuracy of the DIC measurements [106].

The commercial software, LaVision DaVis 7.4 correlation software, was utilised to implement a cross correlation approach by inverse Fourier transforms.

Figure 4-4 Digital Image Correlation: Schematic and photo of experimental setup

Lastly, Specimens 3 and 4 were exposed to 2 hours of accelerated corrosion. The specimens were corroded in solution of 3.5wt% NaCl in distilled water using a potentiostat. This enabled the pits to be formed for examining its effect on crack-in-corrosion defects. Post-test measurements of specimen thickness and deepest crack depth provide a quantitative assessment of the degree of corrosion damage as shown in Table 4-3.

Table 4-3 Specimen Thickness and Crack Size

Specimen	Original Thickness, t (mm)	Thickness, t (after corrosion) (mm)	Thickness, t (after 2 hour accelerated corrosion) (mm)	Crack size, a (mm)
1	4.05	-	-	4.84
2	4.08	-	-	5.68
3	3.94	3.93	3.92	4.41
4	3.93	3.92	3.90	6.21

4.2.2 Calibration

Before starting the experimental testing for each specimen, DIC calibration was conducted to ensure that tests would be within reasonable accuracy. This was done using strain gauges. As DIC processing is sensitive to its subset and step sizes, an initial check on the various step and subset size was conducted [106]. The results of each iteration of subset and step sizes are compared to strain gauge data in Figure 4-2. Here, subset 21x21 and 41x41 with step size of 15 gives the least amount of percentage difference when compared to strain gauge data. As such, future data will be analysed using the subset size of 41x41 and step size of 15.

Following the measurement, the J-integral is calculated from the DIC data. There has been research done using J-integral and DIC such as JMAN [112] and others [120, 121]. However, these are not

available open-source code applications that can calculate J-integral from the obtained data sets apart from FEM data. The following sections show how the J-integral is calculated.

4.3 Extracting J-integral derived from displacement/strain fields

4.3.1 Application of Line Contour Integrals on full-field displacement and strain

Before going into line contour integrals calculation from full-field displacement and strain, Appendix B-2 demonstrates the analytical calculation of the line contour integral using a simple cantilever example. For simple tension cases without complex loading, the line contour integrals on full-field displacement and strain can be calculated. This method has been used by Y. Guo et. al [122] and B. Hakimelahi et. al [123]. Here, the calculation of fracture parameters, J-integral, from DIC data with reference to Figure 4-5 is shown.

Figure 4-5 Line contour integral

From Equation 4-1, the J-integral can be discretised and numerically integrated using midpoint rule for the various contour segments Γ_m [122]:

$$J_m = \sum_{i=1}^{n_j-1} (F_m^{(i)} + F_m^{(i+1)}) \frac{\Delta_i}{2} \quad (m = 1, 2, 3, 4) \quad (4-3)$$

where n_j is the number of nodes along Γ_m , the various paths taken along the contour, Δ_i is the length of the segment I, and the integrand at each node is:

$$F_m = (W)n_m - T_i \frac{\partial u_i}{\partial x} = \frac{E}{2} (\varepsilon_{xx}^2 + \varepsilon_{yy}^2 + \frac{\varepsilon_{xy}^2}{1+\nu}) n_m - E \left(\varepsilon_{xx} n_x + \frac{\varepsilon_{xy} n_y}{2(1+\nu)} \right) (\varepsilon_{xx}) - E \left(\varepsilon_{yy} n_y + \frac{\varepsilon_{xy} n_x}{2(1+\nu)} \right) \left(\frac{\partial u_y}{\partial x} \right) \quad (4-4)$$

Numerically,

$$\left(\frac{\partial u_y}{\partial x} \right) = \frac{u_{y_{i+1}} - u_{y_{i-1}}}{2\Delta x} \quad (4-5)$$

where for contour segments Γ_m , is given by

m	n_m	n_y	n_x
1	-1	0	-1
2	0	1	0
3	1	0	1
4	0	-1	0

Therefore, the data required from the DIC experiment captured will be the various strains and displacements for ε_{xx} , ε_{yy} , ε_{xy} and u_{y_i} .

4.3.2 Application of Area Contour Integrals on full-field displacement and strain

Still, the direct evaluation of J-integral is cumbersome using DIC data due to the geometric difficulties encountered in defining a contour that passes through the integration points. Such a contour needs the most accurate stress and strain quantities at the integration points. As such, domain integral can be used instead, as shown in the following Figure 4-6.

Figure 4-6 The Area Integral

With reference to Figure 4-6, the area integral Γ^* is calculated as the following and also from Equation 4-1 [4, 124]:

$$\Gamma^* = \Gamma_1 + \Gamma_+ + \Gamma_- - \Gamma_0 \quad (4-6)$$

$$J = \int_{\Gamma^*} \left[\sigma_{ij} \frac{\partial u_j}{\partial x_1} - w \delta_{1i} \right] q m_i d\Gamma - \int_{\Gamma_+ + \Gamma_-} \sigma_{2j} \frac{\partial u_j}{\partial x_1} q d\Gamma \quad (4-7)$$

where Γ_+ and Γ_- = upper and lower crack faces, respectively, m_i = outward normal on Γ^* and q is an arbitrary but smooth function [125] that is equal to unity on Γ_0 and zero on Γ_1 as seen in Figure 4-7. This q function is the virtual crack extension function whose value is 1 inside the inner integration contour and 0 outside the outer integration contours and is differentiable at all its points [108].

Figure 4-7 The q function

Next, expanding Equation 4-7 and discretising only the first integral lead

$$J_m = \sum_{j=1}^{n_j-1} \sum_{i=1}^{n_i-1} (F_m^{(i)} + F_m^{(i+1)}) \frac{\Delta_i \Delta_j}{2} \quad (4-8)$$

$$F_m = E \left[\left(\varepsilon_{xx}^2 + \frac{\varepsilon_{xy}}{2(1+\nu)} \left(\frac{\partial u_y}{\partial x} \right) \right) - \frac{1}{2} \left(\varepsilon_{xx}^2 + \varepsilon_{yy}^2 + \frac{\varepsilon_{xy}^2}{1+\nu} \right) \right] \left(\frac{\partial q}{\partial x_1} \right) + E \left(\varepsilon_{yy} \left(\frac{\partial u_y}{\partial x} \right) + \frac{\varepsilon_{xy} \varepsilon_{xx}}{2(1+\nu)} \right) \left(\frac{\partial q}{\partial x_2} \right) \quad (4-9)$$

Equation 4-8 can then be calculated numerically by obtaining the various strains and displacement (ε_{xx} , ε_{yy} , ε_{xy} and u_{y_i}) from DIC.

With the calculation sorted out from Equation 5-8, the following simple algorithm was used to analyse the DIC data obtained:

1. Estimation of the crack tip based on the change in ε_{yy}
2. Using the known length of pixels, estimate the crack length
3. Area contour integrals calculated based on Equations 4-8 and 4-9 respectively: rectangular area contours with 325 pixels (x-direction) by 750 pixels (y-direction)
4. Compare area contour integrals with theoretical calculations.

Microsoft Excel Spreadsheet was used for the calculation of J-integral using Equations 4-8 and 4-9.

4.3.3 Handbook solutions

As a basis of comparison for these corroded materials with crack, handbook calculations [116] were made for cracks under Mode I loading based on Equation 3-7. Based on linear elastic fracture mechanics, J-integral can then be calculated for the elastic region only using Equation 3-6 with Equation 3-7:

$$J = \frac{\frac{P}{t_s \sqrt{W}} \left(\frac{\sqrt{2 \tan \frac{\pi a}{2w_s}}}{\cos \frac{\pi a}{2w_s}} \left[0.752 + 2.02 \left(\frac{a}{w_s} \right) \right] + 0.37 \left(1 - \sin \frac{\pi a}{2w_s} \right)^3 \right)^2}{E} \quad (4-10)$$

4.4 Results and Discussion

4.4.1 Displacement and strain fields

Firstly, the displacement and strain fields were visualised from the DIC experiments conducted.

The uncorroded Specimens 1 and 2 were loaded in tension to 10kN.

Figure 4-8 Displacement field of Specimen 1 under 10kN tensile load

Figure 4-8 shows the displacement field of Specimen 1 over its width. It was expected that the displacement will be a gradual gradient (regular displacement interval) with the smallest displacement at the top since the Instron testing machine was set up with the top of the specimen fixed and the bottom of the specimen being hydraulically pulled and loaded (as such a larger displacement at the bottom). However, there is a sharp gradient over the area where y is from 15mm to 30mm and x is from 0mm to 5mm. From this visualisation itself, it can be predicted that the crack was around that area and an estimation of where the crack was (estimated y at 23mm with a crack length of 5mm). It is noteworthy that the displacement intervals started to increase after around 5mm, and then be reduced after the crack tip. As noted, the crack length for Specimen 1 was 4.84mm which was relatively close to the rough estimation of 5mm just from observing Figure 4-8.

Similarly, for Specimen 2 visualised in Figure 4-9, the same trend in displacement intervals can be observed at the region of y from 20 – 30mm and x from 0 – 7mm.

Figure 4-9 Displacement field of Specimen 2 under 10kN tensile load

Here the x range was longer which indicated the crack was longer and could possibly be 7mm long. However, as noted that the crack length for Specimen 2 is at 5.68mm, an estimation of 7mm might be over-estimating the crack length. Notably, visualisation of displacement is useful but not accurate to decide and quantify the crack itself. Next, strain field visualisations were created by calculating strain values from the DIC data.

Theoretically, the strain after the crack tip should be homogenous to the rest of the specimen body. As such, by calculating the strain gradient in the x -direction, the crack tip was estimated by the

location point where the strain equal to the range of the average strain (0.000495 with std = 0.000402) of the rest of the body and the gradient increases to 0.

From Figure 4-10, with the strain field of Specimen 1, there was a high strain area within the region of y from 20 – 30mm and x from 0 – 5mm. As expected from a SENT specimen, the rest of the field had a relatively low strain with the crack being in the high strain region. The area of higher strain was further away from the crack tip (nearer to the crack mouth) as a higher displacement gradient was observed away from the crack tip in Figure 5-8. As such, based on this figure, the length of the crack can be estimated to be around 5mm which was similar to that from the displacement field. Likewise, in Figure 4-11, Specimen 2 had comparable trends observed in Specimen 1 just that it had a longer high strain region as it had a longer crack length. In this case, x had a range from 0- 6.7mm which provides evidence that the crack length may be 6.7mm long. These crack lengths were still an over-estimation but it can be further improved by analysing the strain gradient more in-depth. Through checking the various lengths of cracks, a good correlation between the strain gradient and the crack tip can be found.

Figure 4-10 Strain field of Specimen 1 under 10kN tensile load

Figure 4-11 Strain field of Specimen 2 under 10kN tensile load

4.4.2 The J-integral and contour Areas

Next, the J-integral was calculated using contour areas as mentioned in section 4.3.2. The strain field for Specimen 1 was used as an example shown in Figure 4-12, where the contour area was made up of 325 pixels (x -direction) by 750 pixels (y -direction). This area was sufficiently large and the boundaries were away from the crack tip region.

Figure 4-12 Contour areas

Various area contours were taken with a decreasing contour area by moving the width towards the crack in the positive x -direction. A total of 10 – 12 contours were taken, and the J-integral was calculated using Equation 4-8.

The various area contour J-integrals were calculated for Specimen 1 with varying loads of 10kN, 15kN and 20kN, respectively. For each of the loads as seen in Figure 4-13, at least 3 runs were conducted and calculated. For a given load (for example 15kN), the 3 runs gave similar calculated J-integral values for each contour area. The result provided good confidence in the stability of DIC

data and the calculation of J-integral in the Spreadsheet calculation. Predictably, a higher load causes a higher J-integral value.

Figure 4-13 J-integral against Countour Areas for Specimen 1

In general, the J-integral decreased as the contour width decreased towards the crack tip and beyond, plateauing after a certain number of contours. In this case, after contour area 6, most of the J-integral values tend to plateau, for example for 10kN, to the value of 200 mPa. This was due to the contours after contour area 6 being beyond the crack tip and did not have any crack within its contour area.

Equally, Figure 4-14 shows the Specimen 2 with a longer crack length having the same J-integral trends but having it plateau at contour area 8. This result equated to a longer crack length. At 10kN, the plateau value was at 300 mPa.

Figure 4-14 J-integral against Contour Areas for Specimen 2

For the both cases, it was expected that the J-integral values to be similar for contours encompassing the crack area and plateau to 0. However, this was not the case here since it was a 3-D SENT specimen having only 2-D surface values calculated. The path independent J-integral applies perfectly for 2-D ideal cases, but differs for the current 3-D practical case [126]. It was mentioned that the path independence is lost for dynamic problems because the transmission of energy to different paths depends on the time stress waves reaching the path [122]. As such, an alternative way of calculating the J-integral was proposed. Since the J-integral value decreases from the start of the crack mouth till the crack tip, an average J-integral value can be taken from these contour areas, ensuring that the plastic zone is included in the contour areas. For example, for Specimen 1, an average J-integral can be calculated from the J-integrals of contour 1 to 6. Since theoretically the J-integral should be the same value, physically it is assumed that to take an

average over the various area contours is logical. The obtained results were compared to handbook solutions to check if such a simple average would suffice to characterise the crack driving force.

4.4.3 Comparison of the average J-integral calculated with handbook solutions

4.4.3.1 Uncorroded

To ensure that there is confidence to use this average J-integral before using it to understand the CIC cases, Specimens 1 and 2 average J-integrals were compared to handbook solutions first when the two specimens did not have any corrosion and thus no complexities involved.

For Specimen 1 in Figure 4-15, the average J-integral calculated for loads of 10kN and 15kN were close to the handbook solutions. There was a slightly higher value for the averaged J-integral compared to handbook solutions for 20kN load (7.99% difference). In addition, even for a bigger crack length for Specimen 2 in Figure 4-16, the same pattern was observed for loads of 10kN and 15kN, and a higher average J-integral value for 20kN (14.3% difference). Indeed, this difference was higher for Specimen 2 most probably due to the longer crack length.

Figure 4-15 J-integral against Load for Specimen 1 **Figure 4-16 J-integral against Load for Specimen 2**

These comparisons provided ample indications that the calculation of averaged J-integral using DIC was consistent with the handbook solutions. Since the load used was up to 20kN only which converted to a 167MPa loading onto the SENT specimen, it was still within the elastic region of the material. However, this might not be the case at the crack tip which experiences a high stress concentration. The obtained results using a simple average compared to handbook solutions are seen to suffice to characterise the crack driving force for lower loads within the elastic region.

4.4.3.2 Corroded

Next, the crack-in-corrosion cases were considered. Specimens 3 and 4 were tensile-loaded and tested using DIC technique. Average J-integrals were calculated and compared with the handbook solutions. Here for handbook solutions, the SENT thickness, t , was taken to be the thickness of the corroded specimen. In this case, DIC experiments were done for both the front and back sides of the specimen as to compare the corroded and uncorroded side respectively. It was noted that the thickness reduction of the specimen might not be substantial (0.25% reduction). However, it was still crucial because upon close inspection of the corroded surface, pits can be seen forming in Figure 4-17 on the surface which was detrimental to the integrity of the SENT specimen.

Figure 4-17 5x Magnification of the corroded Specimen 3 showing pitting around the corroded area

From Figure 4-18 and 4-19, the front-side average J-integral values of Specimens 3 and 4 were mostly higher compared to the handbook solutions. For Specimen 3 in Figure 5-18, the front-side average J-integral values was roughly 20% higher than the handbook solutions with the highest difference of 31.6%. This trend was similarly observed for the longer crack Specimen 4 in Figure 4-19 with 10.8 – 52.4% difference. While the back-side J-integral values of Specimen 3 were quite close to the handbook solutions, with 3.9 – 6.5% difference. However, for Specimen 4, the back-side average J-integral values were closer to the front-side average J-integral values, with about 5% difference.

Figure 4-18 J-integral against Load for Specimen 3

Figure 4-19 J-integral against Load for Specimen 5

At first, it was speculated that due to the thickness of the specimen, the uncorroded back-side of the specimen did not have the same displacement and strain of the corroded front. On the contrary, the back-side average J-integral values of Specimen 4 showed the opposite. Perhaps for longer cracks at higher loads, this displacement and strain were also experienced at the back of the corroded specimen. In general, it can be concluded that DIC measurements give a higher value especially when it was taken from the corroded side for higher loads (15 and 20kN). As such, a simple handbook solution that only takes into consideration the thickness reduction is not able to take into consideration the corrosive effects on the cracks and the material degradation.

Furthermore, accelerated corrosion was done on Specimens 3 and 4. Likewise, not much substantial general corrosion was measured from the corroded area (another 0.25% at most). Although so, this accelerated corrosion induced more pits in the corroded area and especially around the cracks as seen in Figure 4-20.

Figure 4-20 5x Magnification of the corroded (accelerated) Specimen 3 showing pitting around crack tip

Similar trends can be seen in Figures 4-21 and 4-22 when comparing to the ones before the accelerated corrosion. The back-side average J-integral of Specimen 3 seems to be closer to the handbook solution although it is slightly higher than before. For the front-side J-integral values, the difference is estimated at around 50-60% for Specimen 3. As for Specimen 4, at 10kN, the J-integral values from DIC are similar to the handbook solutions. At higher loads, the difference for Specimen 4 front-side J-integral values could be as high as 76.0% compared to the handbook solutions.

Figure 4-21 J-integral against Load for Specimen 3 (accelerated corrosion)

Figure 4-22 J-integral against Load for Specimen 4 (accelerated corrosion)

Generally, for loads of 15 and 20kN, the average J-integral calculated for both the back and front sides were higher than the handbook solutions. From Figures 4-18, 4-19, 4-21 and 4-22 at 10kN load, the average J-integral values were within the range of the handbook solutions. It can be seen here that there might be other factors that cause an increase in average J-integral values calculated from DIC, although the thickness reduction was not substantial and there was no increase in crack length.

4.4.4 Corrosion-crack interactions

The front-side J-integral values for Specimens 3 and 4 from DIC were compared before and after accelerated corrosion with the handbook solutions. From Table 4-4, for the both specimens, using DIC average J-integral calculation, there was a substantial increase in the J-integral values compared to the handbook-calculated ones.

Table 4-4 J-integral values for Specimens 7 and 8

		Handbook-calculated J-integral (m.Pa)			DIC-calculated J-integral (Front-side) (m.Pa)		
	Load (kN)	Corroded	Additional Accelerated Corroded	Difference (%)	Corroded	Additional Accelerated Corroded	Difference (%)
Specimen 3	10	705.58	709.60	0.570	892.39	1076.08	20.6
	15	1587.55	1596.60	0.570	2089.99	2543.42	21.7
	20	2822.30	2838.39	0.570	3437.11	4523.53	31.6
Specimen 4	10	1365.00	1365.00	0	1217.33	1416.55	16.4
	15	3071.25	3071.25	0	3705.44	4706.55	27.0
	20	5460.00	5460.00	0	8323.09	9610.39	15.5

For Specimen 4, the crack did not exhibit any visible change in crack length or corrosion depth, but from DIC observations there was a change detected in the average J-integral calculated with at least 15.5% difference. This was because handbook calculated values only rely on the corrosion depth parameter and crack length, while DIC calculated values were based on the measured displacements and strains. As such, the J-integral values based on the DIC measurements contained more real information and thus were more accurate. For example, the DIC measurements captured the influences of the additional pits induced during the accelerated corrosion. As such, apart from just the generalised corrosion interacting with the crack, the pits-crack interactions are more crucial

to be investigated and considered in such complex defects analysis. So far there are no available methods to calculate the pit effects on cracks [94], especially for shallow corrosion with pits interacting with cracks. As such, the DIC measurements provide a fast tool to calculate the J-integral while considering these pits. For instance, stress corrosion induced cracks can initiate below a corrosion deposit [63]. Such cracks may be detected and measured by DIC.

For crack-in-corrosion defects, the DIC methodology was employed as a non-destructive examination (NDE) technique to measure fracture parameters for offshore steel materials and structures by capturing full-field strains. However, the limitation currently lies in the technique itself requiring random pattern images such as the speckles. As such, this technique has a large potential to be applied for in-situ structural health monitoring and assessment for failure if this limitation of the need for random patterns can be overcome using other image reference points for displacement and deformations.

4.5 Concluding Remarks

In this study, experiments were conducted to investigate crack-in-corrosion defects on SENT specimens. The Digital Image Correlation (DIC) technique was used to capture full-field strain measurements of the corroded specimens. Calculation of the fracture parameter, J-integral, was demonstrated and Excel Spreadsheets were used for various area contour integrals calculation. The obtained J-integral values are averaged and compared with the handbook solutions. The results led to the following conclusions:

- 1) Visualisation of displacement is useful but not accurate in deciding and quantifying the crack parameters, but the full strain field provides an easier visualisation of the crack itself.
- 2) It was interestingly noted that the J-integral value decreases from the start of the crack mouth till the crack tip, instead of the well-known path independent J-integral value expected for 2-D crack case. This could be due to practical conditions of the SENT specimen; it is a practical dynamic 3-D problem.
- 3) For uncorroded specimens, the calculation of average J-integral using DIC was highly consistent with the handbook solutions.
- 4) For corroded specimens, the handbook solution can only consider the corrosion damage through the thickness reduction only, thus was not able to take into consideration the pits effects on the cracked area and the material degradation.
- 5) The current investigation has the advantage over standard industry methods that, for the J-integral calculations, the precise location of the crack tip is not required. It is also insensitive to inelastic strains close to the crack tip that are contained within the chosen contour.
- 6) Our study did not exert loads beyond the elastic region, so that the elastic-plastic capabilities of this technique have yet to be assessed. As such, this can be future works to be embarked on.

Chapter 5

5 Fracture Mechanics based Numerical approach for crack-in-corrosion damage assessment of high strength offshore steel using SENT

In corrosion, damage can be defined in terms of material loss, pit density, pit size, cracking, etc depending on the type of corrosion. In the oil and gas industry, Single-Edge Notched Tension (SENT) specimen has been increasingly used to determine fracture toughness and resistance curves for applications in strain-based design and fracture mechanics assessment of offshore pipelines containing cracks. As such, a nonlinear 3-D numerical model of crack-in-corrosion (CIC) defect was established using Single-Edge Notched Tension (SENT) specimen which was validated against the experimental and handbook results [79]. Based on the modelling results, an assessment protocol was proposed to estimate the acceptable fracture parameters of cracks on corroded structures. We aim at developing a new method for evaluating fracture parameters of CIC defects.

5.1 Methodology

To approximately evaluate a CIC scenario, the damage zone is modelled as a combination of the following two geometric changes, as illustrated in Figure 5-1:

- 1 Uniform damage: a general thickness reduction due to corrosion (represented as the volume loss $l \times d \times w$ as shown in Figure 5-1);
- 2 Local pitting-crack: the stress concentrations and cracks can be found at the sites with localised corrosion or pitting. The deepest/largest crack with a length of a is considered in this study, as shown in Figure 5-1. However, if no such cracks are found, the highest stress concentration location can be considered as an equivalent crack (represented by $2a$).

Figure 5-1 Geometric model of corrosion damage zone (l is the length, d is the depth of corrosion, w is the width of the corroded area, t is the thickness of the material and $2a$ is the crack length)

This approach considered the damage into a crack problem and principles of fracture mechanics are applied to study the crack fracture parameters. A Single-Edge Notched Tension (SENT) corroded plate with through-thickness crack was used for an initial CIC study. A nonlinear 3-D finite element analysis had been conducted based on corroded high strength offshore steel

(EN10225) SENT specimens (material properties mentioned in Chapter 3 and 4). Figure 5-2 shows the dimensions of the specimen ($130\text{mm} \times 30\text{mm} \times 4\text{mm}$).

Figure 5-2 Dimensions of corroded pre-crack specimen

The numerical models were validated using two non-destructive examination techniques, namely Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC). The two experimental tests provided the full-field measurements of stress and strain distributions on the corroded specimen surface as shown in Chapters 4 and 5. These two methods were chosen as it provides a non-intrusive method of investigating the stress intensity factor and J-integral. Conversely, traditional methods such as clip gauging (as mentioned in Section 2.3.3) and strain gauges were not considered as it would require additional clip-in gauges and strain gauges which must be placed on the specimen itself (at the crack opening or be bonded onto specimen).

As the main purpose of the current study was to explore the validity of the modelling method, the corrosion depth parameter is varied while the corroded area is kept constant. In addition, through-thickness cracks have been considered as the equivalent cracks as a basis for this study, instead of semi-elliptical or penny-shaped cracks due to manufacturing constraints.

5.1.1 Material Properties

As mentioned in Chapter 3 and 4, the isotropic power-law hardening model was adopted to characterize the nonlinear material properties of EN10225 offshore steel grade, written as

$$\sigma = \begin{cases} E\varepsilon & \varepsilon \leq \varepsilon_Y \\ \sigma_Y \left(\frac{\varepsilon}{\varepsilon_Y} \right)^n & \varepsilon > \varepsilon_Y \end{cases} \quad (5-1)$$

where σ_Y is the yield stress, $\varepsilon_Y = \sigma_Y/E$ is the corresponding yield strain, E is the Young's modulus and n is the hardening exponent. In the analyses, the Young's modulus, E , was taken as 211 GPa while the Poisson's ratio ν was assumed to be 0.3. The true stress-strain response for the pipeline material is shown in Figure 3-2. In addition, yield stress (σ_Y) of 585MPa and hardening exponent n of 0.0315 were obtained for isotropic hardening behavior based on the uniaxial tension tests. It should be noted that in the present work, the ratio of the ultimate tensile strength to yield stress (σ_{uts}/σ_Y) was estimated at 1.07 based on the experimental data.

5.1.2 Numerical Model

ABAQUS 6.14 is used to model the 3-D coupon specimen (in Figure 6-2) with corrosion and crack defects to predict the fracture parameters K and J -integral. The 3-D coupon model was created by removing a rectangular volume to represent the material lost due to general corrosion, represented in Figure 5-2. The 20-node reduced integration quadratic elements (C3D20R) were used with a structured mesh pattern [127] shown in Figure 5-3 for the crack tip region. A mesh convergence study was done and discussed in Appendix C.

Figure 5-3 Spider web mesh at crack tip region

The FE model typically contained 12 000 – 16 000 elements; the elements range was due to more detailed meshing along longer crack lengths. The schematic diagram for the boundary conditions equivalent to the experimental condition was shown in Figure 5-4.

Figure 5-4 Boundary and load conditions of the model (U1 is in the x-direction, U3 is in the z-direction and P is the pressure)

Corrosion damage was modelled by having the corrosion volume reduced from the coupon body based on the corrosion depth over the corroded area seen in Figure 5-4. The general thickness reduction model of uniform corrosion was numerically run with the maximum stress and average stress on the coupon compared for varying corrosion depth, d (0.1 - 2mm). Then, CIC cases were analysed with varying crack lengths, a (4, 5 and 6mm). A total of 30 3-D model configurations were established.

Here for FE calculation of J-integral, J-integral is characterised as the energy release rate related to crack growth/advance which is then related to Stress Intensity Factor, K for linear material response. This energy release rate is given by

$$J = \int_A \lambda(s) \mathbf{n} \cdot (W \mathbf{I} - \sigma \cdot \frac{\partial u}{\partial x}) \cdot q dA$$

Where $\lambda(s)$ is the virtual crack advance defined in the numerical setup, dA is the surface element along a vanishing small tubular surface enclosing the crack line, $\mathbf{n}$ is the outward normal to dA , and q is the local direction of the virtual crack extension and W is the elastic strain energy density plus the plastic dissipation. For this study, 30 contours around the crack tip were taken as seen in Figure 5-5 to ensure the convergence of the value for J -integral and Stress Intensity Factor, K .

Figure 5-5 Von Mises Stresses distribution for uncorroded numerical model $a = 6\text{mm}$ showing the contours (1 to 8) taken from the crack tip

This convergence value was obtained by comparing the numerically obtained fracture parameters (mode I stress intensity factor, K_I , and J-integral) against the handbook solutions given in Equations 3-6 and 4-10. Equation 4-10 assumes that the fracture analysis is linear elastic. The Von Mises stress around the crack tip, with contours taken around the crack tip along the nodes for the calculation of K and J-integral, can be seen in Figure 5-5.

5.1.3 Model Validation using Experimental Corrosion Test Results

Two fatigue pre-cracked coupon specimens with through-thickness cracks ($a = 4$ to 6mm) were immersed in a test solution (3.5wt% Sodium Chloride (NaCl) in distilled water, pH = 6.17) for a period of 4 weeks at room temperature. The gauge area was exposed to the solution while the rest of the specimen was painted with a silicone-based lacquer to avoid corrosion. During the test, distilled water was added to the solution on a regular basis to maintain the NaCl concentration. At the end of the corrosion-exposure, specimens were recovered, followed by chemical surface cleaning in a 1000 mL pickling solution (500 mL 37% HCl, 3.5 g hexamethylenetetramine ($(CH_2)_6N_4$ and reagent water) according to the standard practice ASTM(G1-03) [114]. A thickness reduction of 0.1mm was detected. An example of the specimen after corrosion can be seen in Figure 5-6. TSA and DIC tests were carried out subsequently to validate the numerical model solutions. These TSA and DIC tests were entirely explained in Chapters 3 and 4.

Figure 5-6 SENT Specimen after corrosion

5.2 Results & Discussion

5.2.1 Validation of numerical results

Firstly, the numerical and TSA experimental average stresses of the corroded specimens without cracks were compared to have a basis of similarity. Using the previously determined thermoelastic constant, k , the average stresses in the corroded specimens without a crack were calculated using Equation 2-5 and tabulated in Table 5-1.

Table 5-1 Numerical and TSA-obtained Stress vs depth of corrosion

Depth of corrosion (mm)	Numerical solution	TSA
0.1	85.4	85.3 ± 0.259
0.25	88.8	89.2 ± 0.415

Numerical solution stresses based on the proposed model were also determined which fit well within the range of the experimental stresses attained. This step provided sufficient confidence that with these similar stresses obtained, the stress field obtained from both TSA and numerical model were similar for the basis of calculation of the stress intensity factor, K .

Then, the stress intensity factor, K , obtained from TSA on pre-cracked specimens without corrosion was compared with the numerical model. Various crack lengths ranging from 4.2mm to 6.1mm were investigated. An example of the full-field stress ($\Delta(\sigma_{xx} + \sigma_{yy})$) from TSA measurement can be seen in Figure 5-7.

Figure 5-7 Stress ($\Delta(\sigma_{xx} + \sigma_{yy})$) map around the crack tip for 10kN with crack length of 4.6mm

Higher stresses were observed around the crack tip with decreasing stresses away from the crack tip. The crack tip location was assumed to be at the highest stress point in the stress field so that the crack length can be estimated.

The experimental stress intensity factors were calculated using Equation 3-3 and plotted in Figure 5-8. The trends in Figure 5-8 show that the experimental stress intensity factors, K , for crack lengths of 4.2mm and 4.55mm are within 2.5% of the numerical curve fit values. However, for cracks of 5.8 and 6.1mm in length, the experimental K was at least 12% lower than the numerical curve fit values.

Figure 5-8 Stress intensity factor vs Crack length for uncorroded specimen

This was because the TSA technique is sensitive to plastic deformation near the crack tip, which led to a non-adiabatic condition and limits the accuracy of the measured temperatures and hence stresses. This can be seen in Figure 5-9 where the numerical stresses along r were lower for both the shorter and longer cracks in Region A.

Figure 5-9 Stresses along r away from the crack tip for a) $a = 4.84\text{mm}$ b) $a = 5.68\text{mm}$

However, the sensitivity of the TSA for non-adiabatic condition at the crack tip seemed to influence Region B from $r = 4\text{mm}$ to $r = 10$ that causes the TSA stresses obtained to be higher than the numerical results. The difference between the TSA data to that of the numerical data were higher for the 5.68mm crack in this region. It was then expected that for a corroded specimen with cracks of 6mm , the experimentally derived K was likely to be lower than numerical estimates.

Subsequently, the stress intensity factors, K , from TSA of corroded specimens of 0.1mm in depth with cracks of 4.2 and 6.1mm were calculated. They were then plotted against the stress intensity factors, K , from the proposed numerical model in Figure 5-10. When comparing against TSA experimental solutions, the numerical model provided a good estimation for a crack length of 4.2mm (with a value within 2.93% of the TSA curve fit solutions), with a similar trend being observed for specimens without corrosion in Figure 5-8.

Figure 5-10 Stress intensity factor vs Crack length for corroded specimen (0.1mm depth)

As expected, the TSA experimental stress intensity factor, K , for the longer crack length of 6.1mm was lower by 14.3% as compared to the numerical curve fit solutions. Despite this limitation for longer crack lengths of 6.1mm, the TSA technique still provided a full-field stress map that allows experimentally based evaluation of the stress intensity factors which validates the numerical solutions for crack lengths of 4.2mm.

Next, DIC was utilised for strain measurements. Firstly, non-corroded specimens with crack length of 4.55 and 5.8mm were tested using DIC technique. The J -integrals were evaluated using Equations 4-8. Figure 5-11 provides an example of ϵ_{yy} from DIC captured strain data.

Figure 5-11 Strain map ϵ_{yy} around the crack tip

From the plot of J -Integral against crack length for uncorroded specimen in Figure 5-12, the J -integrals calculated using strain field data from DIC were 10.4% and 6.42% lower (4.55 and 5.8mm respectively) than the numerically calculated values.

Figure 5-12 J-Integral vs Crack length for uncorroded specimen

Subsequently, the J -integral against crack length for corroded specimen was plotted in Figure 5-13. With a uniform corrosion of 0.1mm, the J -integral values for crack lengths 4.2 and 6.1mm were 5.79% and 9.29% respectively in difference from the numerical curve fit values of the J -integral. The difference in the J -integral values between the numerical model ones and those derived from DIC technique was within the range of 6-10%.

Figure 5-13 J-Integral vs Crack length for corroded specimen ($d = 0.1\text{mm}$)

However, to get an overall validation, both TSA and DIC solutions can be compared with the proposed numerical model using a single fracture parameter, in this case, the J-integral. As such TSA stress intensity factors, K , were converted to J-Integral using Equation 3-6, for crack length of 4.2mm. The DIC and TSA J-integral calculated were within 1.35% of each other which provided good validation for the numerical curve fitting solutions. However, for the longer crack of 6.1mm, the DIC J-Integral calculated was higher compared to the TSA J-Integral calculated; this was because DIC overcomes the limitation of TSA technique of covering post-plastic behaviour and thus provides confidence for the numerical model.

The above work has shown that the two experimental techniques can provide justification for the use of numerical modelling to obtain fracture parameters, for crack lengths up to 4mm. Since artificially corroding these steel specimen takes considerable amount of time, only 0.1mm depth

of corrosion could be induced for the specimen (2.5% depth corrosion). Notwithstanding this constraint, the experimental data still provided a basis for the numerical model concept to be extended for other corrosion depths. As such, handbook solutions were compared to the numerical model for increasing corrosion depth up to 50% thickness.

5.2.2 Comparison of numerical fracture parameters with handbook solutions

It is also important to check the proposed numerical model against handbook solutions by Tada et al. [116]. This is to ensure that numerical model provide a more realistic representation of CIC scenario compared to simplifying the scenario using handbook solutions. The stress intensity factors, K , and J -integral were analysed for various corrosion depths between 0 to 2.0mm and crack lengths of 4.0mm and 6.0mm. As plotted in Figures 5-14 and 5-15, the numerical results were compared with the handbook solutions. For the handbook solutions of J-Integral, the stress intensity factor (SIF) was firstly obtained through Equation 3-7, and then converted the SIF into J-Integral using Equation 4-10.

(a)

(b)

Figure 5-14 K vs Depth of Corrosion for specimen a) $a = 4.0\text{mm}$, b) $a = 6.0\text{mm}$

(a)

(b)

Figure 5-15 J-Integral vs Depth of Corrosion for specimen a) $a = 4.0\text{mm}$, b) $a = 6.0\text{mm}$

In general, the trends in Figures 5-14 and 5-15 show that stress intensity factor, K , and J-Integral increases as the corrosion depth increases. For corrosion depths of 0.5mm and below, the

numerically calculated fracture parameter values differed from handbook solutions by 2 – 10%. This reasonably good agreement between the numerical and handbook solutions showed that the corrosion of such low depth does not influence the fracture parameters too much, and the handbook solutions can still predict the fracture parameters well. However, with the increasing corrosion depth, the differences increase drastically, becoming more significant. For instance, for the 6.0mm crack length at 2.0mm depth, a difference of 97% for K and 82% for J -integral was observed. This significant difference could be due to the corroded area having a more substantial effect on the stress field around the crack tip which was not captured by the handbook solutions. In addition, the dimensions of the corroded area (length and width) could play a role for deeper corrosion defect which might be worth exploring in a next set of further studies. For corrosion depth of less than 12.5% thickness, it can be seen from Figures 5-14 and 5-15 that there was good correlation between numerical solutions and handbook solutions; numerical model can be used with good confidence for these depth range. Yet, for deeper corrosion (of more than 12.5% thickness), it is not a simple case of implying that either the numerical solutions or the SENT handbook solutions can represent the true corroded defect specimen but at least it can provide a range for conservative estimates (between numerical solutions and handbook solutions). Thus, the numerical model concept is still valid for analysis of CIC.

Finally, to complete the analysis of the fracture parameters of such corroded structures using the numerical model, the numerical J -integral value against depth of corrosion was plotted for cracks of length 4.0, 5.0 and 6.0mm in Figure 5-16.

Figure 5-16 J-Integral vs Depth of Corrosion for $a = 4.0, 5.0$ and 6.0mm specimens

The increasing trend of J -integral values with the increasing depth of corrosion was similar for the various crack lengths (especially for crack length 5.0 and 6.0mm). This observation was as expected since the longer crack with an increasing corrosion depth will have a consistently higher J -integral. Figure 5-16 can then be easily referenced to estimate J -integral of these CIC defects.

5.2.3 Proposed Test Protocol for CIC defect using SENT

With non-destructive and non-intrusive testing techniques, such as TSA and DIC, it is possible to characterise fracture parameters of the CIC defect in structures. For instance, the technique can be applied to offshore pipelines where 36% of all pipeline incidents are related to corrosion [128]. This is crucial as from Figure 5-14 and Figure 5-15, handbook solutions provide less conservative values for the fracture parameters compared to numerical solutions, as a result the solutions could be within the failure load for these pipelines. In addition, SENT testing has been enthusiastically

adopted by the pipeline industry in place of the traditional single edge notched bend (SENB) specimens used for standard CTOD tests [88]. Therefore, a proposed test protocol for CIC defect using SENT is shown in Figure 5-17.

Figure 5-17 Schematics of proposed test protocol on offshore pipelines

Here, SENT sample specimens cut out from these pipelines can be sample tested using TSA and DIC techniques to detect CIC defects on these pipelines. The obtained stress and strain data provided by these techniques can be extended in numerical modelling to deliver various iterations of conditions (load, corrosion extend, crack size, etc.). The numerical modelling can thus be utilised to provide an effective and efficient tool to estimate the fracture parameters of the cracks with various depths of corrosion. We can model various crack lengths with regular interval of depths and have a least square curve fitting to the data points. The obtained results are highly useful for failure analysis and failure assessment of corroded offshore pipelines based on fracture mechanics principles, especially for CIC cases.

5.3 Concluding Remarks

The proposed fracture mechanics-based approach had been conducted to quantify the corrosion damage influence on structural integrity. We had investigated the corrosion surface with geometrical structural changes consisting of a general thickness reduction combined with localised pitting-cracks, where the cracks were predicted to initiate or initiated from pitting. These two parameters enabled quantification of the damage and the application of fracture mechanics principles to corrosion damage.

A nonlinear 3-D numerical model concept was validated against TSA and DIC experimental results of coupon specimens for corrosion depth of 0.1mm with varying cracks (length between 4.0 to 6.1mm). Comparison of Stress Intensity Factor, K , and J-integral with experimental testing results provided a basis for the numerical model proposed in quantifying corrosion damage of offshore steels. In our study, 30 numerical models of varying corrosion depth (0 to 2.0mm) and crack length (4.0, 5.0 and 6.0mm) were carried out and the results showed a consistent trend with the handbook solutions. However, for corrosion deeper than 0.5mm (12.5% thickness), numerical solutions give higher K and J-integral values as compared with handbook solutions. Thus, the numerical model concept is still valid for analysis of CIC as it provides a range for conservative estimates when both numerical solutions and handbook solutions are utilised to investigate the CIC cases.

With the conducted validation, our investigation also provides a numerical method for failure analysis for general CIC defective materials. This method could help us more accurately predict

fracture and fatigue failure of corroded offshore pipelines. In the following chapter, we further extend the application of the proposed numerical modelling to pipelines with crack-in-corrosion for other commonly encountered cases in engineering.

Chapter 6

6 Nonlinear Elastic-Plastic fracture analysis for corroded offshore pipelines with 3-D surface cracks subjected to biaxial loadings

Corrosion is commonly found on undersea pipeline due to acidic nature of seawater and various pollutions in the sea. Corrosion damage can greatly reduce the durability and effective lifetime of a pipeline. It is highly important to factor in the corrosion damage in our model to perform much more accurate fracture assessments. The research work reported in Chapter 5 showed that numerical modelling can provide estimation on fracture parameters of crack-in-corrosion defects using SENT specimens. In the current chapter, the fracture modelling methodology was further extended to studying offshore pipelines for crack-in-corrosion cases. The 3-d elastic-plastic fracture behavior of the crack was studied based on the mechanical response of CTOD obtained through structural analysis of the Finite Element Analysis approach.

Furthermore, the existing code DNV-RP-F101: Recommended Practice for Corroded Pipelines [39] did not take into consideration of other failure modes, such as buckling, wrinkling, fatigue and fracture. The assessment procedure mentioned in this standard is applicable only to pipeline steels that are expected to fail through plastic collapse. In addition, the recommended practice in the code does not focus on sharp defects (i.e. cracks) combined with corrosion. Thus, there is a gap to be addressed for the fracture mechanism and failure of such corroded pipelines in current

offshore industry practice. Furthermore, the current industry practice does not deal with defect depths greater than 85% of the original wall thickness (as the case would have been considered as a failure already), the cases studied in this chapter have at most 75% thickness reduction as a limit.

Bhandari et. al [64] provided a technical review of modelling of pitting corrosion for offshore structures. This review highlights the importance of understanding the localized corrosion and its potential to lead to catastrophic failure. It stresses that pit growth is critical in practical applications of offshore structures failure prediction. It poses the challenge to better understand structural failures due to pitting characteristics such as pit depth and cracks propagating from these pits. In addition, the failure pressure of pipelines containing wall reduction and isolated pit corrosion defects were studied [129]. From its findings, there was a significant level of conservativeness and limitations of semi-empirical methods in estimating failure pressure of pipelines containing wall thickness reduction and pit defects. Thus, our current study attempts to simplify these pitting defects to cracks instead. It assumes that these pits grow to form and propagate into a crack. Bedairi et. al [80] and Žiliukas et. al [130] have both attempted to evaluate crack-in-corrosion and corrosion defects (respectively) based on fracture mechanics approach. Bedairi et. al [80] focuses more on failure pressure and model cracks as elliptical pits while Žiliukas et. al [130] demonstrated that these corroded areas were assumed to be cracks which then use J-integral to characterise them. Additionally, Žiliukas et. al [130] found that the maximum decrease in the thickness of the pipe wall after 10 years of operation is no more than 22%.

The main objective of this study was to investigate the crack-in-corrosion (CIC) of offshore pipelines due to large strain loadings through nonlinear elastic-plastic finite element analysis. The effects of corrosion and crack parameters as well as loading conditions on fracture parameters, CTOD, were explored. Due to the interest to check out the severity of such CIC defects on these offshore pipelines and as mentioned by Žiliukas et. al [130], shallow corrosions (less than 30% of pipeline thickness) were considered in most of the cases.

6.1 Methodology

The investigation of crack-in-corrosion in offshore pipelines, subjected to tension, bending and biaxial loadings, had been conducted using 3-D nonlinear elastic-plastic finite element (FE) numerical simulations.

6.1.1 Material properties

The pipeline material steel-X65 was adopted with the isotropic power-law hardening model used to characterize the uniaxial true stress-strain relationship, written as

$$\sigma = \begin{cases} E\varepsilon & \varepsilon \leq \varepsilon_Y \\ \sigma_Y \left(\frac{\varepsilon}{\varepsilon_Y}\right)^n & \varepsilon > \varepsilon_Y \end{cases} \quad (6-1)$$

where σ_Y is the yield stress, $\varepsilon_Y = \frac{\sigma_Y}{E}$ is the corresponding yield strain, E is the Young's modulus and n is the hardening exponent. In the analyses, the Young's modulus, E , is taken as 209 GPa while the Poisson's ratio, ν , is assumed to be 0.3. The true stress-strain response for the pipeline material is shown in Figure 3-1. The yield stress (σ_Y) of 483MPa at room temperature (20°C) and hardening exponent of $n = 0.05$ are obtained for isotropic hardening behavior based on the

uniaxial tension tests [131]. In Figure 3-1, the triangle symbols are from the experimental data and the solid line is obtained from the curve-fitting.

Figure 6-1 The true stress-strain curve for X65 pipeline material.

6.1.2 Geometrical & numerical configurations

The offshore pipeline was 3-D modelled with a corrosion damage zone and a crack at its mid-section of the corroded region. The corrosion damage was taken as a volume reduction as mentioned in Chapter 6 SENT numerical model. This can be seen in Figure 6-2.

Figure 6-2 Schematic drawing of the cracked pipeline mode

Here l is the length of corrosion, w is the width of the corrosion while d is the depth of the corrosion. A flawed pipeline with a corroded volume and a semi-elliptical surface crack was modelled with the dimensions as shown in Table 6-1.

Table 6-1 Pipe dimension

Outer Diameter of pipe, D	800mm
Pipe wall thickness, t	20mm
Length of pipe, L	1600mm

For the surface semi-elliptical crack, the crack length (the major axis of the crack) is denoted by $2c_s$ and the crack depth is represented by a_s . The values of a_s were taken as 2.0 and 4.0mm and for each case of c_s varies from 2.0 to 4.0mm as well. The length of the crack is taken to be perpendicular to the length of the corroded area with centre of the crack length located in the middle of the corroded zone. As mentioned in Chapter 5 with the uniform damage, the corroded

volume on the pipelines can be estimated by a thickness reduction of pipe wall volume as illustrated in Figure 6-2. For this study, the length of corrosion, l , was 450mm with the corrosion depth, d , of 6mm with variable width, w . With reference to Figure 6-3, the maximum global strain ε_g was taken as 3% (24.0mm displacement) for both tension and bending.

Figure 6-3 Boundary and loading conditions applied to the corroded offshore pipelines; displacement-controlled tension, displacement-controlled bending and internal pressure (U1 is in the x-direction, U2 is in the y-direction, U3 is in the z-direction, UR3 is the rotation around the z-axis and IP is the pressure).

Since the loads are path-dependent, the loads for these cases were prescribed through two steps: firstly, a constant internal pressure was applied and then followed by tension or bending moment. This was to follow the practical loads experienced by offshore pipelines in operation. As fluids and gases flow through a pipe, it generated internal pressures and thermal expansion induced tensile and bending forces on the pipelines. The value of internal pressure varied from 10 MPa to 20 MPa, resulting in the ratio of the hoop stress to the yield stress σ_H/σ_Y of 0.2, 0.3 and 0.4. Hoop stress is expressed by $\sigma_H = PD/2t$, in which P is the internal pressure, D is the outer diameter of

pipe and t is the wall thickness. Figure 6-4 provides an overview of the model as seen in ABAQUS with its meshing and crack-in-corrosion modeling. To prevent the problem of stress singularity and to accommodate the large plastic deformation induced by the large plastic strains, a small hole with a radius of 0.01mm was assigned at each crack tip [4]. Focused spider web mesh shown in Figure 6-4 (b) was used in the blunt crack tip region. Based on best practices [4], reasonable results can be obtained if elements around the crack tip subtend angles were in the range of 10° to 22.5° . Ten elements (18°) in the radial and longitudinal directions were seeded for the spider web mesh. For the semi-elliptical surface crack, the crack front (in the circumferential direction) was described by 15 to 30 layers defined over the half crack length. The element sizes were within the range of 0.038mm to 0.330mm.

Figure 6-4 A typical finite element mesh for the cracked pipeline; (a) the mesh grid for the pipeline model, (b) a focused spider web mesh for crack tip region and (c) the detailed mesh grid for the crack-in-corrosion.

For these CIC cases, the total number of elements in the model ranged from 50,000 to 60,000 depending on the crack size. The NLGEOM setting on the Abaqus software was set on to include the feature of non-linear geometry given the large plastic deformation introduced in the model. This means that for large displacements (geometric non-linearity), the stiffness matrix is updated after each loading increment by taking the recent stiffness matrix to the next ‘analysis step’ so that the deformed geometry is considered as an upgraded stiffness matrix in every subsequent ‘analysis step’ in the Abaqus software.

The CTODs obtained through the numerical analysis were illustrated in the following Figure 6-5. CTOD was calculated using the displacement between the intercepts of the nodes (the red colour line and dots in Figure 6-5) obtained from the crack tip and a 45° reference line. Taking the initial position of the nodes as reference, the vertical displacement of the nodes at different strain-loading were measured and tabulated. The values obtained were later plotted against the X-axis component of the displacement of the nodes. Then, a 45° line was then created from the crack tip to intersect with the displacement curves plotted to compute the CTOD. It was noted that the software ABAQUS was not set up to let the crack propagate, hence it can only continue to open the crack up wider, which led to a situation where the CTOD might be beyond its critical CTOD.

Figure 6-5 CTOD calculation in FE analysis

An initial mesh sensitivity study had been performed to check for the accuracy of the mesh use in this study. The analysis is carried out with 3 different meshes and element types for 1 circular elliptical embedded crack case of $a = 2\text{mm}$ and $c = 2\text{mm}$. Taking the lead from Jayadevan et al. [28] and Anderson [4], the same mesh quality was employed for our refined meshing since there was a limitation to the ABAQUS (student edition) program with maximum number of nodes at 100000. The number of elements, nodes and element type were given in Table 6-2. Case 1 meshes use refined elements near the crack region as well as more elements in the region away from the crack with a 8-node linear brick, reduced integration element (C3D8R). This gave a total element number of 63485 with 69968 nodes. As for cases 2 and 3, the meshes had equally refined elements near the crack region but less elements (coarse mesh) in the region away from the crack. Case 2

used the C3D8R elements while case 3 used a 20-node quadratic brick with reduced integration (C3D20R) that gives 62844 nodes.

Table 6-2 The three different cases with various numbers of elements, nodes and element type employed.

Case	Number of elements	Number of nodes	Element Type	Element description
1	63485	69968	C3D8R	8-node linear brick,
2	14185	16248	C3D8R	reduced integration, hourglass control
3	14185	62844	C3D20R	A 20-node quadratic brick, reduced integration

From Figure 6-6, the CTOD-strain curves of the 3 cases were plotted in Figure B1. It shows that results obtained from the coarse mesh (case 2) deviated away from those predicted by the fine model. In addition, results from case 3 with a higher element type deviate only marginally from those of case 1. As such, case 1 mesh quality and element type was employed in the work done, and was used for the numerical models in both Chapter 6 and 7. Overall, the evolution of the CTOD against global strain can be divided into three regions as mentioned by Jayadevan et al. [28]. For the scope of this project, only two regions are clearly defined for strains up to 3% as can be seen schematically in Figure 6-6.

Figure 6-6 The evolution of CTOD with global strain for three different element numbers and types

‘Region 1’ shows the elastic global behavior of the pipe. Initial CTOD values are relatively small but a stronger increase is observed at strain levels just below yield when the crack ligament becomes fully plastic. The non-linearity is due to the plasticity restricted to the ligament and global deformation in the pipe is elastic. Then, ‘Region 2’ represents the region where there is an approximately directly proportionate relation (relatively linear) between the increase in CTOD values and the global strain. This trend is observed as the whole pipe has started to yield, strain is uniform across the whole pipe (for nonlinear material) and thus, uniform plastic deformation occurs due to its strain hardening material properties. Thus, this uniform plastic deformation enables a linear CTOD–strain relationship to be formulated for a strain-based procedure under large plastic strains.

6.1.3 DNV-RP-F101 Standard

To show the validity of corrosion model, the numerical simulation for corrosion damage was checked against DNV-RP-F101 standard solutions for the pressure limit. As seen in Figure 6-6, the corrosion zone has one reduction in volume in a rectangular shape with a length of 200mm and corrosion depth of 4.775mm.

Figure 6-7 Example dimensions and schematics

The pressure resistance (p_{corr}) for a single longitudinal corrosion defect subject to internal pressure loading, is given by:

$$p_{corr} = \gamma_m \frac{2 t f_u (1 - \gamma_d (d/t)^*)}{(D-t) \left(1 - \frac{\gamma_d (d/t)^*}{Q} \right)} \quad (6-2)$$

where D is the nominal outside diameter, f_u is the tensile strength to be used in design, Q is the length correction factor, t is the uncorroded pipe wall thickness, γ_d is the partial safety factor for corrosion depth, γ_m is the partial safety factor for longitudinal corrosion model prediction and

$$Q = \sqrt{1 + 0.31 \left(\frac{l}{\sqrt{D}t} \right)^2} \quad (6-2a)$$

$$\left(\frac{d}{t} \right)^* = \left(\frac{d}{t} \right)_{meas} + \varepsilon_d \text{StD} \left[\frac{d}{t} \right] \quad (6-2b)$$

where l is the longitudinal length of the corroded region, ε_d factor for defining a fractile value for the corrosion depth, $\left(d/t\right)_{meas}$ is the measured (relative) defect depth, $StD\left[d/t\right]$ is the standard deviation of the measured (relative) defect depth. An example calculation from DNV-RP-F101 Corroded Pipelines, Appendix A: Example 1 [39], had be explained and presented to show how this standard calculation was done. This case has the following parameters described in Table 6-2. Using the procedure mentioned in the DNV-RP-F101 and Equations 6-2 with references to DNV-RP-F101 tables, the pressure resistance (P_{corr}) of 17.08 N/mm².

Table 6-3 Case Parameters for DNV-RP-F101 Corroded Pipelines Appendix: Example 1 [39]

Outside diameter	812.8 mm	SMTS	530.9 N/mm2 (X65)
Design pressure (p_d)	150 bar	Accuracy of internal inspection	80%
Design temperature	75°C	Safety Class	Medium
Incidental to design pressure ratio (γ_{inc})	1.1		
α_U	0.96	γ_d	1.28
γ_m	0.85	ε_d	1.0
$f_{u,temp}$	15 N/mm2	StD $\left[d/t\right]$	0.08

6.2 Results & Discussion

6.2.1 Validation of the Corrosion Model

Firstly, the corroded damage pipe model was compared with the code DNV RP F101, the experimental burst test from B. Ma et. al [69] and the work done in Choi et al [132]. These experiments provided a specific limit load solution for corrosion defect assessment in API X65 pipelines by comparing experimental data with finite element analysis results. Our numerical simulation was based on elastic-plastic fracture mechanics (isotropic material with isotropic hardening and Von Mises yielding criterion). The failure pressure of the corroded pipelines will be the corresponding internal pressure pressure when the plastic collapse of the defect region occurs prior to the ultimate stress reached will be the failure pressure. In other words, a stress failure criterion assumes that failure occurs when the maximum von Mises equivalent stress in the ligament of corroded area is equal to the ultimate tensile strength of the pipe steel. This would provide validation of the corrosion damage model mentioned earlier in this chapter. A total of 5 different cases were compared with various corrosion length, width and depth and the results are tabulated in Table 6-3.

Table 6-4 Failure Pressure for various corroded pipeline cases

Case	l (mm)	d/t	d (mm)	w (mm)	Pressure based on von mises stress (Numerical) (MPa)	Pressure based on B. Ma et.[69] al (MPa)	Burst pressure (Experimental) [132] (MPa)
1	200	0.251	4.4	40	24.00	25.58	24.11
2	200	0.503	8.8	40	21.00	21.10	22.61
3	200	0.749	13.1	40	17.25	17.32	17.15
4	100	0.503	8.8	20	23.25	23.72	24.30
5	300	0.503	8.8	60	19.13	19.27	19.80

The above table shows that the burst pressure based on our numerical model matched with the experimental burst pressures conducted in both cases. For both experimental burst pressures, most of the pressures in the cases were within good agreement with each other (an error at most 6.68% for case 2). It was interesting to note that the pressures obtained from different methods were rather similar with at most 7.67% difference for burst pressure of case 2. These results validated our numerical model against the experimental burst tests done by B. Ma et. al [69] and Choi et al [7]. For the cases 1 – 3, it was observed that as corrosion depth increased, the failure pressure decreased as expected. On the other hand, for the cases 2, 4 and 5, the corrosion length increased while maintaining the same corrosion width-length ratio, the longer the length of the corrosion, the lower the failure pressure. However, this effect was not as strong as compared to an increase in corrosion depth.

For pressure based on DNV RP 101, assuming a highly accurate Ultrasonic inspection of the corroded pipeline, the following partial safety factors were used:

$$f_u = 0.96 \times SMTS$$

$$\gamma_d = 1$$

$$\gamma_m = 0.77 \text{ (very high safety class)}$$

$$\gamma_m = 0.94 \text{ (low safety class)}$$

$$\varepsilon_d = 0$$

$\text{StD}\left[\frac{d}{t}\right] = 0$ (The UT is an absolute measurement where the local wall thickness, the defect depth measurement and the accuracy are given directly.)

Table 6-5 Failure Pressure for various corroded pipeline cases

Case	l (mm)	d/t	d (mm)	w (mm)	Pressure based on von mises stress (Numerical) (MPa)	Pressure based on DNV RP F101 For high safety class (MPa)	Pressure based on DNV RP F101 for low safety class (MPa)
1	200	0.251	4.4	40	24.00	18.03	22.01
2	200	0.503	8.8	40	21.00	15.37	18.76
3	200	0.749	13.1	40	17.25	10.76	13.14
4	100	0.503	8.8	20	23.25	17.93	21.89
5	300	0.503	8.8	60	19.13	13.74	16.77

Overall, the burst pressures base on DNV RP F101 were mostly lower than the numerical pressure. This can be explained as there are safety factors in placed for DNV RP F101. The effect of the inspection accuracy and other uncertainties based on Equation 6-2 are accounted for in calculating the safety factor. This includes several partial safety factors that were introduced to ensure consistent reliability level for the validity range of input parameters. For high safety class, average safety factor is 0.71927 while for the low safety class is 0.87806. The values compared is around on average 6.6% difference from the numerical to the DNV RP F101. DNV RP F101 is based on its safety class and safety factors. As a result, it provides a relatively lower failure pressure compared to the numerical solution pressures. As such, if minimum required reliability level is taken, this code would be conservative for some combinations of the input parameters. Results of

FE analyses together with pressure variations and selected levels of uncertainties in the defect sizing, form the required basis for a reliability code calibration where appropriate safety factors were defined. This result showed that the DNV code can be coupled with our numerical models instead of burst tests to ensure higher reliability since to conduct experimental burst tests are costly and limited. This provides a good basis if safety factors are to be put in place for our numerical solutions.

6.2.2 Interaction between corrosion & crack

In our study, a series of numerical simulations with varying crack size and several types and magnitude of loadings had been carried out. The results obtained are illustrated in graphical means. Firstly, the effects of corrosion parameters on the fracture behavior of the crack were explored. Then, the effects of crack size parameters were studied.

6.2.2.1 Effects of corrosion parameters

Firstly, non-linear elastic-plastic finite element analyses had been carried out for corrosion depth of 2.5mm, 5.0mm and 10mm with a constant corrosion width of 20.0mm and length of 450mm. The crack was placed in the centre of the corroded area with its crack width perpendicular to corrosion length. The crack dimensions were a_s were taken as 2.0 and 4.0mm and for each case of c_s varies from 2.0 to 4.0mm. These pipeline models were loaded with 10MPa internal pressure first and then tensile displacement was added to the system. Figure 6-7 shows the CTOD value against applied global strain for the cracks with varying corrosion depth.

Figure 6-8 Curves of CTOD against applied global strain for different corrosion depth with corrosion width 20mm

It can be observed that as corrosion depth increased, the CTOD response was larger at higher strains. The difference at 3% strain for the various corrosion depth seemed proportionate to the thickness increase. This was as expected from the earlier failure pressure studies that a lower pressure failure was observed for higher thickness corrosion. On the other hand, the corrosion depth was fixed at 2.5mm with varying corrosion widths of 10mm, 20mm and 30mm to observe the effect of the discontinuity of the corroded volume on the CIC fracture response. Figure 6-8 showed the CTOD against the applied global strain for varying corrosion widths.

Figure 6-9 Curves of CTOD against applied global strain for different corrosion width with corrosion depth 2.5mm

Contrary to expectations, it was observed that the corrosion width of 30mm had a lower CTOD response than the 20mm width case which had the highest CTOD value (with a 12.5% difference of CTOD at 3% strain). This could mean that the discontinuity had a material hardening effect on the local crack tip plasticity. Interestingly, at 20mm, there could be other mechanism that might reduce this material hardening effect significantly; more observation might be needed for a more conclusive discussion. However, the CTOD increased due to the corrosion width change was not as much as the CTOD increased due to the corrosion depth change. This result indicated that it was more important to be able to measure and estimate the corrosion depth than the corrosion width of the corroded area. However, the width effect was still sufficiently significant, and should be taken into consideration and not be neglected such as in DNV-RP-F10.

6.2.2.2 Effects of crack dimension parameters

Next, the effects of crack dimension parameters were explored. This was done with varying crack size parameters (a_s , $c_s = 2, 4\text{mm}$) subjected to internal pressure with tensile/bending load. The results obtained are shown in Figures 6-9 and 6-10 for tensile and bending loadings, respectively. As a quick mention, the evolution of the CTOD against global strain can be divided into two regions. ‘Region 1’ is the elastic global behavior of the pipe. Initial CTOD values were relatively small but a stronger increase was observed at strain levels just below yield when the crack ligament becomes fully plastic. Then, ‘Region 2’ represents the region where there was an approximately directly proportionate relation (straight line) between the increase in CTOD values and the global strain. For example, in the case of crack with parameters $c_s = 2$ and $a_s = 2$, ‘Region 1’ was denoted by the dotted line at 0.014 in Figure 6-9.

Figure 6-10 Curves of CTOD against applied global strain for different crack parameters subjected to tension loading only

Figure 6-11 Curves of CTOD against applied global strain for different crack parameters subjected to bending loading only

In Figure 6-9, it was observed that a crack with wider width $c_s=4$ and crack depth $a_s=2$, its CTOD was lower than the rest at 3% applied global strain. ‘Region 1’ for this crack dimension was also longer than the other 3 crack cases. It was seen that the CTOD value increases with the increasing crack depth. As the crack dimension changed, so did the trend. This was seen for the case of $c_s=4$ mm and $a_s=2$ mm, where the CTOD response was lower than that for the rest of the other cases. This was due to the crack dimensions a_s/c_s was less than 1 which caused the CTOD response to be lower at the crack tip with the bigger crack width. In Figure 6-10, where the CIC defects on offshore pipelines were subjected to bending loading only, it can be seen clearly that as the crack size increased, so did the CTOD response (the CTOD value is higher at 3% applied global strain for larger crack of $a_s=4$ mm and $c_s=4$ mm). However, in the case with crack dimension $a_s=2$ mm

and $c_s = 2\text{mm}$, the CTOD value was the lowest. As compared with the tensile loading case, due to bending loading, increasing the crack dimension indeed increased the CTOD response.

6.2.2.3 Effects of biaxial loading: tension + internal pressure

In this section, the fracture behaviors of the crack in corrosion zone were investigated when the flawed pipeline was subjected to the combined loadings: the tensile loading combined with the internal pressure. The effect of internal pressure was considered through the hoop stress caused. Since our numerical simulations were based on elastic-plastic fracture mechanics, the results obtained were not a linear superposition of the two loadings.

Four models with various crack dimensions ($a_s, c_s = 2, 4\text{mm}$) were analysed for the four different internal pressure (σ_H/σ_Y of 0.2, 0.3 and 0.4) for up to 3% applied global tensile strain. Taking the case of $a_s = 2\text{mm}$ and $c_s = 2\text{mm}$ in Figure 6-11 as an example, it can be observed that the CTOD increased significantly with the internal pressure (the hoop stress). For the two curves with σ_H/σ_Y of 0.2 and 0.3, both CTOD values increased about 67% at 3% applied global strain, when comparing with the curve without any hoop stress (no internal pressure). In addition, the induced hoop stress caused a decreasing ‘Region 1’ with the applied strain region from 1.25% for no pressure to 0.9% for σ_H/σ_Y of 0.4. These trends were similar for the model of crack dimensions $a_s = 4\text{mm}$ and $c_s = 2\text{mm}$ as shown in Figure 6-12.

Figure 6-12 Curves of CTOD against applied global strain with crack dimension $c_s=2$ and $a_s=2$ subjected to tensile loading with varying hoop stress

Figure 6-13 Curves of CTOD against applied global strain with crack dimension $c_s=2$ and $a_s=4$ subjected to tensile loading with varying hoop stress

On the other hand, for the case of $a_s = 2\text{mm}$ and $c_s = 4\text{mm}$ in Figure 6-13, there was a detectable difference in CTOD values at 3% strain for the two curves with σ_H/σ_Y of 0.2 and 0.3 respectively. Here ‘Region 1’ was confined within 0.5% applied strain. Similar observations can be made for $a_s = 4\text{mm}$ and $c_s = 4\text{mm}$ for Figure 6-14.

Figure 6-14 Curves of CTOD against applied global strain with crack dimension $c_s=4$ and $a_s=2$ subjected to tensile loading with varying hoop stress

Figure 6-15 Curves of CTOD against applied global strain with crack dimension $c_s=4$ and $a_s=4$ subjected to tensile loading with varying hoop stress

In general, with internal pressure added to the pipeline before the tensile strain loading, the induced hoop stress caused an increase in CTOD response. The physical mechanism behind was that the hoop stress (induced by the internal pressure) changed the plastic zone size at the crack tip. Still, for the case of $a_s = 4\text{mm}$ and $c_s = 2\text{mm}$, the internal pressure response was the lowest compared to the other 3 cases. The main reason was that the crack ratio, c_s/a_s for this case was less than 1. High stress was normally found along the shorter distance of the crack from the centre of the crack origin. Therefore, for this inverse crack ratio model, the shortest distance from the crack centre was the crack width. However, the CTOD value was taken from the crack depth, which led to lower CTOD values. In general, σ_H/σ_Y of 0.4 caused the highest CTOD response compared to the lower internal pressure cases considered.

6.2.2.4 Effects of biaxial loading: bending + internal pressure

In this section, the flawed pipeline subjected to a bending loading combined with internal pressure was explored. The relationship between the hoop stress (due to the internal pressure) and the CTOD growth as bending-induced global strain increases were studied in detail.

From Figure 6-15 to Figure 6-18, notably, the cases with zero hoop stress usually yielded the lowest CTOD value. Particularly in Figure 6-15 (the case with crack dimension $a_s = 2\text{mm}$ and $c_s = 2\text{mm}$), all the curves had similar trends with those in tension + internal pressure cases discussed in the previous section, where increasing hoop stress result in higher CTOD values. Similarly, the CTOD values increased significantly at high strain level. The CTOD value for $\frac{\sigma_H}{\sigma_Y} = 0.4$ is 3.8 times larger than that for $\frac{\sigma_H}{\sigma_Y} = 0$ where only bending load was applied.

Figure 6-16 Curves of CTOD against global strain with crack dimension $c_s=2$ and $a_s=2$. The pipe is subjected to bending load with varying hoop stress.

Figure 6-17 Curves of CTOD against global strain with crack dimension $c_s=2$ and $a_s=4$. The pipe is subjected to bending load with varying hoop stress.

Figure 6-18 Curves of CTOD against global strain with crack dimension $c_s=4$ and $a_s=2$. The pipe is subjected to bending load with varying hoop stress.

Figure 6-19 Curves of CTOD against global strain with crack dimension $c_s=4$ and $a_s=4$. The pipe is subjected to bending load with varying hoop stress.

Interestingly, the above-mentioned trend was not observed on the three other cases with differing crack dimensions, as shown in Figures 6-16, 6-17, 6-18, respectively. When the internal pressure was introduced for the cases presented in Figure 6-16 to Figure 6-18, the CTOD values decreased when the hoop stress increased: the cases with $\frac{\sigma_H}{\sigma_Y} = 0.2$ yield the largest CTOD values. Bending causes ovalisation of the pipe which in turn causes stresses in the hoop direction. As such, the increased internal pressure causes the ovalisation to decrease due to bending. This decrease in ovalisation could lead to the observation mentioned above. This scenario occurs when the material is strained beyond its yield point, and an increasing stress is required to produce additional plastic deformation. Therefore, the additional internal pressure on the model could have resulted in the hardening in material at the corroded ligament section. Therefore, there was lesser deformation

which yields a lower CTOD value. This observation raised the question of bending loading on varying corrosion parameters which was not within the scope of the chapter. It could be potentially further investigated on this matter.

6.2.2.5 Comparison between tension and bending loads

As our numerical was based on elastic-plastic fracture mechanics, the results obtained were load path dependent. As observed in the previous sections, the introduction of internal pressure mostly increases the CTOD values significantly under the two combined loads. With crack dimensions of $c_s = 2\text{mm}$ and $a_s = 2\text{mm}$, Figure 6-17 shows the CTOD values for various loading cases of tension/bending with/without internal pressure.

Figure 6-20 Curves of CTOD against global strain with crack dimension $c_s=2$ and $a_s=2$. The pile is subjected to tension/bending loads with/without hoop stress

It can be observed that the CTOD value for “bending loading only” case was just slightly larger than the case for “tension loading only”. ‘Region 1’ for “bending only” was at a smaller applied global strain range (0.55%) compared to “tension only” (1%). However, at higher global strain of 3%, pure bending loads CTOD-strain response was similar to that of CTOD-strain response of pure tension loadings. This could be due to the ovalisation of the pipe under bending which causes bending stresses in the hoop direction. Yet, the cross-section of the pipe ovalises strongly with higher strains which may have cause a smaller applied global strain range. At higher strains, these bending stresses from ovalisation of the pipe are then translated to tensile stresses at the top side of the thickness and compressive at the bottom. In addition, the shear bands of the cracked pipe under bending will be subjected to additional out-of-plane tensile stresses.

On the other hand, it can be observed that the CTOD values for “bending + internal pressure” was much larger than those of “tension + internal pressure” at any strain level in both cases. It implied that the significant CTOD increase was brought by the addition of internal pressure. As such, a combined load of internal pressure σ_H/σ_Y of 0.2, followed by tensile and then bending case had been tested out. Remarkably, this produced the same CTOD response as to one that has a combined load of internal pressure σ_H/σ_Y of 0.2, followed bending case (without the tensile load). It seemed that bending and tension did not add up in superposition but instead it took the bending response instead. This could be explained due to the ovalisation of the corroded ligament softening effects on the crack tip plasticity. This in turn was reflected in the CTOD-strain relationships observed and might explain the same CTOD response mentioned above. Overall, comparing “tension + internal pressure” and “bending + internal pressure”, our study indicated that “bending + internal pressure” was the most serious case.

6.2.2.6 CTOD fracture criterion

As mentioned in Section 2.3, critical CTOD of 0.45mm was assumed. For tension cases shown in Figure 6.11 and 6.12, the structure did not meet the CTOD fracture criterion even at 3% strain; these crack configurations are considered safe for the various biaxial loading (tension and internal pressure) applied. However, from Figure 6.13 and 6.14, the CTODs of both crack configurations for the case of biaxial loading with σ_H/σ_Y of 0.4 met the CTOD fracture criterion at 3% strain; structural failure is to be expected here for the pipeline.

On the other hand, for bending cases shown Figure 6.15, CTOD reached critical values at 3% strain for biaxial loading with σ_H/σ_Y of 0.3 and 0.4. However, for crack dimensions $a_s = 2\text{mm}$ and $c_s = 4\text{mm}$ in Figure 6.16, the structure did not meet the CTOD fracture criterion even at 3% strain. The CTOD response for this crack dimension was lower compared to the case in Figure 6.15 due to the crack configuration. As for $a_s = 4\text{mm}$ and $c_s = 2\text{mm}$ in Figure 6.17, CTOD for biaxial loading cases reached critical CTOD values at 1.5% strain. For $a_s = 4\text{mm}$ and $c_s = 4\text{mm}$ in Figure 6.18, CTOD for all cases reached critical at 2% strain. It can be observed that the crack ratio, c_s/a_s , had more effect on CTOD for biaxial cases, higher crack ratio caused a higher CTOD response.

Therefore, it can be concluded that by knowing the crack dimensions and its loading, the CTOD-strain curve can be numerically produced which can then be checked against the fracture criterion to see if such cases would be acceptable at a certain strain loading conditions.

6.2.3 CIC defect model and CTOD estimation

As a follow up to Section 5.2.3, our current investigation can be added to the proposed test protocol. With non-destructive and intrusive techniques, such as TSA and DIC, it is possible for in-situ inspection of corroded structures. The current chapter provides a basis for industry where numerical modelling together with full-field techniques offers comprehensive failure assessment analysis for corroded structures. The proposed test protocol is modified and improved as shown in Figure 6-20.

Figure 6-21 Schematics of proposed test protocol on offshore pipelines

The offshore pipelines can be deployed with pipeline maintenance systems (e.g. pigging systems) and autonomous underwater vehicles (AUV) with visual-based techniques to detect corrosion damaged structures. With the obtained crack and corrosion parameters, numerical modelling can

then be utilised to provide various iterations of the conditions (load, etc.) which then be checked by the inspection data provided by TSA and DIC techniques (as mentioned in Chapter 4 and 5). In this case, TSA can observe the current stress field in the pipelines and detect high stress concentrations for more in-depth analysis. While the DIC can capture various images at various inspection timings, and its algorithm is able to calculate the strain fields of the pipeline. Therefore, a complete failure assessment for corrosion damage in offshore pipelines can be conducted based on elastic-plastic fracture mechanics.

6.3 Concluding remarks

In offshore pipelines, there are significant differences for failure assessment of cracks with/without corrosion damage. In the present study, based on the nonlinear elastic-plastic fracture mechanics, the crack-in-corrosion (CIC) defects in offshore pipelines subjected to large global strain loadings with/without internal pressure had been investigated. The 3-D cracks were located at the centre of the corrosion region. Shallow corrosions (less than 30% of pipeline thickness) were considered. A series of elastic-plastic large deflection analyses had been carried out with varying crack dimensions and corrosion parameters for various loading conditions. Based on our nonlinear numerical simulations, the following conclusions can be drawn:

- 1 Currently in related industry, there are no handbook solutions and standards on CIC cases. This is the first time that the elastic-plastic fracture mechanics assessment on cracks in corrosion zone with various loading conditions has been conducted.
- 2 DNV RP F101 provides a relatively lower failure pressure compared to the numerical based on its safety class and safety factors. As such, if minimum required reliability level is taken, this code would be conservative for some combinations of the input parameters. Results of

FE analyses together with pressure variations and selected levels of uncertainties in the defect sizing, form the required basis for a reliability code calibration where appropriate safety factors were defined. This result showed that the DNV code can be coupled with our numerical models instead of burst tests to ensure higher reliability since to conduct experimental burst tests are costly and limited. This provides a good basis if safety factors are to be put in place for our numerical solutions.

- 3 With a large amount of nonlinear finite element numerical simulations, the effects of corrosion, crack dimension and loading conditions on the fracture parameter CTOD had been investigated in detail.
- 4 In failure assessment based on CTOD criterion, the corrosion depth is a more important parameter compared to the total corrosion zone volume.
- 5 An important future study should consider crack cases where the crack permeates from the localized pitting corrosion.

Chapter 7

7 Elastic-Plastic Fracture Mechanics Investigations on Offshore Pipelines with Interacting 3-D Surface and Embedded Cracks

As mentioned in our gap in Chapter 2, the complex defect configuration of a surface crack and an embedded crack was chosen to represent the case of pitting developing into a surface crack and interacts with existing mechanical defects (embedded cracks) in the pipeline. Hence, the non-linear Elastic-Plastic Fracture Mechanics (EPFM) responses was investigated thoroughly for two interacting 3-D cracks (a semi-elliptical surface crack and an elliptical embedded crack) in offshore pipelines. The current study explored systematically the influence of the related crack parameters, with the objective of comparing our results with the BS7910:2013 standard code. At the end, a strain-based CTOD estimation scheme was proposed for the fracture assessment of interacting cracks.

7.1 Methodology

In our study on the two coplanar 3-D interacting cracks in offshore pipelines, extensive nonlinear elastic-plastic simulations had been carried out with the finite element (FE) software package ABAQUS to investigate the fracture behavior of the pipe subjected to tension, bending and biaxial loadings. This section specifies the material properties, geometrical configuration, FE modeling and loading conditions employed in the models. For the cases in this chapter, Figure 2-4 shows the

schematics of the semi-elliptical surface crack with a near-by elliptical embedded crack of the flawed pipelines.

API 5L X65 Steel was being identified as the base material of the pipelines. The material properties were described in Chapter 6. As with the crack-in-corrosion cases done in Chapter 6, this chapter deals with the complexity of multiple cracks.

7.1.1 Geometrical configuration

A circumferentially flawed pipeline with one semi-elliptical surface flaw and an elliptical embedded crack was modelled with an outer diameter D of 400mm and wall thickness t of 20 mm.

The pipe configuration and two cracks geometries are displayed in Figure 7-1.

Figure 7-1 Schematic drawing of the cracked pipeline model: (a) an overview of the pipeline with a surface semi-elliptical crack and an embedded elliptical crack; (b) detailed cracks geometry used in the simulations.

The length of the pipe segment L adopted in all analyses was two times the outer diameter which was sufficiently long for bending moment cases, as recommended by Jayadevan et al. [28] and Øtsby [133]. For a semi-elliptical surface crack, the crack depth is denoted by a_s and the major axis length of ellipse is represented by $2c_s$. The value of a_s was taken as 1, 2, 3, 4, 5 and 7mm which covered the range of a shallow to a deep surface crack. For each a_s , three values of c_s varying from 3mm to 35mm, were considered. On the other hand, for the embedded elliptical crack, the crack height is denoted by $2a_e$ and the embedded crack length is $2c_e$. Three crack size values were considered for comparisons. Furthermore, the separation distance between the surface crack tip and the left crack tip of embedded crack is denoted by s , varying from 3mm to 9mm. A total of eighty-one crack configurations were considered.

Figure 7-2 clarifies the notation used in this chapter for discussion of the various CTODs. Here, $CTOD_s$ represents the CTOD value at the surface crack tip, $CTOD_L$ and $CTOD_R$ denotes the CTOD value at the left and right crack tip of the elliptical embedded crack, respectively. The results obtained from the cases with only single crack are denoted by “only”. In addition, a dimensionless parameter, normalized crack separation distance, s_n , is proposed as

$$s_n = \frac{s}{a_s + a_e} \quad (7-1)$$

, which is the ratio of the critical distance against the true distance between the two cracks.

Figure 7-2 Cracks geometry showing CTOD_s, CTOD_L and CTOD_R used in the simulations.

7.1.2 FE modeling

ABAQUS was utilized for computation. The model using one-quarter of the pipeline model was built with linear brick element with reduced integration, C3D8R, by considering symmetry of the problem. Figure 7-3 shows a typical finite element mesh for the two cracks located in the cracked pipeline with similar mesh patterns employed for other crack geometry configurations. To prevent the problem of stress singularity and to accommodate the large plastic deformation induced by the large plastic strains, a small hole with a radius of 0.01mm was assigned at each crack tip [4].

Focused spider web mesh shown in Figure 7-3 (b) was used in the blunt crack tip region. Ten elements in the radial and longitudinal directions were seeded for the spider web mesh. For the semi-elliptical surface crack, the crack front (in the circumferential direction) was described by 15 to 30 layers defined over the half crack length. As for the embedded elliptical crack, the crack front was assigned with 10 to 20 layers. Consequently, a typical FE model for the flawed pipe with multiple cracks had approximately 51,000 to 62,100 elements, depending on the crack size. The ‘Nlgeom’ option was activated to include the feature of the non-linear geometry given the large

plastic deformation introduced in the model. An initial mesh study has been done to ensure consistency of results. This initial mesh study was described in Chapter 6.

Figure 7-3 A typical finite element mesh for the cracked pipeline; (a) the mesh grid for the pipeline model, (b) a focused spider web mesh for crack tip region and (c) the detailed mesh grid for the two cracks.

7.1.3 Loadings

The schematic diagram for these boundary conditions is shown in Figure 7-4. The boundary conditions for the simulation cases were set as follows:

- 1) One end of the pipeline segment except the cracked surfaces was constrained in Y direction, i.e. $U_2=0$, while the other end was applied with the loadings.

Figure 7-4 The boundary conditions for the simulation cases

- 2) The two surfaces which were normal to Z axis, were assigned to Z - symmetry, i.e. $U_3=0$.

The displacement-controlled tension and pure bending load applied on offshore pipelines were shown in Figure 7-5 (a) and Figure 7-6 (a) respectively. Due to symmetry, the rotation for bending cases was imposed on the un-cracked end of the pipe through a set of kinematic coupling constraint so that the pure bending moment can be loaded on the pipe model with zero shear force [134]. The value of the rotational angle was determined based on Euler-Bernoulli beam theory.

Accordingly, the maximum global strain ϵ_g was taken as 3% for both tension and bending for this study cases [6]. For the biaxial loading case, the internal pressure was considered. The two different biaxial loadings were displayed in Figure 7-5 (b) and Figure 7-6 (b), respectively. Since the loads were path-dependent, the loads for these cases were prescribed through two steps: firstly, a constant internal pressure was applied and then followed by tension or bending moment. This loading sequence was common in practice [135, 136], and can produce high value of fracture response [137]. The value of internal pressure varies from 10 MPa to 25 MPa, resulting in the ratio of the hoop stress to the yield stress σ_H/σ_Y of 0.2, 0.3 and 0.5. Hoop stress is expressed by $\sigma_H = PD/2t$, in which P is the internal pressure, D , is the outer diameter of pipe and t is the wall thickness.

(a)

(b)

Figure 7-5 Various loading conditions applied to the offshore pipelines; (a) displacement-controlled tension and (b) displacement-controlled tension combined with internal pressure.

Figure 7-6 Various loading conditions applied to the offshore pipelines; (a) pure bending load and (b) bending load combined with internal pressure.

7.2 Results and Discussion

Firstly, preliminary observations were made for single crack cases to have a basis of comparison for two interacting cracks cases. The results for a single crack case were also compared with those from Jayadevan et al. [28]. Subsequently, the interaction effects of the semi-elliptical surface crack and the elliptical embedded crack CTOD values for the uniaxial strain (tensile) loading conditions were described. The calculation of CTOD values were mentioned in Chapter 6. The influence of crack size, shape ratio, separation distance between the surface crack and embedded crack was discussed in detail. The crack interaction effects were presented with a discussion on the dominant crack tip, the influence of the two-crack separation distance s , combined loading on the fracture response, interaction effects of the embedded crack onto the surface crack and interaction effects of the surface crack onto the embedded crack. Further, the evolution of crack tip opening displacement (CTOD) under biaxial loading conditions was addressed. A comparison of the current numerical data of the two cracks was made against the numerical data of crack models based on BS7910:2013 effective flaw dimensions after interaction. The results and discussion section concluded with the strain-based CTOD estimation scheme and a proposed flow chart for CTOD estimation of 2 coplanar cracks.

7.2.1 Preliminary observations

This section describes the preliminary observations from the simulations of a single crack in a pipeline under tensile strain loading. For pipeline models with surface crack only, the surface crack CTOD values generally increased linearly with the strain as seen from Figure 7-7. Moreover, the curves of CTOD-strain became steeper with the increasing crack area. The difference in CTOD

values at global strain $\varepsilon_g = 0.03$ can be as much as 10 times from the smallest crack ($a = 1\text{mm}$, $\frac{c}{a} = 1$) to the largest crack ($a = 4\text{mm}$, $\frac{c}{a} = 5$) modelled. At low global strain of less than 0.5%, with similar crack area ($a = 1\text{mm}$, $\frac{c}{a} = 3$; $a = 1\text{mm}$, $\frac{c}{a} = 5$; $a = 2\text{mm}$, $\frac{c}{a} = 1$), it was observed that the CTOD-strain curve had similar linear trend.

Figure 7-7 Curves of CTOD against global strain for different surface crack depth and shape ratio

With the same crack shape ratio, $\frac{c}{a}$, the CTOD-strain curve gradient increased as crack depth increased. For crack shape ratio $\frac{c}{a} = 1$ at $\varepsilon_g = 0.03$, as crack depth, a , increased from 1mm to 2mm and then to 4mm, the CTOD values increased by 81.1% and 103.9% respectively. Likewise for crack shape ratio $\frac{c}{a} = 3$, the CTOD values increased by 100.2% for $a = 1\text{mm}$ to $a = 2\text{mm}$ and 128.7% for $a = 2\text{mm}$ to $a = 4\text{mm}$. Hence, the crack shape is a parameter in the crack-blunting phenomenon which has also been observed by Jayadevan et al. [28].

In addition, for the same crack depth a , increasing the crack shape ratio increased the gradient of the CTOD-strain curve. At $\varepsilon_g = 0.03$, increasing the crack shape ratio from $\frac{c}{a} = 1$ to $\frac{c}{a} = 3$ caused a higher increase in CTOD values compared to increasing the shape ratio from $\frac{c}{a} = 3$ to $\frac{c}{a} = 5$. For example, taking the crack depth $a = 4\text{mm}$, there was a 104% increase in CTOD value by increasing the crack shape ratio from $\frac{c}{a} = 1$ to $\frac{c}{a} = 3$. However, the increase was only 14.6% by changing the shape factor from $\frac{c}{a} = 3$ to $\frac{c}{a} = 5$. The same observations were seen in cases with crack depth $a = 1\text{mm}$ and $a = 2\text{mm}$. It can be noted that this difference was more significant for the case of crack depth of $a = 4\text{mm}$. In other words, as the crack is getting deeper, the effects of crack ratio becomes more evident.

Figure 7-8 Curves of CTOD against strain for different embedded crack height and shape ratio

Considering Figure 7-8 (a) for an embedded crack only in pipelines, the CTOD values for the right and left crack tip were approximately similar for shallow cracks ($a = 1\text{mm}$, $a = 2\text{mm}$). However, for deeper crack as the right tip modelled nearer to the inner pipe wall ($a = 4\text{mm}$, $\frac{c}{a} = 3$), the

embedded right CTOD values tend to be higher than the left CTOD. From Figure 7-8 (b), for the embedded left CTOD, as crack area increased, the gradient of CTOD-strain curve increased. The difference in CTOD values at $\varepsilon_g = 0.03$ can be as much as 20 times larger for the largest crack against the smaller crack modelled. Thus, crack shape ratio effect is more distinct for deeper cracks compared to shallow ones.

7.2.2 Two 3-D interacting crack case

With the flawed pipeline subjected to tension load, the fracture response of the circumferential surface crack was investigated for different crack sizes and separation distances to the embedded crack. Figure 7-9 shows the $CTOD_s$ -strain curves for different separation distances between the two co-planar cracks. The results obtained from the cases with only one surface crack denoted by “only”, were also presented for comparisons. From Figure 7-9 (a) and (b), it was seen that in case of $a_s = 3mm$, $CTOD_s$ increased linearly with the strain for both $c_s = 3mm$ and $c_s = 15mm$. Marginal difference between the case of “only” and the case of $s = 9mm$ indicated that the effect of embedded crack on the fracture response of surface crack is minimal. It is consistent with the combination rule of effective crack by BS7910:2013, in which no interaction is considered if $s > (a_s + a_e)$. In other words, when the two cracks are separated by more than the critical interaction distance ($s = a_s + a_e$), the two cracks can be regarded as isolated (crack interaction can be neglected). While the two cracks were closer ($s = 6mm$ is the critical interaction distance), greater $CTOD_s$ value was obtained, and the increase could be as high as 13% when the global strain was 0.03.

It can be observed that when the separation distance s was smaller than the critical interaction distance, the curve of $CTOD_s$ against strain became steeper, compared to the case of “only”, indicating that the $CTOD_s$ value starts to be affected by the crack interaction due to the shorter separation distance. Moreover, from Figure 7-9 (e) and (f), it can be observed that when the separation distance was further reduced, the cracks interaction became more pronounced, resulting in much higher $CTOD_s$ values. These observations imply that for varying surface crack to embedded crack distance, the fracture response at relatively far distance is affected by the crack interaction in a mild manner. Whereas the non-linear and rapid increases in the fracture response of deep surface crack are observed when the separation distance is reduced. Furthermore, compared to the single crack cases, the CTOD values can be doubled or even higher because of the strong interaction of two closely adjacent cracks.

Figure 7-9 Curves of CTOD_s against strain for different separation distance.

To further understand the interactions of two coplanar cracks, the simulations of the flawed pipeline with different embedded crack sizes are also carried out while the surface crack remains unchanged under pure bending load. The relationships of $CTOD_L$ -strain with different separation distances are presented in Figure 7-10 (a) and (b) for the embedded crack's left crack tip and surface crack tip, respectively. For a small embedded crack ($a_e = 2mm$), the difference of $CTOD_L$ evolutions between the case of $s = 7mm$ and $s = 9mm$ was marginal. While for a large embedded crack ($a_e = 4mm$), the $CTOD_L$ value increased significantly in both $s = 5mm$ and $s = 3mm$. Similarly, the curves of $CTOD_s$ versus strain were plotted in Figure 7-11 (b). It was noticed that if the two cracks were located far apart such that no interaction is considered (i.e., $s > a_s + a_e$), the overall fracture behavior of cracked pipeline was dominated by relatively large surface crack alone, compared to the small embedded crack case. However, in the case of $a_e = 4mm$, the $CTOD_s$ value increased by two times at any strain level when the separation distance s was reduced from 5mm to 3mm. It demonstrated that for a given cracks configuration, the fracture response of the two coplanar cracks is significantly affected by the strong interaction when they are located closer to each other.

Figure 7-10 (a) Curves of $CTOD_L$ versus strain for different embedded cracks and (b) corresponding curves of $CTOD_s$ versus strain, while the surface crack $a_s = 5$ and with $\frac{C_s}{a_s} = 3$ is considered.

7.2.3 Effect of combined biaxial loadings

In this section, the nonlinear elastic-plastic fracture analyses had been conducted on the flawed pipeline subjected to the combined loadings: the tensile loading combined with the internal pressure. The influence of the hoop stress (induced by internal pressure) on the $CTOD_s$ evolutions was investigated in detail.

Given the same separation distance of $s = 5mm$, the comparisons of $CTOD_s$ values obtained from tension combined with different internal pressure cases are presented in Figure 7-11. “T+P” denotes the tension combined with internal pressure. Three different values of σ_H/σ_Y were considered as 20%, 30% and 50% respectively, and $\frac{\sigma_H}{\sigma_Y} = 0$ represents the tension only. In Figure 7-12 (a), for surface crack of $a_s = 5mm$ with $\frac{C_s}{a_s} = 5$, $CTOD_s$ increased gradually as the hoop

stress increased at the same global strain level. For a surface crack of $a_s = 7\text{mm}$ with $\frac{c_s}{a_s} = 5$ shown in Figure 7-11 (b), the linear increasing trend of all four $CTOD_s$ -strain curves could be observed when $\varepsilon_g < 0.02$.

Figure 7-11 Comparisons of $CTOD_s$ values obtained from tension combined with different internal pressures; (a) surface crack of $a_s = 5\text{mm}$ with $\frac{c_s}{a_s} = 5$ and (b) surface crack of $a_s = 7\text{mm}$ with $\frac{c_s}{a_s} = 5$, given the separation distance of $s = 5\text{mm}$ and embedded crack of $a_e = 3\text{mm}$ with $c_e = 9\text{mm}$.

However, the exponential increase of $CTOD_s$ values was found for the high strain level $\varepsilon_g \geq 0.02$ under the combined loadings. For instance, at $\varepsilon_g = 0.03$, $CTOD_s$ value obtained from the case of $\frac{\sigma_H}{\sigma_Y} = 0.5$ was two times higher than that from the tension loading alone ($\frac{\sigma_H}{\sigma_Y} = 0$). This observation could perhaps be explained by the local deformation being enlarged by the combined loading to a certain degree as the plastic strain increases continuously, and subsequently, it leads to the crack opening largely.

On the other hand, the bending combined with the internal pressure (“B+P”) was also considered for different separation distances. For a given cracks configuration, that was, surface crack of $a_s = 5\text{mm}$ with $\frac{c_s}{a_s} = 3$ and embedded crack of $a_e = 3\text{mm}$ with $c_e = 9\text{mm}$, Figure 7-12 (a) and (b) present the curves of the $CTOD_s$ against global strain for the four different loadings, corresponding to $s = 5\text{mm}$ and $s = 3\text{mm}$, respectively.

Figure 7-12 Curves of the $CTOD_s$ versus strain for four different loadings: (a) $s = 5\text{mm}$ and (b) $s = 3\text{mm}$. When the surface crack of $a_s = 5\text{mm}$ with $\frac{c_s}{a_s} = 3$ and the embedded crack of $a_e = 3\text{mm}$ with $c_e = 3\text{mm}$ are considered.

When an additional internal pressure ($\frac{\sigma_H}{\sigma_Y} = 0.5$) was applied on the flawed pipeline, the $CTOD_s$ values were significantly augmented for two combined loadings at the same strain level, which was similar to the investigation discussed for Figure 7-11. In addition, it is observed from both figures in Figure 7-12 that the slope of the $CTOD_s$ -strain curve obtained from the tension loading alone was slightly larger than those from the bending loading alone, implying that the more severe deformation can be introduced by the tension load rather than the pure bending.

Furthermore, it was also found that at any global strain level, the $CTOD_s$ value from the tension load combined with the internal pressure ($\frac{\sigma_H}{\sigma_Y} = 0.5$) was much larger than that from the bending load combined with the internal pressure, indicating that the difference of $CTOD_s$ between the two combined loadings was significantly amplified due to the additional internal pressure imposed. In addition, given the tension load combined with the internal pressure ($\frac{\sigma_H}{\sigma_Y} = 0.5$), comparison of two figures in Figure 7-12 revealed that when the two coplanar cracks were located closer to each other ($s = 3mm$), even higher $CTOD_s$ values were produced due to stronger interaction of the two closer cracks, compared to the relatively weak interaction case of $s = 5mm$. In this respect, it could also be observed that the influence of crack separation, s , was much more significant compared to the effects of biaxial loadings. A quick comparison at $\varepsilon_g = 0.02$, the increase in $CTOD_s$ was at 0.5mm for all loads when s reduced from 5mm to 3mm compared to changing of loading that did not give a significant increase except for tension with internal pressure. Moreover, when comparing Figure 7-11 (b) and 7-12 for a reduction in crack separation, s , a wider internal crack ($a_e = 4mm$ with $c_e = 12mm$) caused a higher $CTOD_s$ value increased at $\varepsilon_g = 0.03$. This showed that the influence of s was high for a wider internal crack as compared to loading factors. In summary, all these observations demonstrated that the combined loadings can significantly affect the fracture response of the flawed pipelines, particularly at high global strain levels.

Additionally, Figure 7-13 (a) shows the $CTOD_s$ against the strain curves for semi-elliptical surface cracks with/without internal pressure of 15MPa. To set up the basis of comparison, firstly, flawed pipelines with various semi-elliptical surface cracks loaded with/without internal pressure

cases were done. It could be observed that the $CTOD_s$ values increased with the addition of hoop stress. At $\varepsilon_g = 0.03$ due to the presence of internal pressure, $CTOD_s$ increased by 1.18 times for surface depth of $a_s = 2mm$ and by 1.29 times for $a_s = 4mm$. This could be explained by the enhancement of the local deformation in the cracked pipeline. In other words, the local deformation could be enlarged by the combined loading to some extent as the plastic strain increased continuously. Subsequently, it led to the crack opening largely.

Figure 7-13 Curves of $CTOD_s$ against strain for various cracks with and without biaxial loading: (a) surface crack only and (b) fixed surface crack and various embedded crack

From Figure 7-13 (b), with a fixed surface crack of $a_s = 2mm$ and $\frac{c_s}{a_s} = 3$, different embedded cracks were modelled for the flawed pipelines. As expected, when the internal pressure was applied, the $CTOD_s$ values increased with the addition of hoop stress by approximately the same degree. For example, between with and without internal pressure at $\varepsilon_g = 0.03$, the difference in $CTOD_s$ for the case $a_e = 2mm$ was 25.3% while for $a_e = 4mm$ the difference was 26.0%. This confirmed that the combined biaxial loading has a significant effect on the fracture response of the flawed pipelines, particularly at high global strain. With reference to Figure 7-13 (b), it is of

interest to observe that for the wider embedded crack and short surface crack (both with and without internal pressure) $CTOD_s$ had a very low value for its 'Region 1'. This observation could be due to the shielding effect of the wider embedded crack that caused less plasticity onto the short surface crack and thus lower initial $CTOD_s$ values.

7.2.4 Dominant crack tip

In order to examine which crack tip produces the more significant CTOD value, an analysis was carried out for fracture response of multiple cracks with a fixed surface crack depth $a_s = 2mm$ and shape ratio $\frac{c_s}{a_s} = 3$ while the embedded crack was modelled with crack height $a_e = 2mm$ but varying shape ratio $\frac{c_e}{a_e}$ of 1, 3 and 5. The distance between the cracks was fixed at $s_n = 1$ for observable crack interaction. From Figure 7-14, $CTOD_R$ tends to be the lowest of the three CTOD values in general. For embedded crack shape ratio $\frac{c_e}{a_e} = 1$, $CTOD_s$ values were higher than the $CTOD_L$ values. Even for surface and embedded crack of the same shape ratio of 3, $CTOD_s$ had a higher value compared to $CTOD_L$. Therefore, the semi-elliptical surface crack $CTOD_s$ is the determining CTOD to be investigated for the interaction of two cracks when the surface crack is larger than the embedded crack. However, for larger embedded cracks (i.e, embedded crack shape ratio $\frac{c_e}{a_e} = 5$), embedded crack $CTOD_L$ values were higher than $CTOD_s$ and therefore, $CTOD_L$ is the determining CTOD instead.

Figure 7-14 Curves of CTOD against strain for different embedded crack shape ratio.

7.2.5 Influence of the crack separation distance

As the distance between the cracks plays a key role in the effects of crack interaction, the analysis with cracks modelled at various normalized crack distance ($s_n = 1.5, 1, 0.75, 0.5$) based on Equation 7-1 was investigated for the influence of crack separation distance. The pipe was modelled with a fixed surface crack with depth $a_s = 2\text{mm}$ and shape factor $\frac{c_s}{a_s} = 3$ and the

embedded crack with height $a_e = 2\text{mm}$ and shape factor $\frac{c_e}{a_e} = 3$. Based on the deduction from the previous Section 7.3.4, only $CTOD_s$ values obtained from the semi-elliptical surface crack were considered to investigate the influence of the separation distance. The plot of $CTOD_s$ versus strain for different normalized crack distances between the two coplanar cracks is presented in Figure 7-16. Generally, as normalized crack distance decreases, the CTOD against strain curve became steeper with higher $CTOD_s$ values at global strain, $\varepsilon_g = 0.03$.

Figure 7-15 Curves of $CTOD_s$ against strain for various crack distance

Specifically, at $\varepsilon_g = 0.03$, when the normalized separation distance, s_n , was shortened from 1 to 0.5, it caused a significant increase in the change in $CTOD_s$ values from 7.69% to 47.5%. However, a marginal difference of 3.2% between the case of surface crack ‘only’ and the case of $s_n = 1.5$ indicated that the effect of embedded crack on the fracture response of surface crack was minimal. This further substantiates the deductions made in the earlier part of the current chapter that the interaction between the cracks could be neglected when $s > (a_s + a_e)$. These results are consistent with BS7910:2013, which mentioned that for any $s_n > 1$, crack interaction can be

considered negligible and cracks are isolated from each other. This could also be seen in Figure 7-16 where the stresses mapped on to the FE elements in numerical model shows that for $s_n > 1$, the distance was sufficiently wide that did not cause much interaction and summation of stresses. The stress map on the surface crack for $s_n > 1$ looked similar to that for surface crack only case. Moreover, Pitt et al. [43] stated that significant crack interaction occurs at $s_n < 0.7$. It corresponded well with the results obtained for the case of $s_n = 1$ and $s_n = 0.75$ in which the difference against ‘only’ cases made up to 7.69% and 15.5% respectively which were relatively not as noteworthy as $s_n = 0.5$ where the difference was at 47.5%. The visualization can be seen in Figure 7-16 where the stresses increases between the area of separation for the two cracks with highest stresses observed for $s_n = 0.5$. Thus, the interaction effects were more pronounced for normalized distance $s_n \leq 1$. To study the effects of other parameters, in the following subsequent cases, all crack separation distance was kept consistent at $s_n = 1$ as it still provides sufficiently significant interaction effects.

Surface only

Figure 7-16 Curves of $CTOD_s$ against strain for different embedded crack height and shape ratio

7.2.6 Influence of the embedded crack onto the surface crack

To observe the effect of the embedded crack onto the surface crack, the results of surface crack $CTOD_s$ are discussed here. Figure 7-17 shows the plot of $CTOD_s$ curves for a given fixed surface crack with depth of $a_s = 2mm$, $\frac{c_s}{a_s} = 3$ and $s_n = 1$ with varying surface crack parameters.

It was observed that the curves of $CTOD_s$ -strain become steeper with the presence of embedded cracks than the ‘only’ case for global strain, $\epsilon_g > 0.01$. Presence of the embedded crack with critical distance, $s_n = 1$ caused an increase of up to 25% in the value of $CTOD_s$ at $\epsilon_g = 0.03$. Additionally, the curve of $CTOD_s$ -strain tended to become steeper with the increasing embedded crack height, a_e . However, it was noted that for the embedded crack of height $a_e = 4mm$ with $\frac{c_e}{a_e} = 3$, the surface crack $CTOD_s$ did not show the expected increasing trend at lower global strain

values of less than 1%, compared to the other cases. This could possibly be due to the shielding effect of the large embedded crack on the relatively smaller surface crack until a certain point before 1% global strain where the surface crack started blunting up at a constant steep gradient with increasing strain.

Figure 7-17 Curves of $CTOD_s$ against strain for different embedded crack height and shape ratio

Apart from the embedded crack height, a_e , parameter, shape ratio, $\frac{c_e}{a_e}$, has an effect in the crack interactions. Figure 7-17 (b) also shows that for the fixed surface crack and an embedded crack of fixed height of $a_e = 2$ mm, the $CTOD_s$ -strain curves became steeper as $\frac{c_e}{a_e}$ increases from 1 to 5.

Moreover, at $\varepsilon_g = 0.03$, the difference in $CTOD_s$ between cases with embedded crack of $\frac{c_e}{a_e} = 1$ and $\frac{c_e}{a_e} = 3$ was larger by 32.4% than the difference in $CTOD_s$ for cases with embedded crack of $\frac{c_e}{a_e} = 3$ and $\frac{c_e}{a_e} = 5$. This illustrated that increasing the shape factor, $\frac{c_e}{a_e}$, the increase in $CTOD_s$ values become smaller. However, when comparing Figure 7-17 (a) and (b), this shape factor, $\frac{c_e}{a_e}$, played a more significant role compared to the embedded crack height, a_e , in affecting the $CTOD_s$ values due to crack interactions.

To encapsulate these 2 parameters, Figure 7-17 (c) showed the curves of $CTOD_s$ with varying embedded crack size. Generally, increasing the embedded crack size caused steeper $CTOD_s$ -strain curves. At lower global strain $\varepsilon_g < 0.01$, there was no significant effect of embedded crack parameters on the $CTOD_s$ -strain curves. For embedded cracks size, relatively smaller than the surface crack, the difference in $CTOD_s$ values at $\varepsilon_g = 0.03$ compared to the case without embedded crack was at most 3% higher which was considered negligible. The maximum increase for these cases was up to 24.8% at $\varepsilon_g = 0.03$ for embedded crack of $a_e = 4$ and $\frac{c_e}{a_e} = 5$. Also, 'Region 1' range increased as the embedded crack size increased. This could be due to the surface crack undergoes larger strain before crack ligaments becomes fully plastic.

7.2.7 Influence of the surface crack onto the embedded crack

To further understand the interactions between the two coplanar cracks, more simulations of the flawed pipeline with different embedded crack sizes were also carried out while the surface crack size was fixed. The CTOD of the embedded cracks was observed for surface crack fixed at $a_s = 2$

and of $\frac{c_s}{a_s} = 3$ and $s_n = 1$ with varying embedded cracks. Observing Figure 7-18 (a) and (b), with the embedded crack height, a_e , changed from 1 to 2mm, both $CTOD_R$ -strain curves did not show any visible changes, while the $CTOD_L$ -strain curves became steeper with the presence of the surface crack. However, in Figure 7-18 (c), with a larger embedded crack of height $a_e = 4mm$, $CTOD_L$ -strain curve did not show any visible changes while $CTOD_R$ -strain curve had a slight decrease in gradient with the presence of the surface crack. For this case, $CTOD_R$ was higher than $CTOD_L$ as the right crack tip was nearer to the inner pipe walls compared to the other 2 cases; this was also observed earlier in Figure 7-18 with the embedded crack “only” case with similar parameters. Due to the relatively large embedded crack against the smaller surface crack, the CTODs of the embedded crack in this case were not affected by the presence of the surface crack. Since the $CTOD_L$ was observed to have visible effects for most cases, only results from the embedded crack $CTOD_L$ will be further discussed.

Figure 7-18 Curves of CTOD against strain (a) - (c) comparing with surface and 'only' cases for different embedded crack height and (d) comparing with difference surface crack depth and crack ratio

Consequently, Figure 7-18 (d) shows the $CTOD_L$ -strain curves with a fixed embedded crack for various surface cracks. In contrast, it was observed that the effects of surface crack parameters do not cause the embedded crack $CTOD_L$ values to increase as expected. This can be seen at 3% strain for the cases with $a_s = 2\text{mm}$ with $\frac{c_s}{a_s} = 1$ (-2.9% difference) and $a_s = 4\text{mm}$ with $\frac{c_s}{a_s} = 1$ (-3.5% difference) when compared to the cases $a_s = 1\text{mm}$ with $\frac{c_s}{a_s} = 1$. Only the cases with surface

crack of $a_s = 4mm$ with $\frac{c_s}{a_s} = 3$ and $\frac{c_s}{a_s} = 5$ had a positive difference when compared to $a_s = 1mm$, $\frac{c_s}{a_s} = 1$. For the rest crack cases simulated, they had slight marginal difference and could be taken as like the case of $a_s = 1mm$, $\frac{c_s}{a_s} = 1$. In summary for Sections 7.2.6 and 7.2.7, it can be reasoned that the effects of the surface crack parameters onto the embedded crack CTOD are not as strong as those compared to the influence of embedded crack parameters onto the surface crack CTOD.

7.2.8 Comparison with the Single Equivalent Crack Model of BS7910:2013

It was highlighted that a conservative CTOD estimation was obtained based on the BS7910:2013 combination rule of effective flaw dimensions after interaction. BS7910:2013 re-characterizes the two coplanar cracks (one semi-elliptical surface crack and one elliptical embedded crack) into one effective semi-elliptical surface crack using effective flaw dimensions. When the normalized separation distance $s_n \leq 1$, the effective crack dimensions of the large semi-elliptical crack with reference to Figure 2-4 can be taken as

$$2c_{eff} = \max\{2c_s, 2c_e\} \quad (7-2)$$

$$a_{eff} = 2a_e + a_s + s \quad (7-3)$$

As such, to investigate on how conservative this combination rule adds to estimation of the CTOD value, the re-characterized cracks based on Equation (7-2) and Equation (7-3) were modelled and compared to the two coplanar crack cases.

To begin with, the numerical results for various embedded cracks with a fixed surface crack of $a_s = 2mm$ and $\frac{c_s}{a_s} = 3$ were plotted against the computational simulation results of their respective

equivalent semi-elliptical surface crack in Figure 7-19. It could be interpreted that the re-characterized effective crack CTOD value was always larger than the numerical results for the two coplanar crack cases for any given strain, at least 1.25 times larger than the dominant CTOD value at $\varepsilon_g = 0.03$. At $\varepsilon_g = 0.03$, for small embedded cracks as shown in Figure 7-19 (a), (b) & (d), the dominant CTOD values were always lower compared to the re-characterized effective crack CTOD values. The re-characterized effect crack CTOD values could be as much as 1.5 times higher than the dominant CTOD values. On the other hand, for the CTOD-strain curves for large multiple cracks involving embedded crack as presented in Figure 7-19 (c) & (e), re-characterized effective crack CTOD value compared to dominant *CTOD* values was at least twice as high at $\varepsilon_g = 0.03$. These observations indicated that using the combination rule from BS7910:2013 produces conservative predictions of CTOD values for multiple cracks fracture assessment, especially for the cases of large multiple cracks.

Figure 7-19 Curves of CTOD against strain for various comparing embedded cracks with a fixed surface crack of $a_s = 2\text{mm}$ and $\frac{c}{a} = 3$ and their 'equivalent' effective semi-elliptical surface crack, (a) & (b) embedded curves shows $CTOD_L$.

Next, the influence of crack distance on the conservativeness of combination rule was investigated. By having fixed surface crack $a_s = 2 \text{ mm}$, $c_s/a_s = 3$ and embedded crack $2a_e = 4 \text{ mm}$, $c_e/a_e = 3$, the $CTOD_s$ -strain curves for various normalized crack separation distance ($s_n = 1.5, 1, 0.75, 0.5$) were plotted against the simulation result of their respective equivalent re-characterized effective semi-elliptical surface crack in Figure 7-20.

Figure 7-20 Curves of $CTOD_s$ against strain for various crack distances and their 'equivalent' effective semi-elliptical surface crack

Similarly, the $CTOD_s$ -strain curves for the case of a single surface crack (without the embedded crack) and the equivalent re-characterized semi-elliptical surface crack for the case of crack distance $s_n = 0$ were plotted in the same figures to give another basis of comparison. The difference in $CTOD_s$ values between the two collinear cracks and equivalent re-characterized effective semi-elliptical surface crack were relatively similar for both Figure 7-20 (a) and (b) (around 50% difference) at 3% global strain. However, in Figure 7-20 (d), as the distance was decreased to $s_n = 0.5$, the difference decreased to 4% at $\epsilon_g = 0.03$. Moreover, at less than 1% global strain for $s_n = 0.5$, both $CTOD$ -strain curves trends were comparable, the re-characterized effective crack can be taken as a good approximation to simulate collinear cracks. Adding to that for the same case, the $CTOD_s$ -strain curve of the two collinear cracks were relatively similar to the $CTOD$ -strain curve of the effective surface crack for the case of $s_n = 0$. The current result shows that as the normalized crack distance decreases to $s_n \leq 0.5$, this combination rule is able to account for the interaction effect as it assumes that the cracks coalesce. To understand these observations, the stress maps for the surface crack ‘only’ and effective surface crack for $s_n = 0$ were compared with the embedded cracks for $s_n = 0.5$ in Figure 7-21. It has already been commented earlier in section 7.2.5 on increase in the stress interactions due to the crack tips as the crack separation distance decreases. Hence, due to the high crack interactions for $s_n = 0.5$, the $CTOD_s$ for surface and embedded crack diverges away from surface crack ‘only’ and converges towards the case for effective surface crack for $s_n = 0$ as the stresses between the separation area increases and effectively affect the $CTOD_s$.

These observations indicated that the usage of BS7910:2013 combination rule produces good agreement of CTOD values for multiple cracks fracture assessment when the normalized crack distance $s_n \leq 0.5$. Hence, BS7910:2013 combination rule can still provide a good estimation of CTOD values for multiple cracks fracture assessment when the normalized cracks distance $s_n \leq 0.5$.

Surface Crack 'Only'

Surface and embedded crack with $s_n = 0.5$

Effective surface crack for $s_n = 0$

Figure 7-21 Curves of $CTOD_s$ against strain for various crack distances and their 'equivalent' effective semi-elliptical surface crack

7.2.9 Comments on CIC cases and multiple crack cases

Interestingly, in section 7.2.3, the response to tensile only loading was greater than for bending only. This is the opposite of what was presented in Chapter 6 where bending is more influential in the crack opening displacement. This could be explained due to the localised softening effect of the corroded ligament on the effective yield stress. As such, the ovalisation of the corroded ligament due to bending causes stresses in the hoop direction. Therefore, at higher strains with the softening effects on the crack tip plasticity of the corroded ligament, these bending stresses from ovalisation of the pipe are then translated to tensile stresses which causes the response for bending only to be greater than tensile only for CIC cases. The results demonstrated that these CIC cases are more critical under pure bending compared to tension while multiple cracks are more critical under pure tension. CIC cases may be important to take note of in pipelaying operations like reeling while multiple cracks are important to consider during operations for example elongation of pipeline due to thermal expansion.

7.3 Proposed Strain-based CTOD estimation scheme

Based on our numerical simulation results, the strain-based CTOD estimation scheme is proposed for the fracture assessment of offshore pipelines with multiple cracks. As this is a proposed example of such a strain-based CTOD estimation scheme, it is limited to the cases mentioned and for X65 steel materials with strain hardening exponent of $n = 0.05$. This is to show that the 3-D numerical solutions can provide the needed fracture estimation responses for use in industrial settings either through numerical modelling of the cases itself or for design/codes/maintenance purposes provide an estimation scheme.

A new strain-based CTOD estimation scheme is constructed for surface cracks. As it is noticed that most of *CTOD*-strain curves appear non-linear due to the strong interaction of the two close cracks, and thus the second order polynomial regression is adopted to characterize the normalized *CTOD* (represented by $\frac{CTOD}{t}$). The expression of $\frac{CTOD}{t}$ is written as below

$$\frac{CTOD}{t} = A_0(a_e, c_e, s)\varepsilon_g^2 + A_1(a_e, c_e, s)\varepsilon_g + A_2(a_e, c_e, s) \quad (7-4)$$

Where A_0 , A_1 and A_2 are regression coefficients, depending on the crack configurations and loadings. Here the non-zero global strain is considered, i.e., $\varepsilon_g > 0$. This equation could be converted into linear form with the following transformations in order to estimate the regression coefficients from the least square method [138]:

$$Y = A_0X_0 + A_1X_1 + A_2 \quad (7-5)$$

in which $Y = CTOD/t$, $X_0 = \varepsilon_g^2$, $X_1 = \varepsilon_g$. The method of least squares estimates the coefficients, which minimize the sum of squared deviations between the fitted and actual $\frac{CTOD}{t}$ values. The best

estimate of the coefficients is the value minimizing the residual sum of squares in the regression model to bring the curve close to the data points. For the present numerical results, the value of R^2 (coefficient of determination) provided by Equation 7-5 is greater than 0.99 for all cases. It indicates that the proposed CTOD estimation scheme is reasonably accurate and reliable for these specific multiple cracks configurations considered with a predicted error limit of $\pm 1\%$. With the varying single surface crack depth and shape ratio, the values of A_0 , A_1 and A_2 in Equation 7-5 for surface crack CTOD_s under tensile with and without internal pressure are listed in Table 7-1.

Table 7-1 The values of A_0 , A_1 and A_2 in Equation 3-5 for surface cracks under tensile and biaxial loading

a_s (mm)		T			T+IP		
		A_0	A_1	A_2	A_0	A_1	A_2
1	$\frac{c_s}{a_s}$						
	1	0.63637	0.06817	0.00017	0.38255	0.09033	-0.00020
	3	-0.50732	0.14537	-0.00018	-0.79329	0.18471	-0.00016
	5	-0.69958	0.17577	-0.00023	-0.99691	0.22479	-0.00015
2	$\frac{c_s}{a_s}$						
	1	-0.60282	0.16617	-0.00016	-0.74996	0.20093	-0.00019
	3	-1.24340	0.32282	-0.00010	-1.31730	0.37405	-0.00004
	5	-1.17050	0.33151	-0.00001	-1.40320	0.43285	0.00001
3	$\frac{c_s}{a_s}$						
	1	-1.26180	0.33434	-0.00018	-0.77779	0.36501	-0.00022
	3	-0.53934	0.60571	0.00003	0.17593	0.76482	-0.00011
	5	-0.08195	0.67646	0.00010	-0.98831	0.85963	-0.00002

The values of A_0 , A_1 and A_2 for the embedded crack, CTOD_L and CTOD_R, under tensile with and without internal pressure are listed in Table 7-2.

Table 7-2 The values of A_0 , A_1 and A_2 in Equation 3-5 for embedded cracks under tensile and biaxial loading

a_s (mm)		CTOD_L			CTOD_R		
		A_0	A_1	A_2	A_0	A_1	A_2
1	$\frac{c_e}{a_e}$	T					
	$\frac{c_e}{a_e}$						
	1	1.52320	0.05237	-0.00020	1.48950	0.05272	-0.00020
	3	-0.79577	0.16618	-0.00016	-0.80550	0.16646	-0.00019
2	$\frac{c_e}{a_e}$	T					
	$\frac{c_e}{a_e}$						
	1	-0.76437	0.30083	-0.00010	-0.72100	0.29730	-0.00011
	3	-0.83379	0.33543	0.00003	-0.76659	0.32921	0.00001
4	$\frac{c_e}{a_e}$	T					
	$\frac{c_e}{a_e}$						
	1	-0.28171	0.65500	0.00007	-1.38600	0.76772	0.00006
	3	0.45507	0.84063	0.00002	-0.17959	0.98062	0.00003
1	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016
2	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016
4	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016
1	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016
2	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016
4	$\frac{c_e}{a_e}$	T + IP					
	$\frac{c_e}{a_e}$						
	1	-0.00638	0.10642	-0.00024	-0.05510	0.10665	-0.00023
	3	-0.85091	0.20813	-0.00016	-0.82864	0.20621	-0.00016

When the embedded crack of $a_e = 3mm$ and $c_e = 9mm$ remains constant, the values of A_0 , A_1 and A_2 in Equation 7-5 for surface cracks under tensile and bending loading conditions are listed in Table 7-3.

Table 7-3 The values of A_0 , A_1 and A_2 in Equation 7-5 for surface cracks under tensile and bending loading conditions when the embedded crack of $a_e = 3\text{mm}$ and $c_e = 9\text{mm}$ remains constant

$a_s (\text{mm})$	$\frac{c_s}{a_s}$	T			B		
		A_0	A_1	A_2	A_0	A_1	A_2
3	$s=9\text{mm}$						
	1	-1.5000	0.2858	-0.0004	-1.5530	0.2770	-0.0004
	3	0.1760	0.4157	0.0003	-1.8466	0.4375	-0.0001
	5	0.2003	0.4683	0.0002	-1.2079	0.4500	0.0001
	$s=6\text{mm}$						
	1	-0.3174	0.2620	0.0000	-1.4141	0.2981	-0.0004
	3	-1.6330	0.5293	-0.0005	-0.9596	0.4524	0.0000
	5	0.2959	0.5077	0.0002	-0.6524	0.4807	0.0001
	$s=7\text{mm}$						
5	1	-0.5723	0.4395	-0.0001	-0.6355	0.4484	-0.0003
	3	0.1924	0.9029	-0.0001	-0.7005	0.8488	-0.0004
	5	1.5710	1.0081	0.0004	-0.2616	0.9627	-0.0003
	$s=5\text{mm}$						
	1	0.3042	0.4362	0.0002	-0.0253	0.4758	-0.0003
	3	0.5295	0.9494	0.0000	-0.1856	0.9060	-0.0004
	5	1.9203	1.0704	0.0004	0.2352	1.0118	-0.0003
	$s=5\text{mm}$						
	1	0.9348	0.6367	0.0000	-3.0261	0.7558	-0.0008
7	3	1.7485	1.6661	-0.0001	2.9529	1.5225	-0.0010
	5	11.4618	1.9155	0.0017	10.8011	1.7385	0.0001
	$s=3\text{mm}$						
	1	2.2113	0.7296	-0.0001	10.5051	0.5771	0.0002
	3	-1.4152	2.7526	-0.0025	6.5428	2.4888	-0.0039
	5	0.5006	3.7443	-0.0015	-2.1364	3.4705	-0.0046

In addition, a limited number of embedded crack sizes are also considered to investigate the crack interaction effect on the evolution of $CTOD_s$. The values of A_0 , A_1 and A_2 for those variations are listed in Table 7-4 while the surface crack of $a_s = 5mm$ with $\frac{c_s}{a_s} = 3$.

Table 7-4 The values of A_0 , A_1 and A_2 in Equation 3-5 for the three different embedded cracks while retaining the surface crack of $a_s = 3mm$ and $\frac{c_s}{a_s} = 3$

a_e (mm)	c_e (mm)	s (mm)	T			B		
			A_0	A_1	A_2	A_0	A_1	A_2
2	6	9	-1.6019	0.7696	0.0002	-4.6928	0.8088	-0.0002
		7	-1.9544	0.7881	0.0002	-4.9043	0.8243	-0.0002
3	9	7	0.1924	0.9029	-0.0001	-0.7005	0.8488	-0.0004
		5	0.5295	0.9494	0.0000	-0.1856	0.9060	-0.0004
4	12	5	-0.3319	0.8285	0.0003	-4.3847	0.8892	-0.0003
		3	1.8029	1.6000	-0.0017	-1.2795	1.5904	-0.0021

In addition, strain-based CTOD estimation methods are constructed when both surface and embedded cracks exist and interact. The expressions of normalized CTOD ($\frac{CTOD}{t}$) for the two coplanar cracks (embedded and surface crack respectively) are written as follows:

$$\frac{CTOD}{t}_e = A'_0(a_s, c_s, s)\varepsilon_g^2 + A'_1(a_s, c_s, s)\varepsilon_g + A'_2(a_s, c_s, s), \quad (7-6)$$

$$\frac{CTOD}{t}_s = A'_0(a_e, c_e, s)\varepsilon_g^2 + A'_1(a_e, c_e, s)\varepsilon_g + A'_2(a_e, c_e, s). \quad (7-7)$$

Where A'_0 , A'_1 and A'_2 are regression coefficients, which depend on the crack configurations, loadings and distance between the cracks. For the present numerical results, the value of R^2 (coefficient of determination) provided by Equation 7-6 and Equation 7-7 is around 0.99 for all cases. This indicates that the proposed CTOD estimation method is reasonably accurate and

reliable (within an error limit of $\pm 1\%$) for these specific multiple cracks configurations considered.

When the surface crack is fixed at $a_s = 2mm$ and $c_s = 6mm$, the values of A'_0, A'_1 and A'_2 in Equation 7-6 and Equation 7-7 for both surface and embedded cracks under tensile strain and with/without internal pressure are listed in Table 7-5. While in Table 7-6, the regression coefficient values are given when the embedded crack is fixed at $a_e = 2mm$ and $c_e = 6mm$, Furthermore, the values of A'_0, A'_1 and A'_2 are provided in Table 7-7 for various normalized separation distance, $s_n = 1.5, 0.75, 0.5$. In this table, the data for fixed surface crack is $a_s = 2mm$ and $c_s = 6mm$, and the embedded crack with $a_e = 2mm$ and $c_e = 6mm$. The cracks are under tension and biaxial loading, respectively.

Table 7-5 The values of A_0 , A_1 and A_2 in Equation 7-6 & 7-7 for the three crack tips under tensile and biaxial loading ($\frac{\sigma_H}{\sigma_Y} = 0.3$) conditions when the surface crack is fixed at $a_s = 2\text{mm}$ and $c_s = 6\text{mm}$ but varying embedded crack

a_s (mm)	CTOD_S			CTOD_L			CTOD_R			
	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	
1 $\frac{s_n = 1}{\frac{c_e}{a_e}}$	T									
	1	-1.32340	0.32746	-0.00012	0.53843	0.08437	-0.00025	0.69584	0.08019	-0.00025
	3	-1.25850	0.33068	-0.00009	-0.73659	0.16698	-0.00015	-0.60371	0.15872	-0.00015
2 $\frac{s_n = 1}{\frac{c_e}{a_e}}$	T									
	1	-1.30370	0.33070	-0.00009	-0.81799	0.18985	-1.30370	-0.75749	0.17983	-0.00027
	3	-1.17540	0.34164	-0.00007	-0.70803	0.30530	-1.17540	-0.61069	0.29135	-0.00009
4 $\frac{s_n = 1}{\frac{c_e}{a_e}}$	5	-0.98309	0.34490	0.00001	-0.74939	0.34238	-0.98309	-0.63418	0.32401	0.00004
	T									
	1	-1.02240	0.32575	-0.00007	-0.93945	0.33025	-0.00018	-1.14660	0.34452	-0.00014
	3	-0.43611	0.35361	-0.00071	-0.81810	0.67880	-0.00030	-1.40660	0.75285	0.00009
	5	1.26290	0.31412	-0.00002	5.14380	0.72291	0.00014	8.07290	0.75051	0.00029

a_s (mm)		CTOD_S			CTOD_L			CTOD_R		
		A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2
1	$s_n = 1$									
	$\frac{c_e}{a_e}$	T + IP								
	1	-1.17980	0.39720	-0.00001	0.35717	0.09886	-0.00028	0.44606	0.09824	-0.00028
2	3	-1.08370	0.39926	0.00000	-0.76167	0.20605	-0.00017	-0.63742	0.19865	-0.00016
	$s_n = 1$									
	$\frac{c_e}{a_e}$	T + IP								
4	1	-1.38140	0.40832	-0.00003	-0.61097	0.20423	-0.00024	-0.55820	0.19225	-0.00019
	3	-0.90710	0.40846	0.00002	-0.63673	0.37864	0.00001	-0.57901	0.36287	0.00001
	5	-0.44466	0.40959	0.00004	-0.71080	0.45895	0.00008	-0.63517	0.43627	0.00007
4	$s_n = 1$									
	$\frac{c_e}{a_e}$	T + IP								
	1	-1.25830	0.41067	-0.00003	-0.19968	0.33865	-0.00009	-0.61350	0.35623	-0.00003
4	3	-0.24012	0.42189	-0.00039	-0.21391	0.90499	-0.00014	-2.23910	1.05320	-0.00013

a_s (mm)		CTOD_S			CTOD_L			CTOD_R		
		A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2
2	$s_n = 0.5$									
	$\frac{c_e}{a_e}$					T				
	1	-1.23580	0.34434	-0.00008	-0.61002	0.21057	-0.00022	-0.65892	0.17835	-0.00016
4	5	0.70754	0.45130	0.00013	0.58943	0.42520	0.00015	-0.53361	0.33844	0.00005
	$s_n = 0.5$									
	$\frac{c_e}{a_e}$					T				
	1	-1.05900	0.36483	-0.00003	-1.01590	0.35374	-0.00011	-0.95478	0.32608	0.00018
	3	1.26780	0.51747	-0.00055	1.38030	0.69910	0.00012	0.10101	0.61196	0.00020
2	$s_n = 0.5$									
	$\frac{c_e}{a_e}$					T + IP				
	1	-0.87690	0.40736	-0.00002	0.01587	0.22099	-0.00025	-0.43939	0.19501	-0.00018
4	5	2.94440	0.53996	0.00009	1.34210	0.56687	0.00010	-0.25048	0.44561	0.00006
	$s_n = 0.5$									
	$\frac{c_e}{a_e}$					T + IP				
	1	-0.46326	0.42144	0.00002	-0.06588	0.36012	-0.00006	-0.11049	0.33264	-0.00011
	3	5.89650	0.56712	-0.00056	4.30050	0.93392	-0.00004	0.63590	0.85154	-0.00007

Table 7-6 The values of A_0 , A_1 and A_2 in Equation 7-6 & 7-7 for the three crack tips under tensile and biaxial loading ($\frac{\sigma_H}{\sigma_Y} = 0.3$) conditions when the embedded crack is fixed at $a_e = 2\text{mm}$ and $c_e = 6\text{mm}$ but varying surface crack

$a_s (\text{mm})$		CTOD_S			CTOD_L			CTOD_R		
		A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2
1	$s_n = 1$ $\frac{c_e}{a_e}$	T								
	1	0.09357	0.10240	-0.00021	-0.87279	0.31087	-0.00006	-0.77140	0.29880	-0.00010
	3	-0.71710	0.17600	-0.00012	-0.85716	0.31195	-0.00006	-0.74001	0.29741	-0.00010
2	$s_n = 1$ $\frac{c_e}{a_e}$	T								
	1	-0.67000	0.19556	-0.00015	-0.68759	0.29905	-0.00010	-0.63398	0.28998	-0.00010
	5	-1.81040	0.39040	-0.00016	-0.75879	0.31052	-0.00013	-0.74067	0.29728	-0.00014
4	$s_n = 1$ $\frac{c_e}{a_e}$	T								
	1	-1.08980	0.36450	-0.00014	-0.53795	0.29145	-0.00009	-0.61955	0.28889	-0.00007
	3	-0.38442	0.64920	0.00001	-0.33371	0.30169	-0.00002	-0.46009	0.28226	0.00000
	5	-7.06690	0.85681	-0.00018	-0.32157	0.31321	-0.00007	-0.54223	0.28750	-0.00003

a_s (mm)		CTOD_S			CTOD_L			CTOD_R		
		A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2
1	$s_n = 1$ $\frac{c_e}{a_e}$	T + IP								
	1	-0.00068	0.11823	-0.00026	-0.95268	0.39207	0.00004	-0.77918	0.37164	0.00000
	3	-0.71988	0.21045	-0.00013	-0.89294	0.39185	0.00003	-0.75434	0.37064	0.00000
2	$s_n = 1$ $\frac{c_e}{a_e}$	T + IP								
	1	-0.15523	0.21465	-0.00017	-0.95709	0.37974	-0.00001	-0.79725	0.36423	-0.00001
	5	-0.96150	0.46919	0.00002	-0.62420	0.38044	0.00001	-0.66298	0.36344	0.00000
4	$s_n = 1$ $\frac{c_e}{a_e}$	T + IP								
	1	0.13225	0.37277	-0.00007	-0.45848	0.36259	0.00000	-0.68257	0.36365	0.00002
	3	0.50127	0.81545	-0.00016	0.03166	0.36954	0.00001	-0.46025	0.35504	0.00004
	5	3.08840	0.92303	-0.00008	0.63154	0.37392	0.00003	0.11686	0.34875	0.00007

Table 7-7 The values of A'_0 , A'_1 and A'_2 in Equation 7-6 & 7-7 for the three crack tips under tensile and biaxial loading ($\frac{\sigma_H}{\sigma_Y} = 0.3$) conditions with varying normalized separation distance

For embedded crack: $a_e = 2 \text{ mm}$ $c_e / a_e = 3$				For surface crack: $a_s = 2 \text{ mm}$ $c_s / a_s = 3$					
$a_s \text{ (mm)}$	CTOD_S			CTOD_L			CTOD_R		
	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2	A'_0	A'_1	A'_2
s_n	T								
1.5	-1.60480	0.35157	-0.00037	-0.57712	0.29084	-0.00010	-0.62186	0.28745	-0.00009
0.75	-1.17180	0.36105	-0.00002	-0.83283	0.31946	-0.00006	-0.59423	0.29538	-0.00008
0.5	-0.20548	0.41969	0.00005	-1.00540	0.41818	-0.00042	-0.55699	0.30645	-0.00006
s_n	T + IP								
1.5	-0.78276	0.39396	0.00001	-0.50310	0.36311	0.00000	-0.58926	0.35754	0.00000
0.75	-0.52938	0.42264	0.00004	-0.53059	0.38656	0.00003	-0.53412	0.36736	0.00000
0.5	0.76888	0.50222	0.00005	0.79564	0.45526	0.00007	-0.27638	0.37562	0.00003

7.3.1 Proposed flowchart for the CTOD estimation of the two coplanar cracks

With the stated numerical results in this Chapter, a flowchart of the CTOD estimation for 2 coplanar cracks in offshore pipelines is proposed. In general, to conduct 3-D numerical simulations is expensive and time consuming, strain-based CTOD estimation method together with this flowchart provide a better estimation procedure for fracture assessment. The flowchart in Figure 7-22 gives a clear outline on the estimation procedure using strain-based CTOD values for two interacting coplanar cracks with both surface and elliptical crack shape $\frac{c}{a} > 1$ and for more than 1% global strain. The flowchart is built upon the results of strain-based CTOD values for the coplanar crack configurations. It also takes into consideration the combination rule of BS7910:2013. Figure 7-22 starts with the normalized distance between the two cracks and provides three different paths based on the value of the normalized crack distance to estimate the CTOD values at the crack tips. However, it should be emphasized that the analyses on the two interacting cracks are carried out considering $D = 400mm$ and $D/t = 20$ (which are the average values used in engineering practices). Thus, the proposed flowchart and formulation is valid for this specific case only. They cannot be used for very thin-wall or thick-wall pipelines since the interaction of multiple cracks in these situations may be considerably different from the present cases.

Flowchart for CTOD estimation for 2 collinear cracks

(for both crack aspect ratio $c/a > 1$ & more than 1% strain)

Figure 7-22 Flow chart for CTOD estimation for 2 coplanar cracks

7.3.2 Example cases using the proposed flowchart and estimation scheme

To verify the proposed CTOD estimation flowchart and scheme, Table 7-8 presents four sample numerical cases that were conducted which were not part of the data provided in the estimation schemes. The proposed estimation schemes were checked by those data from the independent numerical calculations.

Table 7-8 Four different cases with various surface and embedded crack depth, aspect ratio and normalized crack separation distance to verify the proposed estimation scheme.

Case	a_s (mm)	$\frac{c_s}{a_s}$	a_e (mm)	$\frac{c_e}{a_e}$	s_n
1	1	8	1	8	1/3
2	2	2	3	3	1
3	4	4	2	3	1
4	4	4	3	4	5/4

The flowchart framework was applied and interpolations were done from the proposed estimation schemes to calculate the estimated regression coefficients for the determining the CTOD for these cases. The curves and comparison of fit for the numerical simulation data and the proposed estimation scheme fit are given in Figure 7-23. The best estimate is the value minimizing the residual sum of squares in the regression model to bring the curve close to the data points. As can be seen in Figure 7-23, the value of R-squared R^2 (coefficient of determination) was greater than 0.95 for the four cases. In addition, based on the Root Mean Square Error (RMSE), it indicated that the proposed CTOD estimation scheme was reasonably accurate and reliable to predict the

CTOD response for these specific multiple crack configurations considered with a predicted error limit of $\pm 5\%$. This provided good confidence in the proposed CTOD estimation flowchart and scheme that can be utilized for practical engineering applications.

Figure 7-23 Curves of CTOD against the applied strain for the four sample cases: comparing the numerical results with those from the proposed estimation scheme.

Next, these curves of CTOD against strain can be checked for failure by applying the CTOD fracture criterion with the assumed critical CTOD of 0.45mm. For tension cases shown in Figure 7.23 (a) and (b), the structure did not meet the CTOD fracture criterion even at 3% strain; these crack configurations were considered safe for the various loadings (tension and/or internal

pressure) applied. However, from Figure 7.23 (c) and (d), the CTODs of both crack configurations for the case of biaxial loading met the CTOD fracture criterion at around 2.25% strain; structural failure was to be expected here for the pipeline and loading should not exceed this strain level.

Therefore, by knowing the crack dimensions and its loading with this proposed strain-based CTOD estimation scheme, the CTOD fracture criterion can be applied to check for acceptable strain loading conditions.

7.4 Concluding Remarks

In this chapter, extensive 3-D finite element simulations had been performed on the nonlinear elastic-plastic fracture behaviors of two interacting coplanar cracks in offshore pipelines subjected to various combined loadings. The influences of the crack configuration, the separation distance between the two cracks, and the pipe internal pressure on the fracture response (represented by *CTOD*) were investigated in detail. From the numerical results and graphs generated in this chapter, the major work done was concluded as follows.

A parametric study on the single crack case was conducted. The crack ratio is a parameter in the crack-blunting phenomenon. As the crack location becomes deeper, the effects of crack ratio are more pronounced. Crack shape ratio effect is more pronounced in deeper cracks compared to shallow ones.

The effect of the two-crack separation distance on the fracture behaviour of the pipeline had been investigated. These results are consistent with BS7910:2013 when the crack distance is relatively

large ($s_n > 1$), where the crack interaction is considered negligible. For a given separation distance, the semi-elliptical surface crack $CTOD_s$ is the determining parameter. It is found that the surface crack is much more seriously influenced by the embedded crack.

Four different loading conditions generally experienced by the offshore pipelines were considered to investigate their corresponding fracture responses. The results indicate that the most severe fracture response can be produced by the tension load combined with high internal pressure ($\frac{\sigma_H}{\sigma_Y} = 0.5$). Given a certain crack separation distance, the parametric studies on the effect of the internal pressure show that under the combined loadings, the linear increasing trend of all $CTOD_s$ -strain curves is observed for $\varepsilon_g < 0.02$, while the exponential increase of $CTOD_s$ value is observed for the high strain level ($\varepsilon_g \geq 0.02$).

Using CTOD fracture criterion, the current study shows that the BS7910:2013 re-characterization on the interacting cracks is highly conservative for multiple cracks fracture assessment, especially for the cases when the crack sizes are relatively large. However, additional observations indicate that the usage of BS7910:2013 combination rule yields good agreement of CTOD values for multiple cracks fracture assessment when the normalized cracks distance $s_n \leq 0.5$. Hence with the numerical results obtained, new strain-based CTOD estimation schemes are proposed, where the CTOD is defined as a function of global strain. It can be utilized to assess the fracture behavior of flawed pipelines with multiple cracks for engineering reference. Additionally, we build a flowchart for the CTOD estimation procedure of the two coplanar cracks with both shape factor $\frac{c}{a} > 1$ and the global strain larger than 1%. The validations on the CTOD estimation flowchart and schemes

are performed with sample cases which shows reasonably accurate and reliable prediction of the CTOD response with a predicted error limit of $\pm 5\%$. In summary, the strain-based CTOD estimation schemes followed by the user-friendly flowchart can be utilized with confidence to assess the fracture behavior of flawed pipelines with multiple cracks for practical engineering applications.

Chapter 8

8 Conclusions and Recommendations

8.1 Conclusions

Based on non-linear elastic-plastic fracture mechanics, this thesis demonstrates the failure analysis and assessment procedures on offshore pipelines and materials with 3-dimensional complex defects such as multiple interacting cracks and crack-in-corrosion (CIC) defects. The two main challenges in our fracture mechanics investigations on these defects are: 1) all the cases we have studied are nonlinear problems with large geometrical deformations of high global strain (3 – 4% plastic strain load); 2) the cracks (defects) studied in offshore pipelines are 3-dimensional. This thesis addressed the research gaps on complex defects failure evaluation mainly in:

- The existing codes and standards on offshore pipelines are mostly based on load-controlled methods using linear elastic fracture analysis. Those codes and standards have been proved to be too conservative for failure assessment application. There is highly important to conduct an elastic-plastic fracture mechanics analysis using a strain-based method for offshore pipelines under high global strain (3 – 4%).
- In failure analysis on offshore pipelines with corrosion damage, complexity occurs when surface cracks are developed due to pitting interacts with the general corrosion or existing mechanical flaws such as embedded cracks. These complex defects have not been extensively investigated so far. In industry practice, the aforesaid 3-D defects are assessed by the existing codes and standards which are based on 2-D crack cases under linear-elastic

deformation. Thus, there is still research to be done to understand complex 3-D defects, especially for multiple interacting cracks, and cracks in corrosion (CIC) zone.

- Non-Destructive Examination (NDE) techniques do not have the accuracy to detect both the cracks and corrosion, it is crucial to identify a technique that could assess both the corrosion damage and be able to predict failure of the material using a stress or strain-based technique. TSA and DIC have yet to be utilised for CIC analysis but show potential to address this issue.

In the current study, firstly, the crack-in-corrosion (CIC) defects were analysed. These types of CIC defects were not extensively studied before, very few literatures can be found in open literature. These defects were initially examined with full-field experimental techniques Thermoelastic Stress Analysis (TSA) and Digital Image Correlation (DIC). Experimental investigation on CIC defects in Single-Edge Notched Tension (SENT) specimen were conducted. The TSA method was applied for the first time to study corroded specimens containing cracks. The interactions between the cracks and the corroded surface were explored. A clear self-contained methodology was discussed on calculating the SIF from the TSA data. From our TSA experiments, it was observed and concluded that:

- TSA enables the observation of crack parameters directly, without the necessity of knowing the exact thickness and material degradation properties for CIC defects.
- It is much more accurate to use TSA to detect the crack tip location, compared to visually checking for the crack tip location and measuring the crack length.

- With the TSA data from a region of linearity around a crack tip, the stress intensity factor (SIF) of the crack is calculated accurately.
- In estimating small thickness reduction of uniform corrosion, the data obtained from TSA for the corroded surface might not be reliable.

Further, DIC technique was used to capture the full-field strain measurements of the corroded specimens. Calculation of the fracture parameter, J-integral, was demonstrated comprehensively and Excel Spreadsheets were used to calculate various area contour integrals. These area contour J-integrals were averaged and compared with the handbook solutions. The results led to the following conclusions:

- Visualisation of displacement is useful but not accurate in deciding and quantifying crack parameters. However, the obtained full strain field provides easier visualisation of the crack itself.
- It was interestingly noted that the J-integral value decreases from the start of the crack mouth till the crack tip, instead of the well-known path independent J-integral value expected for 2-D crack case. This could be due to the practical conditions of the SENT specimen; it is a dynamic 3-D problem
- For uncorroded specimens, ample evidence showed that the calculation of average J-integral using DIC is consistent with the handbook solutions.
- A simple handbook solution with thickness reduction is not able to take into consideration the pits effects on the cracks and the material degradation.

- A new technique is proposed on how to evaluate J in components where a standard technique is currently not easy or impossible. The advantage of our technique is that the precise location of the crack tip is not required. This technique is also insensitive to the inelastic strains which are close to the crack tip and within the chosen contour.

The obtained experimental results were then used to validate the development of a fracture mechanics-based numerical approach to model crack-in-corrosion defects in SENT specimen. The proposed fracture mechanics based approach had been conducted to quantify the corrosion damage influence on structural integrity. We had investigated the corrosion surface with geometrical structural changes consisting of a general thickness reduction combined with localised pitting-cracks, where the cracks were predicted to initiate or be initiated from pitting. These two parameters enabled quantification of the damage and the application of fracture mechanics principles to corrosion damage. Based on the numerical models, the following conclusions were drawn:

- 30 numerical models with varying corrosion depth (0 to 2mm) and crack length (4, 5 and 6mm) were carried out and the results showed a consistent trend with the handbook solutions.
- For corrosion deeper than 0.5mm, numerical solutions gave higher K and J -integral values as compared with the handbook solutions. These findings could help us more accurately predict fracture and fatigue failure of corroded offshore pipelines, as the existing industry codes underestimate the stress intensity factor and J -integral values.

- With the conducted validation, our investigation also provides a numerical method for failure analysis of general CIC defective materials.

The above approach was then extended to offshore pipelines where nonlinear elastic-plastic stress analysis was carried out for corroded offshore pipelines with surface cracks subjected to biaxial loadings. The 3-D cracks were located at the centre of the corrosion region. Shallow corrosions (less than 30% of pipeline thickness) were considered. A series of elastic-plastic large deflection analyses had been carried out with varying crack dimensions and corrosion parameters, as well as various loading conditions. Based on our nonlinear numerical simulations, the following conclusions can be drawn:

- Currently in related industry, there are no handbook solutions and standards on CIC cases. This is the first time that the elastic-plastic fracture mechanics assessment on cracks in corrosion zone with various loading conditions has been conducted.
- DNV RP F101 provides a relatively lower failure pressure compared to the numerical based on its safety class and safety factors. As such, if minimum required reliability level is taken, this code would be conservative for some combinations of the input parameters. Results of FE analyses together with pressure variations and selected levels of uncertainties in the defect sizing, form the required basis for a reliability code calibration where appropriate safety factors were defined. This result showed that the DNV code can be coupled with our numerical models instead of burst tests to ensure higher reliability since to conduct experimental burst tests are costly and limited. This provides a good basis if safety factors are to be put in place for our numerical solutions.

- With a large amount of nonlinear finite element numerical simulations, the effects of corrosion, crack dimension and loading conditions on the fracture parameter CTOD had been investigated in detail.
- In failure assessment based on CTOD criterion, the corrosion depth is a more important parameter compared to the total corrosion zone volume.
- An important future study should consider crack cases where the crack permeates from the localized pitting corrosion.

A summary of the process used to evaluate CIC defects failure analysis on offshore pipelines are shown in Figure 8-1.

Figure 8-1 Summary of CIC failure analysis process

Lastly, the elastic-plastic fracture behaviour for 3-D coplanar interacting cracks in the flawed pipelines was investigated. Extensive 3-D finite element simulations had been performed on the nonlinear elastic-plastic fracture behaviours of two interacting coplanar cracks located in offshore pipelines subjected to different combined loadings. The influences of the crack configurations, the separation distance of the two cracks, and the pipe internal pressure on the fracture response

(represented by *CTOD*, Crack Tip Opening Displacement) were investigated in detail. From this investigation, the major work was concluded as follows:

- A parametric study on the effect of different separation distances of the two cracks was performed. These results were consistent with BS7910:2013 when the crack distance was relatively large ($s_n > 1$), where the crack interaction is considered negligible and the two cracks were isolated from each other. In other words, these results prove that when the normalized crack separation distance $s_n < 1$, the cracks interaction should be taken into consideration for failure assessment.
- At a given separation distance of the two cracks, crack propagation always started at the larger crack tip. The semi-elliptical surface crack *CTOD_s* was treated as the determining CTOD to be investigated for the interaction of two cracks when the surface crack was larger than the embedded crack and vice-versa.
- On the interaction of the two cracks, it was found that the influence of the surface crack on the embedded crack was not as evident as the influence of the embedded crack on the surface crack based on the case studies performed.
- Four different loading conditions generally experienced by offshore pipelines were considered. Our results indicated that the most severe fracture response was produced by the tension load combined with high internal pressure ($\frac{\sigma_H}{\sigma_Y} = 0.5$). The biaxial loading led to significant large CTOD values compared to the tension loading, particularly at high global strain.
- Using the CTOD fracture criterion, the current study showed that the BS7910:2013 re-characterization on multiple cracks was conservative.

8.2 Novel Key findings

This thesis addressed several important failure analysis problems in offshore pipelines based on elastic-plastic fracture mechanics. The research work was done through analysis and interpretation of existing industrial design codes and recommended practice standards, especially for crack defects in corrosion zone. The following are the key novel findings:

- Using CTOD fracture criterion, the current study shows that the BS7910:2013 re-characterization on interacting cracks is highly conservative in fracture assessment of multiple interacting cracks, especially when the crack sizes are relatively large. It is found that the BS7910:2013 combination rule yields good agreement of CTOD values for fracture assessment of multiple interacting cracks only when the normalized crack distance $s_n \leq 0.5$.
- Currently in offshore industry, there are no handbook solutions and standards on CIC cases. The first time, to the best knowledge of the author, the elastic-plastic fracture mechanics assessment on cracks in corrosion zone with various loading conditions has been conducted in this thesis.
- Our new findings can help us more accurately predict the fracture and fatigue failures in corroded offshore pipelines, as the existing industry codes underestimate the stress intensity factor and the J-integral values.

In addition, it is a pioneering attempt to use full-field measurement technique for failure analysis of corroded and CIC offshore materials:

- It is much more accurate to use TSA as a method to detect the crack tip location, compared to visually checking for the crack tip location and measuring the crack length. With the TSA data from a region of linearity around a crack tip, the stress intensity factor (SIF) of the crack is calculated accurately.
- The proposed DIC technique demonstrates the evaluation of J-integral in components. Currently there is no standard technique to obtain the J-integral. The advantage of our technique is that the precise location of the crack tip is not required. This technique is also insensitive to the inelastic strains which are close to the crack tip and within the chosen contour.
- The experimental testing conducted shows that there is significant crack-corrosion interaction effect which directly influences the failure analysis results, especially when the localised corrosion effects from pitting exist.

Following that, numerical simulations were performed for more complicated cases, multiple 3-D cracks and crack-in-corrosion cases in offshore pipelines:

- With the numerical results obtained, new strain-based CTOD estimation schemes are proposed at the end of Chapters 7, where the CTOD is defined as a function of the applied global strain. The proposed schemes can be utilized to assess the fracture behaviour of flawed pipelines with multiple cracks for engineering reference. Additionally, a flowchart at the end of Chapter 7 gives a clear outline on the CTOD estimation procedure using strain-based CTOD estimation for 2 coplanar cracks with both surface and elliptical cracks of shape factor $\frac{c}{a} > 1$ and for global strain larger than 1%. The validations on the CTOD

estimation flowchart and schemes are performed with sample cases which shows reasonably accurate and reliable prediction of the CTOD response with a predicted error limit of $\pm 5\%$. In summary, the strain-based CTOD estimation schemes followed by the user-friendly flowchart can be utilized with confidence to assess the fracture behavior of flawed pipelines with multiple interacting cracks for practical engineering applications

- So far, no handbook solutions and standards on CIC cases are available in the industry. Our research work on CIC provides a reliable failure assessment method for such defects on pipelines under biaxial loading conditions using nonlinear elastic-plastic fracture mechanics. For offshore pipelines, there are significant differences for failure assessment of cracks with/without corrosion damage.

8.3 Industrial recommendations & possible future explorations

Figure 8-2 Protocol for structure failure analysis

This thesis provides evidence-based criteria for model acceptance and testing. As such, Figure 8-2 proposes a protocol for failure analysis based on the thesis findings. This protocol is crucial especially for operational and aging offshore structure. It starts with both inspection & monitoring, and the design basis is nonlinear finite element analysis. For the inspection and monitoring, it takes in full-field stress/strain data from the techniques mentioned in Chapter 3, 4 and 5 based on TSA

and DIC. These are possible in-situ NDT program for complex defects such as crack-in-corrosion (CIC) in offshore materials and structures. On the other side, nonlinear numerical methodology mentioned in chapter 6 and 7 can be used to predict the fracture capacity based on the defects measured during inspection. Simplified corrosion damage model can be used to predict failure based on fracture mechanics for offshore structures. These numerical simulations can be used to replace difficult and costly physical tests. With the known load and strain demand from pipeline operations, a quick decision can be made based on load demand/capacity (CTOD and critical CTOD), defects number, size and locations. This protocol provides a complete analysis of complex defects in offshore pipelines based on elastic-plastic fracture mechanics.

8.4 Future Work

The thesis demonstrates the application of nonlinear elastic-plastic fracture mechanics methodologies to assessing crack-like defects in offshore structures and pipelines under large strain loading. There are plenty of future exploration to be done to further improve and enhance the proposed methodologies and findings. For the experimental techniques, it is noted that this study does not exert loads beyond the elastic region and that the elastic-plastic capabilities of the technique has yet to be assessed. Future work in this aspect can be embarked on checking the validity of plastic loads on such full-field techniques, especially on TSA and DIC. In terms of numerical investigation on CIC, an important future study should consider crack cases where the crack permeates from the localized pitting corrosion. In addition, more cases and examination on the various cracks and corrosion parameters can be done for the CIC analysis to be more robust.

If possible, a full-run through of the proposed protocol using actual in-service offshore pipeline can be made to fully check the capability of the methodologies explored in this thesis.

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A. Appendix A: Corrosion Experiment Report

A-1 Fatigue Pre-cracking Report

The following table shows the fatigue pre-cracking that was conducted on the specimens:

Table A-1 Fatigue Pre-cracking				
Specimen 1				
No. Of cycles	Mean	Amplitude	Frequency (Hz)	a (mm)
25000	15	15	25	0
50000	15	15	25	4.578
Specimen 2				
No. Of cycles	Mean	Amplitude	Frequency (Hz)	a (mm)
25000	15	15	25	0
50000	15	15	25	3.633
55000	15	15	25	6.231
Specimen 7				
No. Of cycles	Mean	Amplitude	Frequency (Hz)	a (mm)
25000	5	5	40	0
75000	10	10	40	0
100000	10	10	40	0
150000	10	10	45	0
190000	15	15	40	3.950
Specimen 8				

No. Of cycles	Mean	Amplitude	Frequency (Hz)	a (mm)
25000	15	8	40	0
50000	15	8	40	0.0898
75000	15	15	20	0.326
100000	15	15	25	5.287
106500	15	15	25	5.818

A-2 Corrosion Experiment summary

Simple specimens (with and without pre-cracks) manufactured from offshore structures were artificially corroded, with the duration of exposure being varied to produce different degrees of corrosion damage. After the environmental attack, the specimens were mechanically tested. Post-test measurements of specimen thickness, deepest flaw depth and full-field stress strain will provide a quantitative assessment of the degree of corrosion damage. This corrosion experiment was done at TSLR Lab, NCATS Lab, University of Southampton (UoS).

The specimens' surfaces were prepared by polishing it evenly using a lapping machine. The specimens were immersed in 3L solution of 3.5wt% NaCl in distilled water in room temperature. A container was used to conduct the corrosion experiment in a fume cabinet as seen in Figure B-1. Specimen 4,5,6,7 used the same container for the period of time while Specimens 7 and 8 uses the same container. The corroded samples are shown in Figure B-2.

Figure A-1 Container with 4 specimens immersed in NaCl solution

Figure A-2 Sample of photos taken of specimen after corrosion immersion

The following Table A-2 shows the specimen mass, length, width, thickness and corrosion time.

Table A-2 Specimens' parameters

Specimen	Original Mass (g)	Density (g/mm ³)	Length (mm)	Width (mm)	Thickness (mm)	Corrosion Time
1	124.355	0.00769844	131.07	30.43	4.05	No corrosion

2	124.17	0.00770445	130.80	30.20	4.08	No corrosion
3	120.512	0.00768055	130.41	30.46	3.95	2 weeks
4	121.158	0.00769406	130.72	30.42	3.96	2 weeks
5	121.665	0.00769667	130.21	30.35	4	4 weeks
6	120.867	0.00775021	130.26	30.31	3.95	4 weeks
7	120.543	0.00774983	130.3	30.3	3.94	4 weeks
8	120.186	0.00776059	130.1	30.28	3.93	4 weeks

A-3 Calibration of TSA and DIC techniques:

1. Samples are sprayed with matt black paint (3 passes of coating: thickness 17.4 microns) are tested under cyclically loaded with an amplitude of $\pm 5\text{kN}$ (41.7 MPa / 62.5 MPa) at about a mean load of 10kN (83.3 MPa / 125 MPa) with loading frequency of 10 Hz
2. Thermoelastic constant, k , is calculated from the average of the ΔT and T values - $K = 3.31 \times 10^{-12} \pm 0.06 \text{ Pa}^{-1}$
3. This k will be used for the experiment samples
4. DIC strains is to be checked against with strain gauges using strain smart system. Virtual measuring tools will be put in place from a known dimension of the samples

B. Appendix B: Calculations

B-1 TSA Experimental calculations

Calculate stress intensity factor K_I

$$\Delta T = -K T_0 \Delta(\sigma_x + \sigma_y) \quad (\text{B-1})$$

$$\frac{\Delta T}{T_0} = -K \Delta(\sigma_x + \sigma_y) \quad (\text{B-2})$$

$$\frac{\Delta T}{T_0} = K_I (-K \sqrt{\frac{2}{\pi r}}) \quad (\text{B-3})$$

B-2 Contour Integral Method: Cantilever Example

Figure B-1: Cantilever Problem

The J-integral [19] is characterised by:

$$J = \int_{\Gamma} \left(w \, dy - T_i \frac{\partial u_i}{\partial x} \, ds \right) \quad (\text{B-4})$$

where w is the strain energy, dy is in the y direction, T_i is the dot product of traction, ds is along the contour path. Based on cantilever specimen as seen in Figure B-1, the J-integral can be calculated from the contour and simplified [139] with the right vertical edge is far away from the crack tip and $B \ll 2h$,

Γ_3 : Γ_3 is far away from the crack tip, $J_{\Gamma_3} = 0$

Γ_2 & Γ_4 : Since contour is in the x direction, $dy = 0$;

Since $n_1 = 0, \sigma_{12} = \sigma_{22} = 0$

$$T_i = \sigma_{ij}n_j = \begin{Bmatrix} \sigma_{11}n_1 + \sigma_{12}n_2 \\ \sigma_{21}n_1 + \sigma_{22}n_2 \end{Bmatrix} = \begin{Bmatrix} 0 \\ 0 \end{Bmatrix}$$

$$\therefore J_{\Gamma_2} = J_{\Gamma_4} = 0$$

Γ_1 & Γ_5 : $J_{\Gamma_1} = J_{\Gamma_5}$

$$J = 2J_{\Gamma_5} = 2 \int_{\Gamma_5} \left(w \, dy - T_i \frac{\partial u_i}{\partial x} ds \right) \quad (\text{B-5})$$

$$T_i = \sigma_{ij}n_j = \begin{Bmatrix} \sigma_{11}n_1 + \sigma_{12}n_2 \\ \sigma_{21}n_1 + \sigma_{22}n_2 \end{Bmatrix} = \begin{Bmatrix} \sigma_{11}n_1 \\ \sigma_{21}n_1 \end{Bmatrix}$$

Since $n_2 = 0$,

$$w = \frac{1}{2} \sigma_{ij} \varepsilon_{ij} = \frac{1}{2} (\sigma_{xx} \varepsilon_{xx} + 2(\sigma_{xy} \varepsilon_{xy}) + \sigma_{yy} \varepsilon_{yy}) = \frac{1}{2} \left(\frac{\sigma_{xx}^2}{E} + \frac{\sigma_{xy}^2}{\mu} \right)$$

$$\frac{\partial u_x}{\partial x} = \frac{\sigma_{xx}}{E}, \quad \frac{\partial u_y}{\partial x} = 0$$

Szekrényes [139] has more examples on analytically solving for J-integral for delaminated beam and plate models.

C. Appendix C: Mesh Study

An initial mesh sensitivity study has been performed to check for the accuracy of the mesh use in this study. The analysis is carried out with 3 different meshes and element types for 1 circular elliptical embedded crack case of $a = 2\text{mm}$ and $c = 2\text{mm}$. Taking the lead from Jayadevan et al.[28] and Anderson[4], the same mesh quality is employed for our refined meshing since there is a limitation to the ABAQUS (student edition) program with maximum number of nodes at 100000. The number of elements, nodes and element type are given in Table B1. Case 1 meshes use refined elements near the crack region as well as more elements in the region away from the crack with a 8-node linear brick, reduced integration element (C3D8R). This gives a total element number of 63485 with 69968 nodes. As for cases 2 and 3, the meshes have equally refined elements near the crack region but less elements (coarse mesh) in the region away from the crack. Case 2 uses the C3D8R elements while case 3 uses a 20-node quadratic brick with reduced integration (C3D20R) that gives 62844 nodes.

Table C-1 The three different cases with various numbers of elements, nodes and element type employed.

Case	Number of elements	Number of nodes	Element Type	Element description
1	63485	69968	C3D8R	8-node linear brick,
2	14185	16248	C3D8R	reduced integration, hourglass control
3	14185	62844	C3D20R	A 20-node quadratic brick, reduced integration

From Fig. B1, the CTOD-strain curves of the 3 cases are plotted in Figure B1. It shows that results obtained from the coarse mesh (case 2) deviates away from those predicted by the fine model. In addition, results from case 3 with a higher element type deviate only marginally from those of case 1. As such, case 1 mesh quality and element type is employed in the work done, and will be used in future work.

Figure C-1 The evolution of CTOD with global strain for three different element numbers and types