Failure analysis and damage prevention on offshore pipelines under extreme loadings

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NANYANG
TECHNOLOGICAL
UNIVERSITY

**FAILURE ANALYSIS AND DAMAGE
PREVENTION ON OFFSHORE PIPELINES
UNDER EXTREME LOADINGS**

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Interdisciplinary Graduate School
Nanyang Environment and Water Research Institute

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List of Symbols

a, a_s	Crack depth of semi-elliptical surface crack
a_0	Initial half crack height of the elliptical embedded crack
$2a, 2a_e$	Crack height of the elliptical embedded crack
a^*	The equivalent half crack height of two interacting cracks
a_{eff}	The effective half crack height of two interacting cracks
$A_0 \sim A_2$	Coefficients in Eq. (3.11a)
b_{eff}	The effective half crack length of two interacting cracks
$2b, 2b_s$	Major axis length of semi-elliptical surface crack
$2b_e$	Major axis of elliptical embedded crack
B	Wall thickness of the weld
$B_0 \sim B_5$	Coefficients in Eq. (3.11b)
c_0	Initial half crack length of the elliptical embedded crack
c_1, c_2	The material constants in Eq. (6.11)
c_3, c_4	The material constants in Eq. (6.12)
c_{eff}	The effective half crack length of two cracks in BS7910
C_0	One of Paris Law's parameters
CTOD_S	CTOD value at the crack tip of the semi-elliptical surface crack in Chapter 5
CTOD_R	CTOD value at the right crack tip of the elliptical embedded crack in Chapter 4 & 5
CTOD_L	CTOD value at the left crack tip of the elliptical embedded crack in Chapter 4 & 5
$CTOD_{in}$	The CTOD_S value from the case of two collinear cracks
$CTOD_{only}$	The CTOD_S value from the case of a surface crack only
D	Outer diameter of pipeline
$D_0 \sim D_9$	Coefficients in Eq. (5.2)
E	Young's modulus

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f	Dimensionless fracture parameter
f_0	A coefficient in Eq. (3.10)
f_1, f_2	Dimensionless functions in Eq. (3.10)
$F_\alpha(x)$	The elastic asymptotic crack tip functions
f_w	Correction term of stress intensity factor for an elliptical flaw
G_{thresh}	The threshold energy release rate
G_{equivC}	Equivalent fracture energy release rate
G_{max}, G_{min}	The energy release rates
$H(x)$	The discontinuous jump function
J	J -integral
K	Strength coefficient in Eq. (3.2)
K_I	Applied stress intensity factor
K_{nn}, K_{ss}, K_{tt}	The normal and tangential stiffness components
L	Length of pipeline segment adopted
L_r	Ratio of reference stress to yield stress
$m_\varepsilon, n_\varepsilon$	Coefficients in Eq. (4.3)
m	One of Paris Law's parameters
M	Bulging correction factor
M_m, M_b	Stress intensity magnification factors
n	Strain hardening coefficient
$N_I(x)$	The usual nodal shape function
N_0	The number of loading cycles when the crack starts to propagate
p	Crack depth from the weldment cap to the closer crack tip of embedded crack
P	Internal pressure
$PEEQ$	The variation of the equivalent plastic strain
P_m	Primary membrane stress
P_b	Primary bending stress
Q	Parameter Eq. (6.13)

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r	The mean shell radius of pipe
R	Stress ratio
R_x	Rotational angle in radian
s	Separation distance between the surface crack tip and the left crack tip of the embedded crack
$2s$	Circumferential crack length of semi-elliptical surface crack
s / a^*	The dimensionless separation distance
S_{crack}	Crack size, i.e. surface area of the elliptical crack
S_{pipe}	The mean cross-sectional area of the pipe
t	Wall thickness of pipeline
t_n , t_s and t_t	The normal and the two shear components of the traction stress vector
u or u_i	The displacement vector
U_I	The usual nodal displacement vector
w	The strain energy density
$\alpha_0, \alpha_1, \alpha_2$	Coefficients in Eq. (4.2)
γ_s	The interaction factor
δ_I	The CTOD value
δ_{mat}	The required CTOD fracture toughness
$\delta_{critical}$	The material's CTOD fracture toughness
$\delta_n, \delta_s, \delta_t$	The normal and the two shear components of separation distances
ΔK_a	The applied stress intensity factor range in crack height direction
ΔK_c	The applied stress intensity factor range in crack length direction
$\Delta \sigma_m$	The primary membrane stress range
$\Delta \sigma_b$	The primary bending stress range
η	An intermediate parameter
η_a	The sensitivity factor for the crack depth
$\eta_{b/a}$	The sensitivity factor for the aspect ratio

λ_s	Parameter Eq. (6.13)
ν	Poisson's ratio
ε_{unc}	The strain of uncracked body
ε_g	Global strain
ε_{ref}	Reference strain calculated at reference stress based on stress-strain relationship
σ_1, σ_2	Stresses from the linearized distribution of actual stress along pipe's wall thickness
σ_H	Hoop stress
σ_{ij}	The stress field ahead of the crack tip
σ_{ref}	Reference stress
σ_{unc}	The stress of uncracked body
σ_Y	Yield tensile strength of the material
σ_{uts}	Ultimate tensile strength
σ_H / σ_Y	The ratio of the hoop stress to the yield stress
σ_{max}^o	The maximum allowable principal stress
σ_{max}	The maximum stress of the model caused by applied load
σ_{min}	The minimum stress of the model caused by applied load
φ	The crack surface function
ψ	The orthogonal surface function

Summary

As foreseen, the crude oil and natural gas are still the key energy resources for the next decades although there has been a rapid development in the clean and renewable energy. Offshore pipeline, which is often constructed by girth welding method, are one of major transportation tools for these natural resources. Some flaws, such as surface or embedded cracks, may be introduced into the welds during the construction. The potential flaws under large plastic deformation will seriously threaten the structural reliability of offshore pipelines. Therefore, the main aim of this PhD research project is to investigate the nonlinear elastic-plastic fracture behaviors of offshore pipelines subjected to extreme loading conditions, such as the large plastic bending and fatigue loadings. Based on our investigations of fracture response of the flawed pipelines, failure assessment diagrams will be proposed for preliminary estimations of the fracture capacity.

Most of the existing fracture assessment procedures were derived based on load-controlled method which is not explicitly suitable for the cases with large plastic deformation. Alternatively, strain-based approach is preferably adopted for fracture assessment on offshore pipelines when it largely exceeds the yield stress. Firstly, the nonlinear elastic-plastic fracture responses of pipeline girth welds with 3-D semi-elliptical surface cracks subjected to large plastic strains are investigated. Extensive numerical simulations are performed to investigate the influences of the crack depth, crack aspect ratio and the internal pressure on the evolution of CTOD.

Secondly, considering that the embedded cracks may be inevitably contained in the pipelines, fracture assessment for offshore pipelines with 3-D elliptical embedded cracks is carried out. Considering the two crack tips in the model which thus requires more mesh grids around the cracked region, the submodeling technique is adopted to alleviate the large computational demanding. Comparisons of fracture toughness obtained by BS7910:2005 and

the current results are made for both surface cracks and embedded cracks. Based on the crack tolerance criteria required for weld metals, the strain-based failure assessment diagrams (FADs) are proposed for the flawed pipelines under large plastic strains.

Thirdly, the investigation on two interacting 3-D cracks in offshore pipelines is carried out. The influences of the cracks configuration, the separation distance and internal pressure on the crack behaviors are studied thoroughly. In order to further demonstrate the interaction of two collinear cracks in pipelines, the interaction factor γ_s is introduced to quantify the effect on nonlinear elastic plastic fracture behaviors.

Last but not the least, fatigue assessment on offshore pipelines with elliptical embedded cracks under cyclic loadings has been performed using XFEM. Fatigue crack initiation, fatigue crack growth and fracture resistance behavior are investigated for pipelines containing different embedded cracks subjected to different stress ratios. Comparisons of those values between 2-D theoretical analysis and the BS7910 code are made to better understand our findings.

CHAPTER 1

Introduction

1.1. Background

As foreseen, the crude oil and natural gas are still the key energy resources for the next decades although there has been a rapid development in the clean and renewable energy [1]. Offshore pipeline, as one of the major transportation tools for these natural resources, has attracted more attention on its reliability and safety research. With sharp focus on the environmental pollution issues recently, the sustainable way to reduce the environmental impact and pollutions such as oil pipeline leaking, is highly preferable. In order to achieve the balance between an economically competent design and environmental protection, carbon steels with considerable plastic deformation capacity are utilized to construct offshore pipelines. The pipeline design strategy can be found in most of the major design codes, such as API [2] and DNV[3].

Typical offshore pipelines are usually connected by girth welding method. During the construction process, mechanical flaws such as surface or embedded cracks, are inevitably introduced into the weld zone. In addition, for pipelines installed by reeling method, the large plastic bending may pose tremendous challenges to the structural integrity of pipelines, especially to the girth welds. For instance, the large plastic strains, one of the potential causes leading to failure, shall be up to a global strain of 3% in a typical reeling situation. Due to the aforesaid reasons, failure analysis on girth welded pipelines under large plastic deformation has obtained increasingly significant attention from researchers [4-7].

Current codes and standards for fracture assessment of offshore pipelines give only a partial representation and are proved to be too conservative in engineering practice, since they are mostly based on the load-controlled methods. The fact is that pipelines may be subjected to displacement-controlled

loading under several circumstances, such as the installation operation (reeling process). In 2006, the first guideline targeting the fracture assessment corresponding to large plastic strains is seen in DNV-RP-F108 [8]. However, the DNV guideline basically recommends the use of BS7910:2005 [9] with some modifications for offshore pipelines subjected to large plastic strains. Some key issues, such as nonlinear elastic-plastic fracture mechanics theory, the non-linear stress-strain constitutive relations of materials under large deformation, are not reflected in the current codes of practice. Moreover, it is highly conservative for these codes to make failure assessment on flawed pipelines under strain-controlled loads exceeding the yield stress [10]. Consequently, the strain-based method is desirable for fracture assessment on offshore pipelines under large plastic deformation.

1.2. Objectives

The theoretical study on elastic-plastic fracture behavior of offshore pipelines subjected to large plastic deformation is still in its primary stage, with several unanswered questions and debates. The current research aims at establishing nonlinear elastic-plastic fracture investigation for offshore pipelines with 3-D cracks under large plastic deformation. An easy-to-interpret and accurate estimation model of fracture toughness will be proposed for further practical failure assessment. In addition to that, the pressure fluctuations are common practice for the static pipelines. However, the issue of the embedded cracks by fatigue in offshore pipelines does not appear to be reported in the open literature. Fatigue crack growth behavior is normally determined by various parameters, such as the geometry of structure and crack, the aspect ratio of crack and fatigue loading conditions. Therefore, the adequate confidence to design offshore oil & gas system productions should be built upon a series of preliminary fatigue tests using full-scale numerical simulations.

The objectives are outlined below:

- To have a thorough understanding on the elastic-plastic fracture behavior of the engineering materials used in offshore pipelines.
- To obtain the fracture response represented by crack tip opening displacement (CTOD) of offshore pipelines with 3-D surface or embedded cracks under extreme bending load.
- To investigate the crack behaviors of the pipelines containing multiple 3-D cracks subjected to large plastic strains, and to evaluate the interactions for different crack configurations.
- To study the fatigue behaviors of offshore pipelines with a circumferential embedded crack subjected to cyclic tensile loading, and to discuss the influence of various parameters, such as initial crack size, aspect ratio and stress ratio on fatigue crack growth.
- To propose user-friendly failure assessment diagram for engineers on fracture response of the flawed pipelines under different loadings.

1.3. Overview

Chapter 2 presents the fundamental theories and concepts used in this study, such as the definitions of the stress intensity factor (SIF), *J-integral* and crack tip opening displacement (CTOD). Previous research efforts provided the fracture assessment represented by the *J-integral* estimation extensively for several different crack geometries in pipelines. Several approaches on fracture assessment are reviewed for comparison purpose. Due to the imperfect construction process, various cracks may be introduced to the components of the pipelines, resulting in pipeline's sudden failure under certain situations. For this reason, the literature review is concerned with the analytical procedures and techniques which are commonly employed to investigate the interaction of multiple cracks. Last but not the least, the development of fatigue research and the fatigue study in offshore pipelines are extensively reviewed.

Chapter 3 describes the fracture response of offshore pipelines with 3-D semi-elliptical surface cracks subjected to pure bending load as well as combined

bending with the internal pressure. For internal pressure, the dependency of the loading path on the plastic fracture response is investigated. Comparison of fracture toughness obtained by the BS7910 code and the current FE results is carried out. Based on the crack tolerance criteria required for weld metals, the strain-based failure assessment diagrams (FADs) are proposed for offshore pipelines with semi-elliptical surface cracks under large plastic strains.

In Chapter 4 the nonlinear elastic-plastic simulations are carried out for offshore girth welded pipelines with 3-D elliptical embedded cracks subjected to large plastic bending as well as biaxial loadings. The influence of strength mismatch on CTOD evolutions is investigated. New strain-based CTOD estimation equation is proposed through the power function of the global strain. Failure assessment diagrams (FADs) are established for the preliminary fracture assessment on pipelines girth weld with an embedded crack.

Chapter 5 looks at the interactions of multiple cracks on the fracture behaviors of the pipelines with two collinear cracks. The influences of the cracks configuration, the separation distance and the internal pressure on the crack behaviors are investigated for various loading conditions. Based on our results, the function of the interaction factor is proposed for assessing the fracture behavior of the large surface crack affected by the two interacting cracks. Comparison of fracture response between the current results and the computational outcomes derived from the BS7910 code indicates that the combination rules in BS7910 may produce conservative estimations for large collinear multiple cracks.

Chapter 6 presents a systematic study on fracture resistance behavior of offshore pipelines containing an elliptical embedded crack under cyclic tension loadings. XFEM is adopted for numerical simulations. The influences of various parameters, such as initial crack size, stress ratio and aspect ratio on the fatigue crack behaviors are investigated in details. Moreover, the interpretation of fatigue assessment from the theoretical analysis and the BS7910 code is

provided, and the comparison between the values obtained from the theoretical analysis, BS7910 and current investigation is made from aspect of fatigue crack growth rate.

Chapter 7 outlines the main conclusions from the thesis and gives the recommendations for future work.

CHAPTER 2

Literature Review

Fundamentals of fracture mechanics, as highly relevant to the study of structural integrity and fracture response of offshore pipelines, are reviewed in this chapter. Different approaches commonly used in assessing fracture behavior of offshore pipelines subjected to large plastic deformations are summarized. Researches on the interactions of multiple cracks with respect to the description model and implementation techniques are discussed. In addition, the development of fatigue study and recent researches on the fatigue crack growth of steel structures are reviewed from the perspectives of both the experimental and the numerical approaches.

2.1. Fundamentals of Fracture Mechanics

2.1.1. Stress intensity factor

Given an infinite elastic plate with a crack, the schematic diagram of a cracked body loaded in mode I (crack opening mode) is shown in Fig. 2.1 (Hutchinson [11]). The stresses ahead of the crack tip can be written as follows (William [12]):

$$\sigma_{ij} = A_{ij}(\theta)r^{-1/2} + B_{ij}(\theta) + C_{ij}(\theta)r^{1/2} + \dots \quad (2.1)$$

The first term $A_{ij}(\theta)r^{-1/2}$ in Eq. (2.1) has an inverse square root of r , i.e., as $r \rightarrow 0$, it would play a dominant role in the solution. Hence, the other terms of Eq. (2.1) are dropped in that region very close to the crack tip, the stresses are completely characterized by such parameter named as the stress intensity factor K :

$$\sigma_{ij} = \frac{K}{\sqrt{2\pi r}} f_{ij}(\theta). \quad (2.2)$$

Where K can be determined for a given specimen geometry and the loading through solving the corresponding boundary value elasticity problem.

Due to the derivation process of K , its application is limited to situations with linear elastic fracture mechanics (LEFM). While LEFM can still be available for small scale yielding (SSY) problem in which the inelastic region's size is much smaller than that from the related dimensions of the structure.

Figure 2.1 Schematic diagram of a cracked body loaded in Mode I and illustration of contour path Γ for J -integral.

2.1.2. J -integral

When a significant amount of plastic deformation takes place before the fracture or failure occurs, the theory of LEFM and K would not be approximate to make the fracture assessment for the structure loaded up to large scale yielding (LSY). The J -integral, originally proposed by Rice [13], has been used as the counterpart of K to characterize elastic-plastic fracture mechanics (EPFM). J -integral is defined as follows (see Fig. 2.1 for illustration of the contour path Γ and its unit outward normal n),

$$J = \int_{\Gamma} (w dy - T_i \frac{\partial u_i}{\partial x} ds), \quad (2.3)$$

where w is the strain energy density and u_i are the displacement components.

Later Hutchinson [14], Rice and Rosengren [15] proved that the near-tip stress and strain fields of an elastic-plastic material can be characterized solely by J (this characteristics of material is named as HRR singularity). The widely used

Ramberg-Osgood formulation is adopted here to represent the uniaxial stress-strain relationship, expressed as below:

$$\frac{\varepsilon}{\varepsilon_0} = \frac{\sigma}{\sigma_0} + \alpha \left(\frac{\sigma}{\sigma_0} \right)^n \quad . \quad (2.4)$$

Thus, the near-tip singularity fields can be written as [11] ,

$$\begin{aligned} \sigma_{ij} &= \sigma_0 \left(\frac{J}{\alpha \sigma_0 \varepsilon_0 I_n r} \right)^{\frac{1}{n+1}} \sigma_{ij}(\theta, n) \\ \varepsilon_{ij} &= \alpha \varepsilon_0 \left(\frac{J}{\alpha \sigma_0 \varepsilon_0 I_n r} \right)^{\frac{n}{n+1}} \varepsilon_{ij}(\theta, n) \quad . \end{aligned} \quad (2.5)$$

From the above equations, it can be found that for the elastic-plastic material, there is a direct relationship between the J -integral and the near-tip stresses and strains [16, 17]. As long as the HRR singularity continues to characterize the near-tip field, J can be regarded as a phenomenological parameter for EPFM, depending on the geometry and loading of the cracked body. Moreover, for such structure under SSY conditions, J -integral and K have a unique relationship derived from aspects of energy release [18].

2.1.3. Crack tip opening displacement (CTOD)

Figure 2.2 Illustration of CTOD measurement.

Crack tip opening displacement (CTOD or δ) was suggested by Wells as another fracture mechanics parameter based on the experimental observations [19]. The definition of CTOD is illustrated in Fig. 2.2 [20]. The CTOD value is the vertical opening distance between the intercepts of two 45° lines, drawn

from the blunted tip to the crack flanks. It was proved that CTOD and J have a unique relationship based on the HRR-dominated crack tip field (Shih 1981[20]). Their relationship can be expressed as follows:

$$CTOD(\delta) = \frac{d_n J}{\sigma_0} . \quad (2.6)$$

Measurement of CTOD value is relatively simple and direct in both experiments and finite element simulations, and thus CTOD becomes an alternative fracture parameter in EPFM.

In fact, whichever fracture mechanics parameter is chosen, the fracture assessment on a cracked body is usually implemented by comparison between the crack driving force (or called the energy release rate) and the critical fracture toughness which is obtained in the experimental specimens. For instance, under a given loading condition of $J < J_R$, the crack would not grow. Usually the material resistance R versus the crack extension is plotted to illustrate the crack growth behavior characterized by fracture toughness of the material. Fig. 2.3[18] schematically shows a typical J resistance curve for a ductile material (such as grade X60 pipeline steel). If small amounts of crack advance are permitted in the ductile materials, the resultant issue will encounter with the judgment on the stability of the crack growth. To ensure that no failure occurs, the criterion for the crack extension in a stable manner must be satisfied

by [11]: $\frac{dJ}{da} < \frac{dJ_R}{da} .$

Figure 2.3 J resistance curve for a ductile material (Anderson 2005[18]).

2.2. Fracture Assessment on Offshore Pipelines

2.2.1. Fully plastic solution of J in an exponential form

Applications of elastic-plastic fracture mechanics (EPFM) to structural integrity analysis require the availability of solutions that relate the fracture response J to loading conditions and the crack geometry. An estimation procedure was developed for the elastic-plastic crack problems to obtain approximation of J -integral through the combination of the linear elastic solutions [21]. Based on the deformation plasticity (nonlinear elasticity), a typical solution in which J is proportional to load- P^{n+1} , was given below:

$$\frac{J}{\alpha \sigma_0 \varepsilon_0 a} = \left(\frac{P}{P_0} \right)^{n+1} h(n, \text{geometry}). \quad (2.7)$$

This power-law solution can reflect the entire range of material's behavior from small-scale to fully plastic yielding. Since it was proposed by the researchers from Electric Power Research Institute, this method was called EPRI methodology, and became widely used to analyze the elastic-plastic fracture behaviors of loaded structures.

Various modifications and improvements have been done to make the solutions more suitable for the engineering practice. The additional geometry parameters, such as different type cracks were considered into this J solution [22]. Combined with the unloading compliance technique, a new J -integral formulation was developed to describe fracture behavior of SET specimen made of pipeline steel and measure crack growth resistance based on the results from crack mouth opening displacement [23, 24]. However, for circumferential cracks in the pipes, the very limited applicability of these J solutions was found to the strain hardening materials. Later this approach was revised by Ruggieri et al. [25-27] to be useful for a wider range of crack configurations and materials through building two dimensionless functions.

2.2.2. Reference stress method

Although Eq. (2.7) is straightforward in giving J estimation using the power-law solution, it is still of some difficulties in practice. For instance, it could be very difficult to find one set of fitting parameters in Eq. (2.3) for work hardening materials. Moreover, the power-law J estimation may not provide accurate results for characterizing the fracture responses of such material that has a significant difference between its yield strength and ultimate tensile strength [28]. Hence, the reference stress approach previously used in the creep analysis, was initially proposed by Ainsworth [29, 30] to estimate the J value in EPFM.

First of all, the reference stress is defined by the following form:

$$\sigma_{ref} = (P / P_0) \sigma_0 . \quad (2.8)$$

Then, the evaluation of J is written as below:

$$J = K^2(a_e) / E' + ch_1(a / w, n) \sigma_{ref} (\epsilon_{ref} - \sigma_{ref} \epsilon_0 / \sigma_0) . \quad (2.9)$$

In Eq. (2.9), the coefficients are usually obtained by finite element simulations. Subsequently, this J estimation scheme was adopted to develop the assessment

route for the fracture behaviors of elastic-plastic materials. The above equation was slightly modified and applied in major design codes (e.g. RSE-M and RCC-MR code [31, 32], R6 and BS7910 [33], DNV-RP-F108 [8]) for elastic-plastic fracture analysis of pipelines, due to its close representation of the real material properties. The modification and improvements for determination of reference stress were carried out to obtain more accurate estimation of J . For example, Goodall and Webster (2001) [34] developed an analytical procedure of the reference stress for the plates with partially penetrating cracks subjected to combined loadings. In addition, based on the reference stress method, Hertelé et al. (2011) [35] proposed a similar equation for part-through inner defect in the curved plates.

As long as the stress-strain relationship is available in a power hardening law, both the fully plastic solution and the reference stress method can provide similar estimations of fracture toughness measured by the J -integral for low and moderate deformation. However, the latter may produce relatively higher value of J for low hardening materials when the circumferentially cracked pipes under pure bending are involved [25].

2.2.3. Reference strain method

When it came to the displacement-controlled loading and large plastic deformation (for instance, the reeling process of the pipelines), the fully plastic approach and reference stress method could not provide more accurate predictions due to their load-controlled derivation. Linkens et al. (2000) [36] firstly proposed an alternative approach which is called the reference strain method. In this approach, the J estimation is expressed by the stress and the strain of the un-cracked body:

$$J = 2F^2 \pi a \sigma_{unc} \varepsilon_{unc} . \quad (2.10)$$

The experimental [37] and numerical results [38, 39] on the reeled pipelines were obtained in agreement with Eq. (2.10). However, for large cracks, Eq. (2.10) could hardly provide any accurate estimation of J because of the major

discrepancy between the reference strain and the strain from the un-cracked body [40]. More research efforts have been taken to improve the accuracy of J prediction scheme as well as to simplify its formulation. For instance, based on the results from the shell FE models, the almost linear evolution of fracture response was found, and a new J estimation defined by a set of nominal strains was proposed for the displacement-controlled loadings [41]. Recently the revised formulation was presented by Nourpanah (2010) [42] to develop one prediction formulation of J for the reeled pipes:

$$\frac{J}{\sigma_y t} = f_1 \varepsilon_{unc} + f_2 \quad . \quad (2.11)$$

Where f_1 and f_2 are coefficients, generally obtained through FE analysis. One of the characteristics from Eq. (2.11) is that the strain-based assessment approach is used to evaluate the fracture response of the reeled pipes.

2.2.4. CTOD measurement

Based on the HRR-dominated crack tip field, the relation between CTOD and the J -integral is unique. Fracture assessment on offshore pipelines evaluated by CTOD value has been investigated by many researchers [43]. Using 3-D finite element simulations, a series of parametric studies were performed on the CTOD evolution of a cracked pipeline subjected to large plastic bending [38, 44]. In addition, the integrity assessment equation was discussed and improved for elastic-plastic pipe bend or elbow with through-wall circumferential cracks [45, 46]. Recently using the mechanical properties of the three component parts in the offshore pipelines, the investigation on fracture response of girth welded pipeline was carried out under large plastic tension [47, 48]. Also, in the major design codes and standard guidelines (such as BS7910:2005 [9]), CTOD can be found as a significant parameter of fracture assessment which provides the corresponding estimation procedure.

2.3. Interactions of Multiple Cracks

The research work reviewed in previous sections is mainly dealing with the influence of a single crack on the structural integrity of offshore pipelines. In fact, many various cracks may be introduced to the components of the pipelines due to the imperfect construction process or even in service. In this case, a pipeline's sudden failure would result from the presence of closely located multiple interacting cracks, even though the size of each crack is quite smaller than the threshold value. To understand the influence of multiple cracks on integrity and safety of the pipelines is indeed desirable in engineering practice [49-53]. The following literature review focuses on the fundamental concepts and techniques which are employed to investigate the interaction of multiple cracks.

2.3.1. Strip yield model

The strip yield model proposed by Dugdale [54] and Barenblatt [55] in 1960s was extensively applied to solve multiple cracks problems since it is relatively easy to interpret and formulate the stresses of the cracked structures. In this model, the remote tension load is considered and both effective crack tips carry the closure yield stress (shown in Fig. 2.4). The solution can be obtained by summation of the stress intensity factors induced by the remote tensile stress and the closure yield stress at the effective crack tips (where the net stress intensity factor is zero). By using the superposition of two elastic problems, the strip yield model basically approximates the elastic-plastic fracture behaviors of multiple cracks through calculating the plastic zone size for each crack tip. Once the size of plastic zone is obtained, K and CTOD will be given through the approximation equations [54-56].

Figure 2.4 Dugdale-Barenblatt strip yield model: Slender yield strips of length ρ exist at the crack tip regions.

Various methods and techniques were proposed to solve multiple cracks problems using the strip yield model. For example, an approach using the weight function was extended to study the repeated collinear cracks in the infinite or finite width panel [57-59]. First through a series expansion of the crack opening displacements, the closed-form weight function was derived for the center crack. And then K , plastic zone size and CTOD values were obtained by the boundary element method [60]. The plastic interaction of link-up multiple cracks in an infinite sheet was studied numerically using an energy approach with strip yield model [61, 62]. In addition, the dislocation model and images method are combined to study the multiple cracks (subsurface or edge cracks) embedded in a plate subjected to out-plane shear loadings [63].

Because of the complexity of the multiple cracks problems, the above mentioned investigations are mainly implemented by numerical method. Only a small amount of 2-D configurations have been investigated analytically. Two equal length collinear cracks - simplest format of multiple cracks could be one best choice for researchers to investigate the intrinsic mechanism of the cracks' interaction in theory. Fig. 2.5 shows geometrical configuration of two collinear cracks under remote tensile loading [64]. A closed-form analytical solution for such case was originally derived [65, 66] by virtue of a complex stress approach. Later Chang and Kotousov [64, 67, 68] presented another analytical formulation by means of distributed dislocation method.

Figure 2.5 Geometric configuration of two collinear cracks [64].

2.3.2. Distributed dislocation theory

A significant breakthrough was achieved in the beginning of the 1980s by introducing the distributed dislocation method to solve the complicated crack problems in fracture mechanics. In the distributed dislocation concept, a crack is mathematically simulated by the distributed dislocation, and then the physical problem can be treated as the summation of many singular integral equations. These singular integral equations can be solved theoretically with the help of Erdogan and Gupta's method [69].

An edge dislocation is commonly used to solve the in-plane crack problems in fracture mechanics. Its schematic figure from microscopic view [70] is shown in Fig. 2.6, an extra half-plane of atoms is introduced into the crystal, resulting in the distortion nearby planes of atoms. In a closed loop $XCC'YX'$ (which is representative to illustrate the scenario in the edge dislocation), a failure displacement XX' called Burgers vector b is used to calculate the CTOD values.

Figure 2.6 An edge dislocation [70].

Based on the concept of distributed dislocation theory, a lot of research works on crack-inhomogeneity interaction in composite materials have been carried out. Erdogan [71, 72] first investigated the sub-interface crack under different traction conditions, with respect to the stress intensity factor and the propagation path. Following this work, the stress field was obtained analytically for an arbitrarily oriented crack or a crack normal to the interface [73, 74]. In addition, the sub-interface crack problem has been discussed in terms of the crack depth and the stress intensity factors using theoretical and numerical approaches. For example, using the contact zone concept, Xiao and Fan [75-77] discussed the sub-interface crack based on the research from Yang and Kim [78]. Furthermore, in contrast to the crack located entirely in one homogenous material, some major interface crack models were well defined and analytically solved in the literatures [79-81].

2.3.3. Study of multiple cracks in engineering structures

Under continuous loading conditions, small and rather closely distanced cracks can grow and coalesce into a large one which may subsequently pose a challenge to the structural reliability and safety of engineering structures [53,

82, 83]. When the distance between two adjacent cracks decreases, the interaction effect becomes increasingly significant.

In the last two decades, multiple cracks have been observed in various cases of aging marine components. For example, the stress corrosion cracking in pipelines was reported by Leis and Mohan [84]. The piloted tests were conducted on the interaction effect between coplanar cracks and irregular cracks for fatigue crack growth in 1990s [85-87]. Moreover, the fatigue growth behaviors of a steel plate with the interacting surface cracks [88-91] were investigated experimentally and numerically. In their studies, the relative spacing of cracks and the stress intensity factor were also discussed. In addition, a large crack interacting with one or several small cracks were numerically examined by Ouinas et al. [92] for the thin-wall pipelines.

Furthermore, the problem of nonplanar multiple cracks was also investigated numerically. For instance, the relationship between the interaction factor of the multiple cracks and the crack's aspect ratio was examined using finite element simulations [86, 93]. Based on a series of FE analyses of both the nonaligned and aligned multiple cracks, combination rules and reference stress method, a failure assessment diagram was proposed for multiple axial surface flaws subject to limited loads [90, 94].

2.4. Fundamentals of Fatigue

2.4.1. Introduction

Fatigue is regarded as a process of damage accumulation in a structure subjected to continuously fluctuated stress which is lower than its yield stress. Fatigue occurs in all engineering materials. The fluctuated stresses can result in local high stress in the engineering structure, and the damage will happen and increase gradually as the cyclic loading is applied continuously. Generally, the fatigue of structures subjected to cyclic loadings can be affected not only by the maximum stress, but also by the range of the applied stresses. For this reason, the stress ratio R is defined as below to distinguish the applied cyclic loading:

$$R = \frac{\sigma_{\min}}{\sigma_{\max}}. \quad (2.12)$$

Where $\sigma_{\min}$ and $\sigma_{\max}$ are minimum and maximum applied stresses, respectively.

Fatigue of metals in structures was studied at the beginning of the 19th century after the catastrophic disaster - Tay Bridge collapsed accompanied with a train during its service [95]. The bridge failure caused by the fatigue was reported by Albert [96], which also provided the possible reason for many unexpected failures in metal structures. Around two decades later, Wöhler [96] had performed a systematic experiments on railroad axles, found that the rupture would happen at stress below the yield strength when the axles was applied to cyclic loading. He also summarized the relationship between the applied stress and the “time to failure”, which is the S-N curve or called Wöhler curve. In the S-N diagram, the applied stress is defined as a function of the number of cycles. Fig. 2.7 shows typical S-N curves for 1045 steel and T6 aluminum. It is found that there is a fatigue limit for 1045 steel, whereas T6 aluminum doesn't have a fatigue limit.

Figure 2.7 S-N curves for 2014-T6 aluminum and 1045 steel.

From many steel failure events, it was found that there was a huge discrepancy between the applied load and the ultimate tensile strength of the material used. With respect to this issue, the experimental investigation conducted by Griffith in 1920s [97, 98], revealed that many microscopic cracks exist in a material, lowering the overall strength of the material. Until then, the fatigue research had been achieved a lot in the physical explanations and the predictive models. However, the influence of the existing cracks on the fatigue life was not well understood.

A major step to link the fatigue process with the cracks was made by Paris and Elber. In 1961 Paris et al. [99] found that the crack growth rate and the cyclic stress intensity factor range have an almost linear relationship on the logarithmic scale. It is the well-known Paris law, written as below,

$$da / dN = C \Delta K^m. \quad (2.13)$$

It is widely accepted to describe the fatigue mechanism for a lot of engineering materials. Later Elber [100] found that under low stress intensity factor, the fracture response of the flawed structure was almost the same with that obtained from an uncracked body in several fatigue specimens. He explained that there had a crack closure in which no fatigue crack growth occurred. The effective cyclic stress intensity factor range ΔK_{eff} was proposed to cover the crack closure effect for the metallic materials. The illustration of ΔK_{eff} is shown in Fig. 2.8. The crack surfaces will be open until the applied stress intensity factor reaches K_{op} . And thus, usually ΔK_{eff} is lower than the applied ΔK for the metallic materials.

Figure 2.8 Principle of Elber's Crack Closure Theory.

2.4.2. Fatigue process

For a better understanding of fatigue process, fatigue life generally can be divided into three main regions. They are summarized as follows.

Region I: fatigue crack initiation and threshold region.

Region II: Stable crack extension, following the Paris Law.

Region III: Unstable crack propagation until failure.

Figure 2.9 Typical fatigue crack growth rate curve of da/dN - $\log \Delta K$ [101].

The characteristic of the fatigue process curve da/dN versus $\log \Delta K$ is presented in Fig. 2.9 [101]. It can be seen that in Region I, it exhibits non-continuum behavior, and the fatigue behavior is affected by the microstructures and the remote loading. The fatigue starts with the evolution of many microscopically small cracks. The slip lines are formed at the very beginning of fatigue, and the microscopic crack or voids tend to propagate along the slip planes outwards or inwards. Generally the fatigue crack growth rate in Region I is very low, in the order of $10^{-9} m/cycle$. When the micro cracks become larger, they begin to propagate along the direction normal to the maximum tensile stress. The fatigue process moves to Region II which is mostly dominated by Mode I. In Region II, parameters such as the applied stress range, the material properties and the frequency of cyclic loading can play a significant role in the fatigue crack growth. On a microscopic scale a pattern of ripples or fatigue striations are observed. Each striation is induced by one single cycle, and denotes the successive position of the propagating crack front. The overall crack extension behavior in Region II can be characterized by the Paris Law, as pointed out in Fig. 2.9. In Region III, the fatigue crack begins to propagate significantly as the stress intensity factor approaches K_c . It is postulated that Region III behavior is from the superposition of the fracture and fatigue, less affected by the plastic zone. The total fatigue life is a summation of the cycles taken in all three regions. However, the duration of Region III is usually very short compared to the other two. Thus, only Region I and Region II are considered in estimating the fatigue life.

2.4.3. Fatigue study in offshore pipelines

Fatigue-life prediction on offshore pipelines under continuous wave/current loadings is a major issue in ensuring the integrity and reliability of offshore pipelines against catastrophic failures of piping components caused by fatigue crack growth. Up to date, fatigue crack growth of pipelines has been studied extensively through experimental tests. Considering a reel-lay installation process, the fatigue performance of the pipelines under bending was investigated experimentally. The low-cyclic loading with large plastic strains

was applied to the girth welds, and the local deformation and fatigue crack growth rate were discussed for some simple defects [102, 103]. The effect of the stress ratio on the fatigue behavior of API X65 steels was also investigated for the cyclic bending [104]. For the thin pipeline containing stress corrosion crack, the experimental study was conducted by Gamboa et al. (2008) [105] for cyclic tensile loading.

In recent years, a large amount of researches have been performed on the effect of environmental factors on the fatigue crack growth. One of these studies is the hydrogen-assist fatigue crack growth of the pipeline steels which contain and transport high pressure gaseous productions [106-109]. For API X100 pipeline steel, the experimental tests [107] showed that there was a large discrepancy in fatigue crack growth between lower stress range and higher stress range. Another important study in subsea pipelines is the effect of the sour environments on fatigue crack growth rate since the sour productions are common in oil and gas industry [110-113]. The fatigue behavior of pipeline base steel material in a sour environment was investigated by Holtam et al. [112]. It was found that in a sour environment the fatigue crack growth rate for deep cracks was significantly greater than that in air.

It is well recognized that the scale factor and the large amount of costs on the experimental set up are the major issues to conduct a full-scale experiment. Therefore, with the aid of the high computational power, the numerical simulation is an alternative way for extensive parametric studies with regard to crack shapes, crack sizes and different loadings.

As one of the major computational methods, the finite element method has been used for lots of analyses of structure reliability regarding to crack issues [114]. For the plates with surface cracks, the fatigue crack growth was predicted through a multiple degree of freedom numerical procedure [115-117]. For the straight pipeline containing a circumferentially surface crack [118], limited

number of different crack profiles were compared using FEM model to discuss fatigue crack growth.

With respect to the crack growth simulation, it tends to pose certain limitations in the topology of cracks and more severe restriction in complex geometries. To alleviate the computational difficulties in crack extensions, the extended finite element method (XFEM) [119, 120] had been proposed in the late 1990s. In XFEM, a jump function and the near-tip asymptotic functions are introduced to the displacement vector approximation [119, 121]. The outstanding advantage of XFEM is the ability to characterize arbitrary cracks in finite element models without re-meshing. With XFEM, there is no need to manage the finite element mesh grid to be adapted to the evolutions of cracks. Therefore it saves a lot of computational times on finer mesh grids ahead of the crack tips. For the applications of XFEM, The plastic collapse and the crack growth of carbon steel pipelines were investigated [122]. Under cyclic loading, the fatigue behavior of the plate with multiple minor cracks was studied by virtue of XFEM [123]. Due to its merits in numerical simulations, plenty of implementation of XFEM in the preliminary fatigue assessment can be expected to establish a consistent planning for offshore pipelines design in the near future.

2.5. Concluding Remarks

In this chapter, some fundamental concepts of fracture mechanics, including the stress intensity factor, the J -integral and CTOD, are reviewed. The commonly used approaches for fracture assessment on offshore pipelines are briefly summarized. One is the fully plastic solution for J estimation which is available as long as the stress-strain relationship can be described through a power-law expression. Its representative is EPRI methodology. The reference stress method can be utilized for the elastic-plastic materials, and thus adopted in various major design codes and standard guidelines (e.g. R6, BS7910 and DNV-RP-108). Considering the displacement-controlled situations, recently the

reference strain approach was proposed for the structures under large plastic deformation.

For the multiple cracks problems, the yield strip model is generally adopted to analyze the elastic behavior of multiple cracks with a limited plastic zone considered. The distributed dislocation approach is discussed for the study of multiple cracks located in the inhomogeneous components. Moreover, the researches performed in the engineering structures containing multiple cracks are reviewed.

Fatigue is a process of damage accumulation in a material subjected to continuously fluctuated loads. Fatigue, as one common phenomenon occurred in all engineering materials, is reviewed from the historical perspective. The fatigue process is analyzed by means of the demonstration of the typical fatigue crack growth rate curve. Last but not the least, the fatigue study in offshore pipelines is reviewed from the viewpoint of experimental tests and numerical simulations.

CHAPTER 3

Elastic-plastic Fracture Analyses of Pipeline Girth Welds with 3-D Semi-elliptical Surface Cracks

3.1. Introduction

Offshore pipelines mostly constructed by girth welding method, are the major transportation tools for crude oil and natural gases in the offshore oil/gas industry. During construction, some flaws, such as surface or embedded cracks, are inevitably introduced in the welding zones. In addition, the pipelines are subjected to large plastic deformation during the reeling installation. For instance, in the reel-up process, the plastic bending can lead to a global strain of 3% [3]. Therefore, fracture analysis of pipeline girth welds under large plastic deformation has been paid significant attention.

Literature reviews show that previous research efforts provided extensive J -integral (one of the vital fracture parameters) estimations for lots of crack configurations. However, few researches contribute to the fracture behavior's description on J -integral or CTOD of pipeline girth welds under high plastic strain conditions. Certain major issues, such as nonlinear elastic-plastic fracture mechanics theory, the nonlinear stress-strain constitutive relationships of materials under large plastic deformation, are not reflected in the current codes of practice. Moreover, our research finds that it is too conservative for the existing codes to make fracture assessment on pipelines under strain-controlled loads exceeding the yield stress. Consequently, strain-based method is highly desirable for fracture assessments on offshore pipelines under large plastic deformation.

This chapter aims at performing nonlinear elastic-plastic fracture analysis of offshore pipelines with 3-D semi-elliptical surface crack subjected to pure bending load, as well as bending combined with internal pressure. The influences of crack depth, semi-elliptical surface crack size and internal

pressure on the CTOD value are investigated. Comparisons of the fracture assessment between BS7910:2005 and the current results are made to assess the applicability of BS7910 to the scenario considered. Also, a new strain-based CTOD estimation scheme is proposed for the flawed pipelines girth welds, followed by failure assessment diagram (FAD) in the case of global strain of 3%.

3.2. Methodology

3.2.1. Geometrical configuration

The geometrical configuration of girth welded pipeline with a 3-D semi-elliptical surface crack is shown in Fig. 3.1. The average outer radius of pipeline and the wall thickness are 181.4 mm and 20.9 mm, respectively. The length of the pipeline model L is assumed to be three times the outer diameter [38], which is long enough to implement the bending load and has little impact on the numerical results. The inner wall of the pipeline is clad with corrosion resistance alloy (CRA) with a thickness of 3 mm. The outer shell is carbon steel of 17.9 mm thickness, denoted in Fig. 3.1(b). The detailed geometrical dimension of the girth weld is illustrated in Fig. 3.1(c). A circumferential semi-elliptical surface crack is located at the weldment, as shown in Fig. 3.1(d). Half length of the minor axis is a , denoting the surface crack depth. Correspondingly, half length of the major axis is b . The ratio of b/a is termed as the aspect ratio. Also in Fig. 3.1(d) the circumferential crack length $2s$ is highlighted by the bold black line.

To characterize the influence of crack geometries on the CTOD evolutions, four crack depths of 5, 6, 8, and 10 mm and five aspect ratios of 1, 2, 4, 6 and infinity are considered in this study. Thus, a total of twenty different crack configurations are considered.

Figure 3.1 Schematic drawing of the flawed pipeline girth weld (a) The geometrical dimension of the pipeline's section profile, (b) Material distribution of the pipeline, (c) Geometrical dimension of girth weld section which passes the center of the pipeline and is perpendicular to Z axis and (d) Geometries of the semi-elliptical surface crack located at the mid-plane section which passes the top of the weld cap and is perpendicular to Y axis.

3.2.2. Material model

Three different materials are involved in the girth welded pipeline. Outer base steel is made up of API 5L Grade X65. Inner CRA is a titanium-stabilized fully austenitic nickel-iron-chromium alloy with additions of copper and molybdenum. As for the weldment, it is inconel filler metal 625 which is Ni-Cr-Mo alloy. The mechanical properties of the three materials are listed in Table 1.

Table 3.1 Mechanical properties of the three materials used in the simulation.

Material model	Young's Modulus (GPa)	Poisson ratio	Yield stress (MPa)	Yield strain
Base steel	207	0.3	484	0.0023
CRA	200	0.3	80	0.0004
Weld metal	178	0.3	408	0.0023

For the base steel and the CRA, a strain hardening power law model to characterize the uniaxial true stress-strain relationship is employed as below:

$$\sigma = \begin{cases} E\varepsilon & \varepsilon \leq \varepsilon_Y \\ \sigma_Y \left(\frac{\varepsilon}{\varepsilon_Y} \right)^n & \varepsilon > \varepsilon_Y \end{cases} \quad (3.1)$$

In which 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 strain hardening coefficient. Herein, the strain hardening coefficients of the base steel and the CRA are 0.05 and 0.26, respectively.

For the weld metal, the uniaxial stress-strain relationship is assumed as the bi-linear one, expressed by the following,

$$\sigma = \begin{cases} E\varepsilon & \varepsilon \leq \varepsilon_Y \\ K(\varepsilon - \varepsilon_Y) + E\varepsilon_Y & \varepsilon > \varepsilon_Y \end{cases}, \quad (3.2)$$

where K is the strength coefficient. The value of 1538.6 MPa is taken for K in this study. The uniaxial stress-strain curves for the three materials are shown in Fig. 3.2. It should be noted that the solid lines represent the results calculated by Eq. (3.1) or Eq. (3.2), and the symbols are from the experimental data of the uniaxial tension tests [47].

Figure 3.2 Uniaxial stress-strain curves for the three materials involved in this study.

3.2.3. FE modeling

The finite element simulations are carried out with the ABAQUS 6.9 code. Considering symmetry, only one-quarter of the pipeline model is built with eight node 3-D elements (ABAQUS type C3D8R). The overview of meshed pipeline model is displayed in Fig. 3.3(a). The boundary conditions are set as follows:

- I) One end of the pipeline segment is constrained in Y direction except the cracked surface.
- II) The two surfaces, of which the normal direction is Z axis, are not allowed to move along the axial direction (Z-axis).

A typical finite element mesh around the cracked zone is shown in Fig. 3.3(b). The high element concentration in the crack tip region is observed from this figure. In the case of large scale plastic deformation, a blunt crack tip model is recommended in order to reduce the singularities near the crack tip and to guarantee fast convergence [45]. Here a small hole with the radius of 0.01 mm is assigned around the crack tip. A focused spider web mesh is applied to the blunt crack tip zone (shown in Fig. 3.3(c)). The size of the smallest element is in the order of $0.001 \times a$. There are 15 rows of elements along the crack line in total. The number of elements varies from 27,000 to 50,000, depending on the crack size.

(a)

(b)

(c)

Figure 3.3 Details of the mesh grids used in the simulation: (a) The overview of meshed pipeline model, (b) High element concentration around the cracked zone and (c) Focused spider web mesh for the blunt crack tip.

The material behavior for the carbon steel, the CRA and the weld material is governed by J2 flow theory of isotropic hardening plasticity. The ‘Nlgeom’ option in ABAQUS code is turned on to include the nonlinear geometry since the large deformation is involved in the analysis.

Figure 3.4 Two different loading conditions: (a) Pure bending load and (b) The bending load combined with internal pressure.

Two different loading conditions are considered, as illustrated in Fig. 3.4. The first loading type is the pure bending load. In order to apply it, rotation is imposed on a reference point tied to the un-cracked end of pipeline through a set of Kinematic Coupling relationship [124]. This operation constrains the un-cracked end of pipeline remaining in plane during the rotation. According to Euler-Bernoulli beam theory, the rotational angle [5, 42, 44] applied to the reference point is determined by:

$$R_x = \frac{2\varepsilon_g L}{D} . \quad (3.3)$$

Where ε_g is the global strain, L is the length of pipeline, D is the outer diameter of pipeline and R_x is the rotational angle in radian. Since offshore pipelines may experience large plastic strain up to 3% [3], the maximum global strain is assumed as 3% in this study.

The other loading condition is the biaxial loading - the bending load combined with internal pressure. In this case, the loading process includes two steps: the internal pressure is exerted on the inner surface of the pipeline at the first step; and then the rotational angle is imposed gradually. The magnitude of the internal pressure is kept constant as 20 MPa. It should be noted that the hoop stress due to the internal pressure (20 MPa) is equal to 153.6 MPa, which is about 32% of base carbon steel material's yield strength.

3.3. Fracture Response of Pipeline Girth Weld with Semi-elliptical Surface Crack

Based on the numerical results, the influences of crack geometrical properties and loading conditions on the CTOD evolutions are investigated in this section.

3.3.1. Evolution of CTOD under pure bending

The curves of CTOD values against the global strains for various aspect ratios are shown in Fig. 3.5 for different crack depths. The following observations can be made:

- 1) For small strain levels ($\varepsilon < 0.003$), the CTOD-strain curve is almost linear, which means that the plasticity is restricted and the global deformation is within the elastic range. However, at large plastic strains, CTOD values increase significantly.
- 2) Given the crack depth and the global strain, CTOD increases gradually and nonlinearly with growth of the aspect ratio b/a . In the case of small aspect ratios (i.e. b/a is 1, 2, 4), CTOD increases more significantly.

When the aspect ratio b/a is no less than 6, the increase in CTOD value is relatively slow.

A possible explanation for these observations could be made: the value of CTOD increases considerably if the cracked area develops quickly. All these observations are in a good agreement with those in literature [39, 45, 47].

(a)

(b)

(c)

(d)

Figure 3.5 Curves of CTOD against global strains for different aspect ratios under pure bending load: (a) $a=5mm$, (b) $a=6mm$, (c) $a=8mm$ and (d) $a=10mm$.

Given certain crack shapes, comparisons of CTOD for different crack depths are shown in Fig. 3.6. It can be observed that in Fig. 3.6 (a), (b) and (c), CTOD values for deep cracked pipes ($a>6mm$) increase significantly as the global strain develops. While for the shallow cracks, the evolution of CTOD versus the global strain is less considerable, even for the case with the largest aspect ratio (b/a is infinite).

(a)

(b)

Figure 3.6 Comparisons of CTOD values for different crack depths: (a) $b/a = 1$,
(b) $b/a = 4$ and (c) b/a is infinite.

To further explore the influence of the crack depth on CTOD values, the curves of CTOD with the crack depths for different aspect ratios is plotted in Fig. 3.7, in which the global strain is assumed as 3%. It is noticed that for each given aspect ratio, CTOD increases with the increase of crack depth. Higher CTOD value is obtained for deep cracks with a large aspect ratio, which is consistent with the observation of [125].

Figure 3.7 Curves of CTOD versus crack depths under pure bending load at the global strain of 3%.

3.3.2. Evolution of CTOD under biaxial loading

It should be noted that the value of CTOD strongly depends on the loading path according to the theory of plasticity. Three possible loading sequences for the biaxial loading - bending combined with internal pressure are considered. In loading path 1, first the internal pressure (20 MPa) is exerted on the pipe, followed by the rotational angle for bending. In loading path 2, the rotation is applied on the pipeline before internal pressure. The last is loading path 3, in which the bending load and the internal pressure are simultaneously imposed.

For the case of crack depth of 10 mm with the largest aspect ratio, the evolutions of CTOD with the global strains are shown in Fig. 3.8 for the three loading paths. It can be found that the maximum CTOD is obtained through loading path 1. In contrast, loading path 2 leads to the minimum CTOD. Considering the worst scenario first, only loading path 1 is adopted for further parametric study. Meanwhile, this analysis may explain why in practice the

frequent loading sequence for the biaxial loading is loading path 1 mentioned in these literatures [45, 126].

Figure 3.8 Curves of CTOD versus strains for the three loading paths.

(a)

(b)

(c)

(d)

Figure 3.9 Comparisons of CTOD values for different crack depths under the two loading conditions: (a) $b/a = 1$, (b) $b/a = 2$, (c) $b/a = 4$, (d) $b/a = 6$ and (e) b/a is infinite.

The comparisons of CTOD between the pure bending load and biaxial loading are shown in Fig. 3.9. It is found that for the biaxial loading cases, the similar trend as that of the pure bending load can be observed. However, the curves from biaxial loadings become much steeper due to the internal pressure applied.

At the larger plastic strains ($\epsilon > 0.003$), CTOD values for biaxial loading are much greater than those obtained through the pure bending load. Fig. 3.9(a) shows the CTOD values with global strains for different crack depths when the aspect ratio b/a is 1. For all the cases except the shallowest crack ($a=5\text{mm}$), the value of CTOD increases significantly due to the internal pressure. Moreover, from Fig. 3.9(b-e), it is observed that the CTOD value almost increases by twice in the case of biaxial loading.

All these observations indicate that at any strain level, the increase in CTOD with hoop stress is more obvious compared with the pure bending load. The

possible explanation for the above results could be made below: in the viewpoint of energy conservation, the bending load combined with internal pressure can lead the effective yield stress lower, resulting in a smaller plastic zone size. In other words, the internal pressure applied on the inner surface of the pipeline introduces the hoop stress and consequently brings about larger plastic deformation which in turn leads to higher CTOD values. The similar result is also observed under both tension loading and bending of cracked pipelines [38, 39, 47, 126].

3.4. Comparisons of Fracture Assessment Based on BS7910:2005 and Current Results

3.4.1. Interpretation of fracture assessment of the pipeline with surface cracks in BS7910:2005

Based on BS7910:2005, three levels are provided for fracture assessment of the flawed pipelines. Level 2B assessment procedure is applicable to the cases in which material specific stress-strain relationship is available. In view of all materials properties given in this study, Level 2B is adopted here. A brief summary of the fracture assessment provided in BS7910 is presented to compare with the current results.

The applied stress intensity factor K_I is expressed in the following form,

$$K_I = (Y\sigma)\sqrt{\pi a} = Mf_w(M_m P_m + M_b P_b)\sqrt{\pi a} . \quad (3.4)$$

Where M is the bulging correction factor, f_w is the correction term of the stress intensity factor for elliptical flaw, M_m and M_b are the stress intensity magnification factors, P_m is the primary membrane stress, P_b is the primary bending stress, and a is crack depth. Both M and f_w are presumed as 1. M_m and M_b are determined by the pipe configuration and crack size.

The primary membrane stress P_m and primary bending stress P_b are determined by the stress analysis of the unflawed pipeline under the pure bending load. They are expressed as,

$$\begin{cases} P_m = (\sigma_1 + \sigma_2) / 2 \\ P_b = (\sigma_1 - \sigma_2) / 2 \end{cases} \quad (3.5)$$

In which σ_1 and σ_2 are the stresses from the linearized distribution of the actual stress along the pipeline's wall thickness. Fig. 3.10 shows the actual stress distribution along the wall thickness of unflawed girth weld when the global strain is 3%. In the abscissa axis, the value "0" represents the outer surface of pipeline, and the value "24.2" represents the inner surface of pipeline. Take the crack depth of 8 mm as an example, the stress at the crack depth of 8 mm is employed for σ_1 and σ_2 based on the principle of the linearized stress distribution. And consequently, the primary membrane stress P_m and primary bending stress P_b can be obtained for each global strain considered.

Figure 3.10 The actual stress distribution along the wall thickness of unflawed girth weld when the global strain is 3%.

Once the stress intensity factor K_I is determined, the corresponding CTOD value denoted by δ_I can be obtained from the classical formulas [18] given as follows.

$$\delta_I = \frac{K_I(1-\nu^2)}{E\sigma_Y} \quad (3.6)$$

If a flaw is acceptable in the weld metal based on BS7910, the assessment criterion is written as below,

$$\frac{\delta_I}{\delta_{mat}} \leq \left(\frac{E\varepsilon_{ref}}{L_r\sigma_Y} + \frac{L_r^3\sigma_Y}{2E\varepsilon_{ref}} \right)^{-1} \quad (3.7)$$

Where δ_{mat} is the required CTOD fracture toughness, δ_I is the CTOD value calculated by Eq. (3.6), σ_Y is the yield stress, $L_r (= \sigma_{ref} / \sigma_Y)$ is the ratio of reference stress to yield stress, σ_{ref} is the reference stress, and ε_{ref} is the reference strain calculated at σ_{ref} based on the stress-strain relationship of the weld metal (Eq. (3.2)).

For a pipeline with external surface flaws oriented circumferentially, the reference stress is determined by applied stress, the pipeline geometry and the flaw size, expressed as the following equation:

$$\sigma_{ref} = \frac{P_m \{ \pi(1 - a/B) + 2(a/B) \sin(s/r) \}}{(1 - a/B) \{ \pi - (s/r)(a/B) \}} + \frac{2P_b}{3(1 - a/B / (1 + B/s))^2} \quad (3.8)$$

In which B is the wall thickness of the weld pipe, r is the mean shell radius of pipe. In this study $B = 24.2mm$ and $r = 172.6mm$. s is the half crack length, varying with different crack depths.

Finally, derived from Eq. (3.7), the required CTOD fracture toughness δ_{mat} for the weld metal should be satisfied by the followings:

$$\delta_{mat} \geq \delta_I \cdot \left(\frac{E\epsilon_{ref}}{L_r \sigma_Y} + \frac{L_r^3 \sigma_Y}{2E\epsilon_{ref}} \right). \quad (3.9)$$

Therefore, the minimum fracture toughness for the weld metal represented by CTOD value can be estimated through Eq. (3.9).

3.4.2. Comparisons of the required fracture toughness between BS7910:2005 and current results

For a tolerable crack in the flawed pipeline under pure bending load, the required CTOD fracture toughness obtained from BS7910 are compared with the current FE results. The curves of CTOD toughness requirement against global strains are shown in Fig. 3.11 for all the four different crack depths considered. The aspect ratio of b/a is taken to be infinite for all crack depths.

(a)

(b)

(c)

(d)

Figure 3.11 Comparisons of the CTOD toughness requirement based on BS7910:2005 and current results for a semi-elliptical surface crack in the pipeline girth welds: (a) $a=5\text{mm}$, (b) $a=6\text{mm}$, (c) $a=8\text{mm}$ and (d) $a=10\text{mm}$. The aspect ratio of b/a is infinite for these four cases under pure bending load.

It can be found in Fig. 3.11(a) that for the crack depth of 5mm, the values of CTOD toughness requirement from BS7910 are lower than those from FE results at any strain level. It means that BS7910 gives an underestimated value for the shallower crack. While from Fig. 3.11(b-d) it is observed that the values of CTOD toughness requirement from BS7910 are much greater than those from FE results. For the crack depth of 6mm, the CTOD toughness requirement calculated by Eq. (11) is around 0.6mm, 30% higher than that from the numerical results when the global strain is 3%. Moreover, for the crack depth of 10mm, the value of the required CTOD toughness requirement from BS7910 is five times greater than that from our results.

The possible reason for the significant difference in CTOD toughness requirement between BS7910 and current FE results is that the weld metal has

under-matched yield stress. With the existing of large crack, the stress accumulated in the weldment results in higher P_m and P_b . Also the remaining ligament of the backing carbon steel decreased with the increasing crack size. The effect of lower CRA stress-strain curve becomes significant for the girth welded pipelines. Thus, the required CTOD toughness drastically increases. In summary, the BS7910 code makes over-conservative fracture assessment for pipeline girth welds with an externally circumferential crack under large plastic bending, especially for the deep cracked pipelines.

3.5. New Strain-based CTOD Estimation Scheme

3.5.1. Description of the CTOD estimation equation

Response surface methodology (RSM) was introduced by Box & Wilson (1951) [127] aiming to derive an optimal response through a series of experimental data. For many practical applications, the performance functions which relate the dependent predictions to the involved variables, may not be available in the explicit form. RSM, adopting a square or cubic polynomial fitting, is used to approximate the implicit performance functions. For instance, Chiodo & Ruggieri (2010) [25, 35] used a cubic polynomial fitting to obtain the *J-integral* and CTOD estimation equations for the pipes with surface cracks.

Response surface methodology (RSM) is adopted to construct the prediction model of CTOD in this section. A polynomial formula obtained to calculate normalized CTOD is assumed as below,

$$\frac{CTOD}{a} = f_0 \cdot \varepsilon^2 + f_1(a, b, t) \cdot \varepsilon + f_2(a, b, t). \quad (3.10)$$

Where ε is the global strain, f_0 is a constant parameter, f_1 and f_2 are dimensionless functions of crack depth, aspect ratio and the wall thickness. The expressions of f_1 and f_2 in terms of a , b/a and t are represented as,

$$f_1(a, b, t) = A_0 + A_1 \frac{a}{t} + A_2 \log(b/a), \quad (3.11a)$$

$$f_2(a, b, t) = B_0 + B_1 \frac{a}{t} + B_2 \log(b/a) + B_3 \left(\frac{a}{t}\right)^2 + B_4 (\log(b/a))^2 + B_5 \frac{a}{t} \cdot \log(b/a). \quad (3.11b)$$

In which a , b/a and t are the crack depth, aspect ratio and wall thickness of the weld, respectively. The coefficients f_0 , $A_0 \sim A_2$ $B_0 \sim B_5$ are to be determined through the least-square method based on our simulation results. These coefficients are listed in Table 3.2 and Table 3.3 for the two loading conditions. It should be noted that Eq. (3.10) is specific to the current geometry and material properties of the particular pipeline, and it is valid for a stationary crack. Crack extension by ductile tearing is not considered here.

Table 3.2 RSM coefficients of f_0 and $A_0 \sim A_2$ in Eq. (3.10) and Eq. (3.11a).

B.C.	A_0	A_1	A_2	f_0
Pure bending	0.647	7.935	0.8789	-41.48
Biaxial loading	1.519	11.245	1.6586	-76.53

Table 3.3 RSM coefficients of $B_0 \sim B_5$ in Eq. (3.11b).

B.C.	B_0	B_1	B_2	B_3	B_4	B_5
Pure bending	0.0032	-0.0981	0.0260	0.1196	-0.0245	0.0841
Biaxial loading	-0.0157	-0.0297	0.0513	-0.0030	-0.0398	0.1053

A comparison of CTOD values calculated by Eq. (3.10) and the current results is shown in Fig. 3.12, for the various deep cracks under two different loading conditions. In general, the slight discrepancies in CTOD values show that the good agreement with current results is obtained for all the crack depths

considered. From Fig. 3.12 it is obvious that the proposed estimation scheme (Eq. 3.10) can provide an accurate prediction on the CTOD evolution.

(a)

(b)

(c)

(d)

Figure 3.12 Comparison of CTOD values between current results and the predicted results by RSM for different crack depths: (a) $b/a=2$ under Pure bending, (b) $b/a=2$ under Biaxial loading, (c) b/a is infinite under Pure bending and (d) b/a is infinite under Biaxial loading.

3.5.2. Parametric sensitivity analysis

From the previous analyses, it is noted that the crack depth a and the aspect ratio b/a are two key influencing parameters in determining CTOD values. To assess the relative importance of these two parameters, an intermediate parameter η is introduced, defined as

$$\alpha = \alpha_{\min} + \eta(\alpha_{\max} - \alpha_{\min}), \quad \eta \in [0,1] . \quad (3.12)$$

In which α could be a or b/a . η changes from 0 to 1, making α change from the minimum to the maximum. η_a is the sensitivity factor for the crack depth, making a change from 5 to 10 mm accordingly. In the similar way, $\eta_{b/a}$ is the sensitivity factor for the aspect ratio. $\eta_{b/a}$ varies from 0 to 1, making b/a change from 1 to 6.

Figure 3.13 Parametric sensitivity analyses of η_a and $\eta_{b/a}$ on the CTOD evolution.

Parametric sensitivity analyses of η_a and $\eta_{b/a}$ on the CTOD evolution are shown in Fig. 3.13 when the global strain is assumed as 3%. The two different loading conditions are considered here. It is clearly observed that under the

same loading condition, for instance, in the cases of pure bending, the curve of η_a is much steeper than the curve of $\eta_{b/a}$. Additionally, it is noticed that the curve of η_a from the biaxial loading has a little larger gradient than that from the pure bending. These analyses indicate that under a given the loading condition, the CTOD value is greatly influenced by the crack depth.

3.5.3. Strain-based failure assessment diagram (FAD) for the flawed pipelines subjected to large global strain (3%)

In offshore industry, the possible failure of the critical components is estimated in advance to provide guidance for inspection programs using the existing evaluation standards or codes. A suitable fracture assessment diagram would ensure a safe operation of offshore pipelines. It would avoid expensive over-conservative inspections. Based on the numerical simulations and analyses, new strain-based failure assessment diagram (FAD) is established for the flawed pipelines under large plastic deformation. In the present work, the criterion that a flaw can be accepted is expressed as,

$$\text{CTOD} \leq \delta_{\text{critical}} \cdot \quad (3.13)$$

In which δ_{critical} is the CTOD fracture toughness (in mm) for the material considered. When the crack depth, the aspect ratio and the global strain are given, the CTOD value can be obtained through the proposed equation - Eq. (3.10).

Figure 3.14 Failure assessment diagram (FAD) for the cracked girth welded pipelines: (a) Pure bending and (b) Biaxial loading, when the global strain is assumed as 3%.

According to the experimental data provided by Det Norske Veritas Pte. Ltd. (DNV), the minimum CTOD value of 0.8 mm was achieved for the weld metal.

The CTOD value reported by the experiment corresponds to maximum load plateau on the Load vs. CMOD curve. The new strain-based failure assessment diagrams (FADs) are plotted in Fig. 3.14(a) and (b) for the flawed girth welded pipelines under pure bending and biaxial loading, respectively. The vertical axis is the crack depth a , and the horizontal axis is the inverse of aspect ratio, a/b , varying from 0 to 1. In the figure, the bold black line is the critical assessment line. A surface crack in the weldment can be considered acceptable if it lies in the shadow area called the acceptable region. These two FADs can be used for the preliminary fracture assessment of the girth welded pipelines with a surface crack.

3.6. Concluding remarks

In this chapter, the fracture response (CTOD) of a pipeline girth weld with a semi-elliptical surface crack under large plastic bending has been analyzed using 3-D nonlinear elastic-plastic finite element models. The influences of the crack depth, aspect ratio and the internal pressure on the evolution of CTOD are investigated in details. The main conclusions are drawn as follows.

- 1) At a given global strain, the CTOD value increases with the increasing crack depth and the aspect ratio. The CTOD value initially evolves slowly for a shallow crack, and then increase significantly for a deep crack with large aspect ratio values.
- 2) The internal pressure applied on the inner surface of the pipeline induces hoop stress and consequently results in higher CTOD values.
- 3) The BS7910:2005 code produces over-conservative predictions, especially for the pipeline girth weld with deep surface cracks.
- 4) A polynomial equation is proposed to estimate the CTOD value for a semi-elliptical surface flaw in the weld, under pure bending as well as biaxial loading. This equation is very user-friendly for engineers in offshore industry.

- 5) New strain-based failure assessment diagrams (FADs) are established for the flawed pipelines under large plastic strain of 3%.

It is noted that the input parameters to the CTOD estimation scheme are based on the particular geometrical configuration and material properties of the specific pipeline and crack dimensions. Thus, it is imperative that certain limitations apply to the FADs proposed for fracture assessments. However, the proposed CTOD estimation scheme provides an alternative solution for such case where the weld metal has under-matched strength as this is not covered by any codes and standards so far.

CHAPTER 4

Elastic-plastic Fracture Analyses of Pipeline Girth Welds with 3-D Elliptical Embedded Cracks

4.1. Introduction

During construction, weldments may contain surface and embedded imperfections and defects. As an extension, the fracture behaviors of offshore pipelines with an elliptical embedded crack under large plastic strains are investigated through 3-D nonlinear elastic-plastic fracture analyses. One difference between the current problem and the one in the previous chapter lies in the more complicated geometry models. The FE meshing on the interest region around the two crack tips is highly challenging. A sub-modeling technique is adopted here to accommodate the dense mesh elements requirement. Besides, as the flawed weldment is of our main concern, results are more dedicated for the situations with strength mismatch. The influence of strength mismatch between the base carbon steel and the weld metal on the fracture response represented by CTOD is one of key issues in many practical engineering problems.

In this chapter, extensive 3-D numerical simulations are performed for the girth welded pipelines with elliptical embedded cracks. The influences of crack depth, embedded crack size and internal pressure on the CTOD evolutions are investigated under the pure bending as well as the biaxial loadings. Comparisons of the fracture assessment between BS7910:2005 and the current FE results are presented for offshore pipelines with an elliptical embedded crack. Based on the reference strain method, new strain-based CTOD estimation equation is proposed through the power function of the global strain. Furthermore, failure assessment diagrams (FADs) are established for the preliminary fracture assessment on pipelines girth weld with an elliptical embedded crack.

4.2. Methodology

4.2.1. Geometrical configuration of girth welded pipeline with an elliptical embedded crack

The geometrical configuration of the girth welded pipeline with an elliptical embedded crack is shown in Fig. 4.1. The detailed dimension of the weldment with an elliptical embedded crack is presented in Fig. 4.1(c). The crack height of the embedded crack is denoted by $2a$ and highlighted by the bold black line. The elliptical embedded crack's location and geometry are illustrated in Fig. 4.1(d). The crack depth measured from the weldment cap to the closer crack tip is represented by p . As for the crack length, it is the major axis length of the elliptical crack, denoted by $2c$. The ratio c/a is termed as the aspect ratio. In this study, three crack depths of 3, 5, 7 mm, three crack heights of 6, 8, 10 mm, and aspect ratios of 1, 3, 5 are considered to characterize the influence of crack geometries on the CTOD evolutions.

Figure 4.1 Schematic drawing of the pipeline girth weld with an elliptical embedded crack: (a) Geometric dimension of the pipeline's section profile, (b) Material distribution of the girth welded pipeline, (c) Dimension of the weldment with an embedded crack, and (d) Geometry of the elliptical embedded crack.

4.2.2. FE modelling with sub-modeling technique

Finite element simulations are carried out through ABAQUS 6.10 code. Considering symmetry, one-quarter of the pipeline model is built using eight node 3-D elements (ABAQUS type C3D8R). The sub-modeling technique [128] is adopted to simulate the large deformation of the pipeline with an embedded crack, based on the following two reasons: first, the elements near the crack front generally have a high aspect ratio, and even some near the crack tip are distorted seriously, which will influence the stress field results. Second, a large amount of elements in one model are required to construct two crack tip regions due to the embedded crack, introducing more difficulty in meshing.

The basic concept of the sub-modeling technique is to perform a transition from the global model to sub model. It is an approach to take the interested domain out of the entire model, control and improve its mesh quality, in order to produce accurate and stable numerical results. The premise of adopting this

technique is that the sub-model boundary must be defined far away from the crack tip zone. The sufficient accuracy can be accomplished through adopting coarse mesh in the global model while using finer mesh in the sub-model.

Extensive studies have been performed for sub-modeling technique in linear elastic problems, such as calculating the stress intensity factors [129-131]. However, few literatures have focused on the nonlinear elastic-plastic fracture mechanics [132]. The sub-modeling with its ability to generate the dense mesh grids for the two crack tips, is employed to investigate the fracture response of pipeline girth weld with an embedded crack.

(a)

(b)

(c)

(d)

Figure 4.2 (a) Overview of the global meshed pipe, (b) Geometry of the sub-model, (c) Focused spider-type mesh for crack tip, and (d) Mesh grid of the half embedded crack in the cross-section plane.

An overview of the global meshed pipeline is shown in Fig. 4.2(a). The sub-model boundaries should be selected close enough to the crack region, but so far enough from the crack tips that the stresses in the boundaries will not be influenced by the crack. The sub-model size $2s$ in the longitudinal direction (Y-axis) is assumed to be two times as the length of the largest semi-elliptical crack $2c$, i.e. $2s = 2 \times 2c = 100\text{mm}$ [129]. Geometry of the sub-model is shown in Fig. 4.2(b).

In the analysis of large plastic deformation, a blunt crack tip model is recommended in order to avert the singularities near the crack tip and ensure fast convergence [18, 133]. Hence a small hole of radius 0.01 mm is retained around the crack tip. A focused spider web mesh is applied to the blunt crack tip zone shown in Fig. 4.2(c). As mentioned previously, high mesh density is used to study the crack tip field in the sub-model. For example, along the crack length the maximum number of rows is up to 45, as shown in Fig. 4.2(d). Moreover, the size of the smallest element around the blunt crack tip is on the order of $0.001 \times a$. Thus, the number of elements in the sub-model varies from 20,000 to 50,000, depending on the crack size.

In both the global model and the sub-model, J2 flow theory of isotropic hardening plasticity is assumed for the material behaviors of the carbon steel, the CRA and weld metal. The 'Nlgeom' option is activated to include the feature of the non-linear geometry in the simulations.

Two different loading conditions are applied on the global model. They are pure bending load and the biaxial loading. The sub-modeling computational procedure for the loading transition follows: the numerical solutions for the cracked pipe are obtained by the global analysis (the embedded crack with a coarse mesh), and then the results obtained from the global model are interpolated on the sub-model boundaries and are presumed as boundary conditions for the sub-model to simulate the local model with a much finer mesh grid. It is worthy noting that the internal pressure for the sub-model is still

imposed on the inner surface of the pipeline. The magnitude of the internal pressure is taken as 20 MPa , which induces the hoop stress of 153.6 MPa .

4.3. Fracture Response of Pipeline Girth Weld with Elliptical Embedded Crack

In this section, firstly the influences of the crack geometry parameters on the CTOD evolutions are investigated. Considering the strength of weldment, the effect of strength mismatch between the base carbon steel and weldment on the fracture behavior is discussed for the girth welded pipelines. Following that, the CTOD evolutions are studied for the elliptical embedded cracks subjected to biaxial loadings.

4.3.1. Influence of crack geometry on the CTOD evolutions under pure bending

Fig. 4.3 (a) shows the deformation of the embedded crack after loading. Here the CTOD value of the left crack tip is denoted by CTOD_L, while the CTOD value of the right crack tip is represented by CTOD_R. For instance, half CTOD value of the right crack tip (CTOD_R) is illustrated in Fig. 4.3(b).

Figure 4.3 (a) The deformed embedded crack with two crack tips after loading and (b) Illustration of the half CTOD measurement.

It is found that CTOD_R is greater than CTOD_L for all cases under the pure bending load. The same trend is also observed in the biaxial loading cases. Fig.4.4 shows two cases in which both CTOD values with different geometrical parameters are plotted. It is clearly observed that regardless of the deep or shallow embedded cracks, CTOD_R is greater than CTOD_L at any strain level considered. For safety purpose, the worse scenario should be considered in practice. Therefore, only the CTOD value at the right crack tip, i.e. CTOD_R is considered in the following analyses.

(a)

(b)

Figure 4.4 Comparisons of the CTOD values at two crack tips for different crack heights (a) crack depth $p=7\text{mm}$ and $c/a=5$, and (b) crack depth $p=3\text{mm}$ and $c/a=3$.

Curves of CTOD values versus the global strains for different crack heights and the aspect ratios are presented in Fig. 4.5. It can be found that for the given crack depth and crack height, the CTOD values in the case with aspect ratio

$c/a > 1$ are much larger than the case with aspect ratio $c/a = 1$, indicating that the pipeline containing an elliptical embedded crack have larger CTOD values, compared to the pipeline with a circular crack. In addition, given the aspect ratio, comparison of the curves of $a=3\text{mm}$ and $a=5\text{mm}$ shows that the higher crack height is, the larger CTOD value is at any strain level.

(a)

(b)

Figure 4.5 Curves of CTOD values versus global strains under pure bending: (a) crack depth $p=3\text{mm}$, (b) crack depth $p=5\text{mm}$, and (c) crack depth $p=7\text{mm}$.

To examine the influence of the crack height on CTOD values, the curves of CTOD with the half crack height for different crack depths are plotted in Fig. 4.6(a), in which the global strain is assumed as 3% and the aspect ratio c/a is 5. It is obvious that for the three crack depths, the CTOD value increases significantly with increasing the half crack height. Moreover, the curves of CTOD against the crack depth for different crack heights are illustrated in Fig. 4.6(b). It is found that all the three curves have a very gentle gradient, i.e. given the embedded crack size, the CTOD value increases slowly even when the crack depth increases considerably. Comparison of Fig. 4.6(a) and (b) indicates that the crack height plays a more significant role in the CTOD evolutions.

(a)

(b)

Figure 4.6 (a) Curves of CTOD versus half crack heights and (b) Curves of CTOD versus crack depths, for pure bending cases at the global strain of 3%.

4.3.2. Influence of strength mismatch

The strength mismatch between the base material of the pipeline and the weld material plays a significant role in the fracture behavior of the flawed girth welded pipelines (as pointed out in DNV-OS-F101[3]). In order to investigate the influence of the strength mismatch, two different base materials are introduced for the pipelines. The yield stress of current weld metal is 408 MPa, lower than the yield stress of base carbon steel (whose value is 484 MPa). That is, the weld has under-matched strength. Thus the two more yield stresses of base material are assumed to be 408 MPa and 320 MPa, respectively. The corresponding uniaxial stress-strain relationship still follows Eq. (3.1) in which the Young's modulus E is 207 GPa and the hardening exponent n is 0.05. Meanwhile, the mechanical properties of the weld metal are kept as constants in all simulations. Consequently, a total of three scenarios of strength mismatch are considered here. In other words, the weld metal has the under-matched (base carbon steel with yield stress of 484 MPa), even-matched (base material with yield stress of 408 MPa) and over-matched strength (base material with yield stress of 320 MPa). The stress-strain curves for the three base materials are plotted in Fig. 4.7.

Figure 4.7 Stress-strain curves for the three different base materials considered.

For an elliptical embedded crack with crack height $2a$ of 8 mm, the aspect ratio c/a of 5 and the crack depth p of 7 mm, the CTOD evolution with global strain for the three strength mismatch cases is shown in Fig. 4.8. It is observed that given the strain level, the under-matched case produces the highest value of CTOD while the over-matched case has the minimum CTOD. It suggests that under the same conditions, the weld with under-matched strength is more susceptible to induce the significant fracture response, compared to other two strength mismatches. The under-matched case is also the emphasis of the current study.

Figure 4.8 Effect of strength mismatch for the pipeline with a large elliptical embedded crack subjected to pure bending load.

4.3.3. Influence of internal pressure

Evolutions of CTOD with the biaxial loading are investigated for the different embedded crack geometrical configurations. It should be noted that the hoop stress of the cracked pipeline due to the internal pressure (20 MPa) is taken as $\sigma_h \approx 153.6 \text{ MPa}$, which is about 32% of the base carbon steel's yield strength.

(a)

(b)

(c)

Figure 4.9 Comparisons of the CTOD evolutions for pure bending load and biaxial loadings: (a) crack depth $p=3\text{mm}$, crack height $2a=6\text{mm}$, (b) crack depth $p=5\text{mm}$, crack height $2a=8\text{mm}$, and (c) crack depth $p=7\text{mm}$, crack height $2a=10\text{mm}$.

In Fig. 4.9, the CTOD values obtained from pure bending and biaxial loading are compared for different crack depths and crack heights. Generally for the biaxial loading cases (denoted by the solid lines), the curves are steeper as a result of the internal pressure applied. With the aspect ratio $c/a=1$, the differences of CTOD values between the two cases obtained from the biaxial loading and the pure bending are relatively small, indicating that the internal pressure plays an insignificant role on the evolutions of CTOD when the crack shape is circular. In addition, Fig. 4.9(a) displays the similar trend as that of Fig. 4.9(b). From the both figures it is noticed that the CTOD values of the larger crack in red dashed line ($c/a=5, P=0\text{MPa}$) are lower than those of smaller cracked pipe under biaxial bending ($c/a=3, P=20\text{MPa}$). Moreover, for the pipe with crack depth of 7 mm, the biaxial loading results in larger CTOD, and the maximum value can be up to 1.6 mm when the crack height $2a$ is 10 mm.

Under the two different loadings, Fig. 4.10 (a) gives the curves of CTOD versus half crack height for different crack depths when the global strain is taken as 3% the aspect ratio c/a is 5. It is observed that for the crack depth $p=3\text{mm}$, the CTOD value obtained from the case with P is much larger than that from the case without P , at the given the half crack height. The similar trend is found for the crack depth $p=7\text{ mm}$. It indicates that for the given embedded crack under large plastic strains, the fracture response in terms of CTOD is largely enhanced by the biaxial loadings due to the internal pressure.

In addition, the curves of CTOD against the crack depth for two different crack sizes are plotted in Fig. 4.10(b) for both loadings. Each curve has a very gentle slope. In particular, the line for crack height of $a=5\text{ mm}$ under the case with P is almost a straight line, i.e. the crack depth has a minimum effect on the CTOD values for the large elliptical embedded crack under the biaxial loading.

(a)

(b)

Figure 4.10 (a) Curves of CTOD versus half crack height for two different crack depths and (b) Curves of CTOD versus crack depth for two different crack sizes, at the global strain of 3%.

All these observations indicate that the increase of CTOD with the hoop stress is more significant compared to the cases under pure bending load. This result can be explained as follows. The internal pressure applied on the inner surface of the pipeline introduces the hoop stress, resulting in a smaller plastic zone size. This, in turn, reduces the flaw tolerance capacity and makes the pipeline more vulnerable to be broken. In short, the internal pressure applied on the girth welded pipeline induces higher CTOD values.

4.4. Comparisons of Fracture Assessment between BS7910:2005 and Current Results

The procedure of the fracture assessment based on BS7910 for an elliptical embedded crack is similar with the description from Section 3.4. To avoid the redundancy, only the differences are pointed out in the followings.

First of all, the determination on the two parameters σ_1 and σ_2 is different. As written in Eq. (3.5), σ_1 and σ_2 are the stresses from the linearized distribution of the actual stress along the wall thickness of the pipeline. For example, in the case of the crack depth of 7 mm and crack height of 10 mm, the determination for σ_1 and σ_2 based on the linearized stress distribution is shown in Fig. 4.11 when the global strain is 3%. In this figure, the blue solid curve shows the actual stress distribution along the wall of uncracked girth weld, and the linearized stress distribution described in BS7910 is plotted in the blue dashed line. The stresses σ_1 and σ_2 are indicated in the figure.

Figure 4.11 The actual stress distribution along the wall of unflawed girth weld at $\varepsilon_g = 3\%$. The blue dash line represents the linearized stress distribution for crack depth p of 7 mm and crack height $2a$ of 10 mm.

Secondly, another difference lies in the definition of reference stress σ_{ref} . Now the reference stress σ_{ref} for the pipeline with a circumferential embedded crack is determined by the following equation:

$$\sigma_{ref} = \frac{P_b + 3P_m \alpha'' + \left[(P_b + 3P_m \alpha'')^2 + 9P_m^2 \left\{ (1 - \alpha'')^2 + 4 \left(\frac{p \alpha''}{t} \right) \right\} \right]^{0.5}}{3 \left\{ (1 - \alpha'')^2 + 4 \left(\frac{p \alpha''}{t} \right) \right\}}. \quad (4.1)$$

In which α'' is a coefficient related to the embedded crack size, p and t are the crack depth and the wall thickness of the weldment, respectively.

Finally, once the reference stress is obtained from Eq. (4.1), the required CTOD fracture toughness δ_{mat} for the weld metal with an embedded crack can be obtained using Eq. (3.9).

(a)

(b)

Figure 4.12 Comparisons of the required critical CTOD values based on BS7910 and the current results for the pipeline girth weld with an embedded elliptical crack: (a) $p=5\text{mm}$, $2a=8\text{mm}$, $c/a=3$ and (b) $p=7\text{mm}$, $2a=10\text{mm}$, $c/a=5$. Both cases are subjected to pure bending.

For a tolerable flaw in the pipeline girth weld, the required CTOD fracture toughness obtained from BS7910 are compared with the FE results. The curves of the critical CTOD values against the global strains are shown in Fig. 4.12(a) and (b) for crack depths of $p=5\text{ mm}$ and $p=7\text{ mm}$, respectively. From these two figures it can be found that at any strain levels, the critical CTOD values obtained from BS7910 are larger than those from our current results for the pipeline girth weld subjected to pure bending load. For the first case, the crack depth is 5 mm and the crack length is 24 mm, which is nearly equal to the wall thickness, the critical CTOD value is around 0.9 mm, 50% higher than that from our elastic-plastic fracture analysis results when the global strain is 3%. In the second case in which the crack depth is 7 mm and the crack length is 50 mm, the critical CTOD values from BS7910 are almost twice larger than those from our current results. Since the weld metal has under-matched tensile strength, with large crack the stress accumulated in the weldment results in higher P_m and

P_b , which in turn cause the significant increases of the required critical CTOD value. In summary, the BS7910 code makes highly conservative fracture assessment for pipeline girth welds with an elliptical embedded crack under large plastic bending, especially for the case where the large crack is located in the weldment.

4.5. New Strain-based CTOD Estimation Scheme for Elliptical Embedded Crack

4.5.1. Description of the CTOD estimation equation

As an alternative for the fracture assessment of offshore pipelines, the strain-based design considers the displacement controlled phenomenon which often occurs in the installation operation. The studies carried out by Jayadevan et al. [39] and Østby et al. [41] indicated that the use of strain measurement in the fracture response under large plastic deformation was much preferable compared to the conventional load-based approaches. In view of this, Nourpanah & Taheri (2010) [42] revised the reference strain method proposed by Linkens et al. In the improved strain-based formulation, J approximation was defined as a linear function of the nominal strain of the uncracked body for the reeled pipelines.

Due to the influence of the under-matched weld metal on the base steel in this study, the linear function may not be applicable to describe the relationship between the fracture response and the global strain. Therefore, based on the reference strain method, the relationship between the normalized CTOD value and the global strain is approximately expressed by the following power function,

$$\frac{CTOD}{t} = \alpha_0 \cdot \left(\frac{2a}{t}\right)^{\alpha_1} \cdot \varepsilon_g^{\alpha_2}. \quad (4.2)$$

Where ε_g is the global strain, $2a$ is the crack height, t is the wall thickness at the weldment. The coefficients α_0 , α_1 and α_2 are determined by the crack locations and the crack shapes. The values of these coefficients for pure

bending load and biaxial loading are listed in Table 4.1 and Table 4.2, respectively.

Table 4.1 The values of α_0 , α_1 and α_2 in Eq.(4.2) for pure bending load.

$p(mm)$	c/a	α_0	α_1	α_2
3	1	1.5059	1.5698	0.9218
	3	4.7865	1.9187	0.9027
	5	5.3292	2.009	0.8473
5	1	1.1828	1.4274	0.8773
	3	3.4792	1.6330	0.8746
	5	4.9033	1.8410	0.8342
7	1	0.8851	1.1887	0.8581
	3	3.3361	1.5626	0.8602
	5	3.8217	1.6656	0.7985

Table 4.2 The values of α_0 , α_1 and α_2 in Eq.(4.2) for biaxial loading.

$p(mm)$	c/a	α_0	α_1	α_2
3	1	1.0967	1.5702	0.7649
	3	3.8516	1.6254	0.8297
	5	6.9518	1.7220	0.8787
5	1	0.8596	1.2624	0.7579
	3	2.5151	1.2723	0.7935
	5	3.7569	1.4036	0.7846
7	1	0.5571	0.8594	0.7528
	3	2.1994	1.1841	0.7707
	5	3.0027	1.2824	0.7510

Since the stress-strain curve of the weld metal is linear after yielding, which is believed to possess significant effect on the relationship of CTOD and the large

plastic strain applied on the girth welded pipeline. It leads to the coefficient α_2 less than one for both loading conditions, seen in Table 1 and Table 2. Moreover, the influence of the hardening difference between the base and the weld metal could not be neglected. In Zhang et al. [134] the weldment was not considered, the curves of CTOD vs. applied strain display more or less linear trend when the base steel has exponent strain hardening behavior. Thus, due to the effect of the weldment's stress-strain relationship, the power function is adopted to construct the proposed CTOD estimation equation.

(b)

(c)

(d)

(e)

(f)

Figure 4.13 Comparisons of the CTOD values obtained from the current FE results and the proposed equation Eq. (4.2): (a) $p=3\text{mm}$ under pure bending, (b) $p=5\text{mm}$ under pure bending, (c) $p=7\text{mm}$ under pure bending, (d) $p=3\text{mm}$ under biaxial loading, (e) $p=5\text{mm}$ under biaxial loading and (f) $p=7\text{mm}$ under biaxial loading.

The curves of CTOD values obtained by the proposed equation (Eq. (4.2)) and current FE results against the global strains are depicted in Fig.4.13 for the crack depth of 5 and 7 mm. All the proposed fitting curves are in good agreement with the FE results. Additionally, the coefficient of determination R^2 for the Eq. (4.2) is approximately 0.98, indicating that the proposed CTOD estimation formulation is very accurate for the specific crack considered. With this equation, fracture behavior of offshore pipeline with an elliptical embedded crack subjected to the global strain $\varepsilon_g \leq 3\%$ can be predicted with an error limit of $\pm 2\%$.

4.5.2. Failure assessment diagram (FAD) for the elliptical embedded crack subjected to large plastic strain of 3%

For safety reason, analysis of the CTOD evolution with different crack size is performed for pipeline girth weld with an embedded crack, given the global strain at 3%. The relationship between the normalized CTOD value and the normalized cracked area (crack size) is expressed by the following equation:

$$\frac{CTOD}{t} = m_{\varepsilon} \cdot \left(\frac{S_{crack}}{S_{pipe}} \right)^{n_{\varepsilon}} . \quad (4.3)$$

Where S_{crack} is the crack size, i.e., the area of the elliptical embedded crack equal to $\pi \cdot ac$, S_{pipe} is the mean cross-sectional area of the pipe equal to $\pi \cdot (R_{outer}^2 - R_{inner}^2)$, and t is the wall thickness of the pipeline. m_{ε} , n_{ε} are coefficients depending on the crack depths and the loading conditions, listed in Table 4.3.

Table 4.3 The values of m_{ε} and n_{ε} in Eq. (4.3).

Crack depth $p(mm)$	Pure bending		Biaxial loading	
	m_{ε}	n_{ε}	m_{ε}	n_{ε}
3	0.6383	0.6862	1.5771	0.7894
5	0.8455	0.7138	1.1689	0.7077
7	0.8896	0.7096	1.2752	0.7173

Figure 4.14 Curves of the normalized CTOD versus the normalized cracked area, given the global strain of 3%.

The curves of normalized CTOD values obtained from the proposed Eq. (4.3) and those from our current results against the normalized cracked area are plotted in Fig. 4.14 for two different crack depths under the two loadings. Firstly, for the same cracked area, the CTOD value increases with the crack depth under both loading conditions. Secondly, for the smaller cracked area ratio ($S_{crack} / S_{pipe} \leq 0.04$), the differences of the normalized CTOD between the biaxial loading and pure bending are not so significant at the same crack depth. However, the normalized CTOD curves under the biaxial loading become much steeper as the cracked area ratio increases (shown in the red and purple lines). Finally, the highest gradient of the normalized CTOD is obtained from the case with a deep crack under biaxial loading, indicating that the internal pressure plays a significant role in the CTOD values for large cracked area.

For an acceptable flaw, the measured CTOD should be less than the critical CTOD toughness $\delta_{critical}$, as given in Eq. (3.13). Substitute Eq. (4.3) into Eq. (3.13), for the large plastic strain of 3%, the acceptable cracked area should be satisfied by the following,

$$S_{crack} \leq \left(\frac{\delta_{critical}}{m_{\varepsilon} \cdot t} \right)^{\frac{1}{n_{\varepsilon}}} \cdot S_{pipe} . \quad (4.4)$$

So the maximum cracked area S_{max} for an embedded crack located in the weldment is obtained as below,

$$S_{max} = \left(\frac{\delta_{critical}}{m_{\varepsilon} \cdot t} \right)^{\frac{1}{n_{\varepsilon}}} \cdot S_{pipe} . \quad (4.5)$$

(a)

(b)

(c)

(d)

Figure 4.15 Failure assessment diagram (FAD) for girth welded pipeline with an embedded crack: (a) Crack depth $p=3\text{mm}$ under pure bending, (b) Crack depth $p=7\text{mm}$ under pure bending, (c) Crack depth $p=3\text{mm}$ under biaxial loading and (d) Crack depth $p=7\text{mm}$ under biaxial loading. The global strain is assumed as 3%.

To perform fracture assessment on the offshore pipeline with an embedded crack, strain-based failure assessment diagrams (FADs) are established based on the maximum cracked area obtained by Eq. (4.5). Here the critical CTOD value for the weld metal is 0.8mm, as mentioned in Section 3.5. Fig. 4.15 shows Failure assessment diagrams (FADs) for two different crack depths under the two loading conditions considered. Since the maximum cracked area is obtained by Eq. (4.5), the crack length can be calculated for each given crack height through the definition of S_{crack} . The vertical axis of the FAD is the crack length $2c$, and the horizontal axis is the crack height $2a$. The critical assessment line is plotted by the bold black line for each FAD. An embedded flaw in the weldment can be treated as acceptable if its size belongs to the acceptable region. For instance, for an embedded crack of $2a=7\text{ mm}$ with $2c=30\text{ mm}$ and crack depth $p=7\text{ mm}$, it is acceptable under pure bending load (seen in Fig.

4.15(b)), however it is not acceptable in the case of biaxial loading (seen in Fig. 4.15(d)). The proposed FADs are useful for the preliminary fracture assessment of the girth welded pipelines with an elliptical embedded crack.

4.6. Concluding Remarks

In this chapter, the fracture response of pipeline girth weld with an elliptical embedded crack subjected to large plastic strains is analyzed through 3-D elastic-plastic finite element models. The sub-modeling technique is employed to generate the desirable embedded crack models. The main observations and conclusions are made as follows.

- 1) At any global strain level considered, the CTOD value increases with the increasing crack depth, crack height and aspect ratio. Given certain crack depth and aspect ratio, the CTOD value increases significantly with the increasing crack height. However, under a given crack size, the CTOD value increases slowly even when the crack depth increases considerably.
- 2) Comparison of CTOD values for the strength mismatch cases indicates that the weldment with under-matched strength is more susceptible to induce significant fracture response, compared to the other two strength mismatch cases.
- 3) Curves of CTOD against global strains are steeper as a result of the internal pressure accompanied with the bending load. That means the fracture response of the flawed pipeline is greatly enhanced by the biaxial loading. It implies that the internal pressure reduce the flaw tolerance capability of the pipelines and makes them more vulnerable to be broken.
- 4) Comparisons of CTOD toughness requirement between the BS7910 code and our current investigation results indicate that the BS7910 code makes conservative predictions, especially for pipeline girth welds containing larger embedded cracks.

- 5) New strain-based CTOD estimation equation is proposed using the power function of global strain, for pipeline girth weld with an embedded crack under pure bending as well as biaxial loading. Following that, the failure assessment diagrams (FADs) are established for the preliminary fracture assessment of flawed pipelines with an embedded crack.

CHAPTER 5

Nonlinear Elastic-plastic Stress Investigation for Two Interacting 3-D Cracks in Offshore Pipelines

5.1. Introduction

The study of multiple cracks in engineering structures such as pressure vessels and pipelines has become increasingly important due to many catastrophic failures of piping components. The fact is that under continuous loading conditions, small and rather closely distanced cracks can grow and coalesce into a large one which may subsequently threaten the reliability and safety of the engineering structures [53, 82, 83].

Although a lot of research works were performed on evaluating the interaction of multiple cracks on the fatigue behaviors, most of them were based on the elastic analyses of multiple fatigue cracks and also constrained to crack size studies. There is a lack of information on nonlinear elastic-plastic analysis of fracture behaviors of ductile materials which are commonly used in offshore pipelines. Moreover, in the engineering practice, multiple cracks are generally treated as an equivalent large crack according to the existing codes, such as ASME Code Section XI [135] and BS7910:2005 [9]. These simplified combination rules of multiple cracks results in conservative evaluations for applications such as in the offshore pipelines containing multiple cracks. Therefore, a reasonable and accurate approach is highly required to evaluate the fracture response of offshore pipelines with multiple cracks subjected to large plastic strains.

This chapter presents a systematic investigation on the 3-D fracture behavior of two collinear cracks in the pipelines subjected to large plastic deformation. Nonlinear elastic-plastic FE analyses are carried out for the interaction of a semi-elliptical surface crack and an elliptical embedded crack. The influences of the cracks configuration, the separation distance and internal pressure of the

pipeline on the crack behaviors are investigated for these two collinear cracks. Based on the numerical results, the function of interaction factor is proposed for assessing the fracture behavior of the large surface crack affected by the two interacting cracks. Furthermore, a comparison of fracture response (in terms of CTOD value) has been made between the current results and the computational outcomes derived from BS7910:2005. Conclusions and a summary of the work presented in this chapter are given in the last section.

5.2. Methodology

5.2.1. Geometrical configuration of the pipeline with two collinear cracks

It is generally recognized that some defects may be introduced in the manufacturing or installation process. Multiple closely located flaws could be developed during in service as a result of overloading, fatigue or harsh environmental conditions. Nonlinear 3-D numerical simulations are performed on circumferentially flawed pipelines with one semi-elliptical surface flaw and an elliptical embedded crack. The pipeline has an outer diameter D of 400 mm with wall thickness t of 20 mm.

The pipeline configuration and two collinear crack geometries are shown in Fig. 5.1. Considering that it is the clean pipeline used and also the two cracks are modeled in the section profile of the pipeline, the length of the pipeline segment L adopted in all analyses is twice of the outer diameter. It is sufficiently long for bending load without much change of results, as recommended in several references [39, 41, 42, 44]. Fig. 5.1(b) shows the details of the two cracks geometries considered. For a semi-elliptical surface crack, the crack depth is denoted by a_s and the major axis length of semi-ellipse is represented by $2b_s$. The value of a_s is adopted as 3, 5 and 7 mm which covers the range from a shallow surface crack to a deep surface crack. For each a_s , three values of b_s varying from 3 mm to 35 mm, are considered. Meanwhile, for an elliptical embedded crack, the crack height is denoted by $2a_e$. The major axis of elliptical

embedded crack is $2b_e$ which is called the embedded crack length. As the wide variation of embedded crack geometry is not of primary concern in this study, only some values of the embedded crack size are considered for comparison purpose. In addition, the separation distance between the surface crack tip and the left crack tip of the embedded crack is denoted by s , varying from 3 mm to 9 mm.

Figure 5.1 Schematic drawing of the cracked pipeline model: (a) Overview of the pipeline with a semi-elliptical surface crack and an elliptical embedded crack; (b) Detailed cracks geometries used in the simulation.

The material model used here is the carbon steel same as the base steel of the girth welded pipeline. The true stress-strain response for the pipeline material is shown in Fig. 5.2. The yield stress (σ_Y) of 484 MPa and hardening exponent of $n = 0.05$ are obtained for isotropic hardening behavior based on the uniaxial tension tests. It should be noted that the ratio of the ultimate tensile strength to yield stress (σ_{uts} / σ_Y) is roughly equal to 1.174.

Figure 5.2 The true stress-strain curve of the pipeline material.

ABAQUS version 6.13 is utilized for nonlinear elastic-plastic analyses of the flawed pipelines. Fig. 5.3 shows a typical finite element mesh for the two cracks located in one end of pipeline. Very similar mesh patterns are employed for the other crack geometry configurations. A typical FE model for the flawed pipe with two cracks has approximately 51,000~62,100 elements, depending on the crack size.

Figure 5.3 Typical finite element meshes for the cracked pipeline with a semi-elliptical surface crack and an elliptical embedded crack.

5.2.2. Boundary conditions and loadings

The tension and pure bending load are two primary loadings that most offshore pipelines experience, especially during the reeling installation [3, 42]. The strain-controlled tension is considered since it is widely recognized that the use of stress-controlled tension is not suitable for the ductile materials with large plastic deformation in excess of yield stress [10, 47, 48]. In our investigation, the tension load is displacement-controlled, shown in Fig. 5.4(a). For pure bending, it is implemented through a set of kinematic coupling constraint which is applied on the un-cracked section of pipeline, illustrated in Fig. 5.4(b). The maximum global strain ε_g is assumed as 3% for both tension and pure bending cases.

$$U1=U3=0, \\ U2 = 24mm$$

(a)

$$U1=U2=0, \\ UR3 = 0.12$$

(b)

(c)

(d)

Figure 5.4 Various loading conditions applied to offshore pipelines: (a) displacement-controlled tension, (b) pure bending load, (c) displacement-controlled tension combined with internal pressure and (d) bending load combined with internal pressure.

For the biaxial loading, the internal pressure is considered due to its existence on the pipelines during service [4]. The two different biaxial loadings are displayed in Fig. 5.4(c) and (d), respectively. The load is enforced through two steps: a constant internal pressure is applied first and then followed by tension or bending moment. This loading sequence is common in practice and can introduce high value of fracture response which corresponds to the worst scenario. In order to investigate the effect of biaxial loading on multiple cracks, 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, respectively. Hoop stress is expressed by $\sigma_H = PD/2t$, in which P is the internal pressure, D is the outer diameter of pipeline and t is the wall thickness.

5.3. Fracture Response of Pipelines with Two Collinear Cracks under Tension

Nonlinear elastic-plastic simulations are carried out to investigate the fracture behaviors of the two interacting cracks located in the pipeline subjected to tension load. We use CTOD_S to represent the CTOD value of the surface crack tip. Similarly, for the embedded crack the CTOD values at the left and right crack tips are denoted by CTOD_L and CTOD_R, respectively. The influences of separation distance (s) between the surface crack and embedded crack, crack size and the combined loading on the fracture response are discussed in the following subsections.

5.3.1. Interaction factor γ_s

Firstly the results obtained from the semi-elliptical surface crack are considered to investigate the interaction of these two cracks when the embedded crack size is kept constant as the crack height of $a_e = 3mm$ with $b_e / a_e = 3$. Fig. 5.5 shows the CTOD_S - strain curves for different separation distances, given the certain surface crack. The results obtained from the cases with only one surface crack denoted by “only”, are also presented for comparisons. From Fig. 5.5 (a) and (b), it can be seen that for the case of $a_s = 3mm$, CTOD_S increases linearly with the strain for both $b_s = 3mm$ and $b_s = 15mm$. Marginal difference between the case of “only” and the case of $s = 9mm$ indicates 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, in which no interaction is considered if the separation distance is greater than the summation of the crack heights of the two cracks. 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. While, as the separation distance s is reduced, greater CTOD_S value is obtained.

It can be observed in Fig. 5.5 that when the separation distance s is smaller than the critical interaction distance, the curve of CTOD_S versus strain becomes steeper, compared to the case of “only”, indicating that the CTOD_S value starts to be affected by the cracks interaction as a result of the shortened separation distance. Moreover, Fig. 5.5(e) and (f) present the curves of CTOD_S versus strains for the deep surface crack ($a_s = 7mm$). It is observed that when the separation distance is further reduced, the cracks interaction becomes more pronounced, resulting in much higher CTOD_S values. These observations imply that for varying separation distance, the fracture response at relatively far distance is affected by the cracks interaction in a mild manner, whereas the nonlinear and rapid increases in the fracture response of deep surface crack are observed as the separation distance is reduced. Particularly, compared to the single crack cases, the CTOD_S values can be doubled or even more for the deeper surface crack due to the strong interaction of two closely adjacent cracks.

(a)

(b)

(c)

(d)

(e)

(f)

Figure 5.5 Curves of CTOD_S against strains for different separation distances:

- (a) $a_s = 3\text{mm}$ and $b_s / a_s = 1$, (b) $a_s = 3\text{mm}$ and $b_s / a_s = 5$, (c) $a_s = 5\text{mm}$ and $b_s / a_s = 1$, (d) $a_s = 5\text{mm}$ and $b_s / a_s = 5$, (e) $a_s = 7\text{mm}$ and $b_s / a_s = 1$, (f) $a_s = 7\text{mm}$ and $b_s / a_s = 5$. All cases are under the tension.

To better understand the interaction of two collinear cracks, the interaction factor γ_s is introduced to quantify their interactions, defined as follows,

$$\gamma_s = \frac{CTOD_{in}}{CTOD_{only}}, \quad (5.1)$$

where $CTOD_{in}$ and $CTOD_{only}$ are the CTOD_S values from the case of two collinear cracks and the case of a surface crack only, respectively. The effective crack height is defined as $a^* = (a_s + a_e) / 2$, then s / a^* is regarded as the dimensionless separation distance. When the global strain is assumed as 3%, the variations of γ_s with s / a^* for different surface crack aspect ratios are plotted in Fig. 5.6. It can be observed that in the case of $b_s / a_s = 1$ shown in Fig. 5.6(a), i.e. for the circular surface crack, most of γ_s values are less than 1.2 and the maximum one is roughly 1.4 for all the crack depth considered. It indicates

that the interaction of two cracks has a little influence on the fracture response of the circular surface crack, although large global strain 3% is considered. As the aspect ratio of surface crack increases, the γ_s value increases significantly. In Fig. 5.6(c) with $b_s/a_s = 5$, for $s/a^* \leq 1.0$, a considerable increase in γ_s is observed as s/a^* is shortened. The observation implies that the fracture response of the semi-elliptical surface crack is significantly enhanced by the cracks interaction, in particular, for the relatively large deep crack.

(a)

(b)

Figure 5.6 Variations of γ_s with s/a^* for different surface crack depths: (a) $b_s/a_s = 1$, (b) $b_s/a_s = 3$ and (c) $b_s/a_s = 5$. All cases are under the tension.

Considering the strong influence of the two cracks interaction on the fracture behavior of the semi-elliptical surface crack, the function of γ_s is proposed to quantify the interaction of the two interacting cracks. The relative position and surface crack's geometry are considered in the function of γ_s which is used for the preliminary fracture assessment of the surface crack affected by the multiple cracks interaction. It is difficult to describe the variation of γ_s with the dimensionless separation distance by using a low order function [86]. Therefore, by virtue of Least Squares Method, the three order polynomial is employed to fabricate the relationship between γ_s , s/a^* and b_s/a_s . The expression of γ_s is written by,

$$\begin{aligned} \gamma_s = & D_9(s/a^*)^3 + D_8(b_s/a_s)^3 + D_7(s/a^*)(b_s/a_s)^2 + D_6(s/a^*)^2(b_s/a_s) \\ & + D_5(s/a^*)^2 + D_4(b_s/a_s)^2 + D_3(s/a^*)(b_s/a_s) \\ & + D_2(s/a^*) + D_1(b_s/a_s) + D_0 \end{aligned} \quad (5.2)$$

Where s/a^* is the dimensionless separation distance, b_s/a_s is the aspect ratio of surface crack. $D_0 \sim D_9$ are the coefficients determined by Least Squares

Method, based on the current results. The values for these coefficients are listed in Table 5.1.

The γ_s values obtained by Eq. (5.2) are mostly in good agreement with the current numerical results within an error limit of 5%. It indicates that Eq. (5.2) can provide a reasonably accurate and reliable value of interaction factor γ_s for the pipelines with multiple cracks subjected to large plastic strain of 3%.

Table 5.1 Values for the coefficients in Eq. (5.2).

D_0	D_1	D_2	D_3	D_4	D_5	D_6	D_7	D_8	D_9
2.475	0.001	-2.329	-0.350	0.150	1.446	0.071	0.006	-0.019	-0.268

5.3.2. Effect of internal pressure

Internal pressure is one of the primary loadings experienced by offshore pipelines as well as ground pipelines in operation. For pipeline design, the effect of the internal pressure cannot be neglected, since it changes the effective axial force which is one of the dominating factors resulting in global buckling of pipelines [136]. In this section, the nonlinear elastic-plastic analyses focus on two circumferential cracks in the cracked pipeline subjected to combined loadings such as tensile loading combined with internal pressure. The influence of the hoop stress (induced by internal pressure) on the CTOD_S evolutions is investigated in details.

Given the same separation distance of $s = 5mm$, the comparisons of CTOD_S values obtained from the biaxial loading cases are presented in Fig. 5.7. “T+P” denotes the tension combined with internal pressure. Three different values of σ_H / σ_Y are considered as 20%, 30% and 50% respectively, and $\sigma_H / \sigma_Y = 0$ represents pure tension loading (no internal pressure). In Fig. 5.7 (a), for surface crack of $a_s = 5mm$ with $b_s / a_s = 5$, CTOD_S increases gradually as the

hoop stress increases at the same global strain level. For surface crack of $a_s = 7\text{mm}$ with $b_s / a_s = 5$ as shown in Fig. 5.7 (b), the linear increasing trend of all four CTOD_S-strain curves can be observed for $\varepsilon_g < 0.02$. However, the exponential increase of CTOD_S values is 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 $\sigma_H / \sigma_Y = 50\%$ is two times greater than that from the tension loading only ($\sigma_H / \sigma_Y = 0$). This observation could perhaps be explained by the enhancement of the local deformation in the cracked pipeline. In other words, the local deformation can be enlarged by the combined loading to some extent as the plastic strain increases continuously, and subsequently, it leads to the crack opening largely.

(a)

(b)

Figure 5.7 Comparisons of $CTOD_S$ values obtained from tension combined with different internal pressures: (a) surface crack of $a_s = 5mm$ with $b_s/a_s = 5$, and (b) surface crack of $a_s = 7mm$ with $b_s/a_s = 5$, given the separation distance of $s = 5mm$ and embedded crack of $a_e = 3mm$ with $b_e = 9mm$.

5.4. Fracture Response of Pipelines with Two Collinear Cracks under Bending

5.4.1. Interaction of two collinear cracks

Under the bending load, the flawed pipelines with varying embedded crack size are also modeled for comparisons while the semi-elliptical surface crack remains unchanged. The relationships of CTOD against strains for different separation distances are presented in Fig. 5.8(a) and (b) for $CTOD_L$ and $CTOD_S$, respectively. For the small embedded crack ($a_e = 2mm$), the difference of $CTOD_L$ evolutions between the case of $s = 7mm$ and $s = 9mm$ is marginal. While for a large embedded crack ($a_e = 4mm$), the $CTOD_L$ value increases significantly for both $s = 5mm$ and $s = 3mm$. The similar trend for

CTOD_S is also observed from Fig. 5.8 (b). It should be noticed that if the two cracks are located so far apart that the cracks can be considered isolated (i.e., $s > a_s + a_e$), the overall fracture behavior of cracked pipeline is dominated by the relatively large surface crack alone, compared to the small embedded crack. However, it can be seen that in the case of $a_e = 4\text{mm}$, the CTOD_S value increases by two times at any strain level when the separation distance s is reduced from 5 mm to 3 mm. It demonstrates that for a given crack configuration, the fracture responses of both cracks are significantly affected by the strong interaction when they are located closer to each other.

(a)

Figure 5.8 (a) Curves of CTOD_L versus strain for varying embedded crack size and (b) the corresponding curves of CTOD_S versus strain. The surface crack is fixed at $a_s = 5mm$ with $b_s / a_s = 3$.

Secondly, the curves of CTOD values versus strains for the three crack tips are presented in Fig. 5.9, where the separation distances of $s = 5mm$ and $s = 3mm$ are considered for the semi-elliptical surface crack of $a_s = 5mm$ with $b_s / a_s = 3$ and the elliptical embedded crack of $2a_e = 8mm$ with $b_e / a_e = 5$.

In Fig. 5.9(a), it can be seen that at any given strain level, the highest CTOD value is obtained from the right crack tip of the elliptical embedded crack. The possible reason could be explained by the followings: considering the two-crack configuration, the right crack tip is very close to the inner wall (the distance from the right crack tip to the inner wall of the pipeline is 2 mm), which in turn results in higher stress at the right crack tip than the other two crack tips due to the bending moment. In this case, the fracture response of the elliptical embedded crack plays a dominant role in the whole scenario.

On the other hand, in the case of $s = 3\text{mm}$ shown in Fig. 5.9(b), it is clearly shown that when the elliptical embedded crack moves closer to the surface crack, i.e. the separation distance is reduced from 5 mm to 3 mm, the value of CTOD_S is the highest while the CTOD_R value is the least at any given strain level. This scenario is quite different from the case of $s = 5\text{mm}$. Additionally, it is noticed that both the CTOD_S and CTOD_L values increases significantly as the strain increases. All these observations indicate that the reduced separation distance plays a significant role in determining the fracture response of multiple interacting cracks.

(a)

(b)

Figure 5.9 Curves of the CTOD values against strain for the two interacting cracks: (a) $s = 5 \text{ mm}$ and (b) $s = 3 \text{ mm}$. Here the surface crack of $a_s = 5 \text{ mm}$ with $b_s/a_s = 3$ and the embedded crack of $2a_e = 8 \text{ mm}$ with $b_e/a_e = 5$ are considered.

Furthermore, in order to explore the interaction of the semi-elliptical surface crack and the elliptical embedded crack, the variation of the equivalent plastic strain ($PEEQ$) along the wall thickness is analyzed when the global strain is considered as 3%. Fig. 5.10 shows the curves of $PEEQ$ versus wall thickness for $s = 5 \text{ mm}$ and $s = 3 \text{ mm}$, for the given two-crack configuration.

(a)

(b)

Figure 5.10 Curves of the $PEEQ$ values against wall thickness: (a) $s = 5 \text{ mm}$ and (b) $s = 3 \text{ mm}$. Here the surface crack of $a_s = 5 \text{ mm}$ with $b_s/a_s = 3$ and the embedded crack of $2a_e = 8 \text{ mm}$ with $b_e/a_e = 5$ are considered.

It should be noted that in the axis of abscissa, the value of '0' denotes the outer surface of pipeline along the section profile, and the value of '20' represents the inner surface of pipeline. In Fig. 5.10 it can be observed that three peaks of $PEEQ$ values are obtained at the place of the three crack tips. In the case of $s = 5\text{mm}$, the peak of $PEEQ$ value occurs at the right crack tip of the elliptical embedded crack. However, the semi-elliptical surface crack tip produces the maximum value of $PEEQ$ when the separation distance is reduced to 3 mm.

It is recognized that the higher $PEEQ$ reflects the occurrence of larger plastic deformation. Thus the curves of $PEEQ$ against wall thickness demonstrate that when the two collinear cracks move closer, i.e. the separation distance is shortened to 3 mm, the corresponding fracture behaviors are significantly affected by the strong interaction of the two collinear cracks.

5.4.2. Effect of various combined loadings

In order to further investigate the effect of the different combined loadings and the cracks interaction on the evolution of $CTOD_S$, the bending combined with internal pressure ("B+P") is also considered for different separation distances.

(a)

(b)

Figure 5.11 Curves of the CTOD_S versus strain for four different loading cases: (a) $s = 5\text{mm}$ and (b) $s = 3\text{mm}$. Here the surface crack of $a_s = 5\text{mm}$ with $b_s / a_s = 3$ and the embedded crack of $a_e = 3\text{mm}$ with $b_e = 3\text{mm}$ are considered.

For a given crack configuration, that is, surface crack of $a_s = 5\text{mm}$ with $b_s / a_s = 3$ and embedded crack of $a_e = 3\text{mm}$ with $b_e = 3\text{mm}$, Fig. 5.11 (a) and (b) present the curves of the CTOD_S against strain for the four different loadings, corresponding to $s = 5\text{mm}$ and $s = 3\text{mm}$, respectively.

When the internal pressure ($\sigma_H / \sigma_Y = 50\%$) is applied on the flawed pipeline, the CTOD_S values are significantly augmented for two combined loadings at the same strain level, which is similar to the investigation discussed for Fig. 5.7. In addition, it is observed from both figures in Fig. 5.11 that the slope of the CTOD_S - strain curve obtained from the tension only is slightly steeper than those from the bending only, implying that the more severe deformation can be introduced by the tension load rather than the pure bending.

Furthermore, it is also found that at any given strain level, the CTOD_S value from the tension load combined with the internal pressure ($\sigma_H / \sigma_Y = 50\%$) is much larger than that from the bending load combined with the internal pressure. It indicates that the difference of CTOD_S between the two combined loadings is significantly amplified due to the additional internal pressure imposed. In addition, given the tension load combined with the internal pressure ($\sigma_H / \sigma_Y = 50\%$), comparison of two figures in Fig. 5.11 reveals that when the two collinear cracks are moved closer to each other ($s = 3mm$), even higher CTOD_S values are produced due to the stronger interaction of the two closer cracks, compared to the case of $s = 5mm$ in which interaction is less significant. In summary, all these observations demonstrate that the combined loadings can significantly affect the fracture response of the flawed pipelines, particularly at high global strain levels.

5.4.3. Comparison with the single equivalent crack

Currently, the industrial design standards or codes for pipelines mainly focus on single defect or single crack. Practically the case with multiple cracks has a much larger potential to cause the structures to fail earlier than expected. Subsequently the combination rules of multiple cracks were hence provided in these standard and codes. Take BS7910:2005 as an example to illustrate the combination rule. When multiple cracks are involved, the procedure proposes that the two cracks can be regarded as a single equivalent crack following certain rules. For the in-plane collinear two-crack case - one is surface crack and the other is embedded crack shown in Fig. 5.12, in which s denotes the distance between two cracks, a_1 , a_2 , c_1 and c_2 are the half crack height and half crack length, respectively. The interaction is considered if $s \leq a_1 + a_2$, and thus the effective crack height is taken as $a_{eff} = s + 2a_1 + a_2$, while the effective half crack length is taken as $c_{eff} = \max\{c_1, c_2\} = c_1$. Nevertheless, these simplified combination rules to handle multiple cracks results in conservative evaluations for industry applications on fracture assessment of flawed pipelines. Since it

could be a long process before fracture occurs, the analysis of multiple crack interaction is required.

Figure 5.12 Schematic diagram of the effect crack height and crack length for the two interacting cracks (extracted from BS7910:2005).

Based on the combination rule from BS7910:2005, the two interacting cracks can be treated as a single equivalent crack when the separation distance s is less than or equal to $a_s + a_e$. In this study, the two collinear cracks (one semi-elliptical surface crack and one elliptical embedded crack) are made to be equivalent to a large semi-elliptical surface crack. The effective crack depth of the large semi-elliptical surface crack is taken as $a_{eff} = s + 2a_e + a_s$, and the effective half crack length is taken as $b_{eff} = \max\{b_s, b_e\}$.

Considering three typical crack geometry configurations, comparisons of the CTOD values are made between the two real interacting cracks and the single equivalent crack, as shown in Fig. 5.13. It is observed that at any given strain level, the CTOD value obtained from the single equivalent crack is larger than that from any crack tip of the two interacting cracks. In addition, Fig. 5.13(c) shows that the difference of CTOD values between the single equivalent crack and the two collinear cracks increases significantly when there are two larger

interacting cracks (as a result the single equivalent crack is also larger). These observations indicate that using the combination rule from BS7910:2005 produces conservative predictions of CTOD values for multiple cracks' fracture assessment, especially for the case of relatively large multiple cracks.

(a)

(b)

Figure 5.13 Comparisons of CTOD values between two interacting cracks and the single equivalent crack.

5.5. Concluding Remarks

In this chapter, extensive 3-D finite element simulations are performed on the nonlinear elastic-plastic fracture behaviors of two interacting cracks contained in offshore pipelines. Various loading cases such as tension, pure bending and different biaxial loadings are considered. The influences of the crack configurations, the separation distance, and the internal pressure on the fracture responses of the cracks are investigated in details. Some useful conclusions are drawn as follows.

- 1) A parametric study on the influence of different separation distances of the two interacting collinear cracks is performed.
- 2) The interaction factor is introduced to quantify the interaction of the two interacting cracks, and the proposed function for interaction factor can be useful for the preliminary fracture assessment of the surface crack affected by the interactions.

- 3) Four different loading conditions generally experienced by the offshore pipelines are taken into account 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 ($\sigma_H / \sigma_Y = 50\%$).
- 4) Given a certain separation distance, the parametric studies on the effect of the internal pressure show that under the combined loadings, the linearly increasing trend for all CTOD_S - strain curves is observed when $\varepsilon_g < 2\%$, while the exponential increase of CTOD_S value is observed for the high strain level ($\varepsilon_g \geq 2\%$).
- 5) Comparisons of CTOD values between the two interacting cracks and the single equivalent crack show that using the combination rule from BS7910:2005 produces conservative predictions of CTOD values for the fracture assessment of multiple cracks, especially for the cases with relatively large multiple cracks.

It should be emphasized that the analyses of the two interacting cracks are carried out by considering $D = 400mm$ and $D/t = 20$ (which are commonly used in the engineering practice). This study will then be extended to crack interaction analysis of different pipeline diameter to wall thickness ratio, as well as other crack geometries such as different crack shapes, cracks distances and parallel cracks.

CHAPTER 6**Fatigue Analysis of Offshore Pipelines with Embedded Cracks****6.1. Introduction**

Offshore pipelines are usually under fluctuation loadings due to waves, currents and sea ground motions. While these loading cycles do not normally pose any threat to unflawed pipelines, it is possible that pre-existing surface or embedded defects could propagate by fatigue caused by the loading cycles. Since many catastrophic failures of piping components were caused by fatigue crack growth, fatigue-life prediction of offshore pipelines becomes a major issue to ensure the integrity and reliability of offshore pipelines.

A large amount of experimental and numerical investigations were conducted on fatigue crack growth of the engineering structures. For instance, Singh et al. [104] carried out a set of experimental tests on pipelines with surface cracks, discussed the effect of different crack sizes on crack initiations and crack growths. However, the fatigue issue of the embedded cracks in offshore pipeline steels does not appear to be reported in the open literature. In addition, it is well recognized that the scale factors and the high cost of experimental set-up always play an important role in conducting a full-scale experiment. Normally fatigue crack growth behavior is determined by various parameters, such as geometries of structure and crack, aspect ratio of crack shape and fatigue loading conditions. The adequate confidence to design offshore oil & gas system productions should be built upon a series of preliminary fatigue tests using full-scale numerical simulations.

Therefore, in this chapter an extensive study is performed to investigate the fatigue behaviors of offshore pipelines containing embedded cracks. Extended finite element method (XFEM) is adopted for numerical simulations. The influences of different initial crack length and stress ratio on fatigue crack

growth are investigated. Furthermore, the numerical results have also been validated by the theoretical solutions, and compared with BS7910.

6.2. Interpretation of XFEM and Fatigue Model

6.2.1. XFEM

Extended Finite Element Method is called XFEM for short. In XFEM, the nodal enrichment functions are introduced for the fracture analysis. On one hand, the asymptotic functions are assumed to capture the singularity ahead of the crack tip. On the other hand, a discontinuous function is used for denoting the sudden change in displacement across the crack surfaces.

Generally the approximation of a displacement vector function used in Finite Element Method (FEM) is written as follows,

$$u = \sum_{I=1}^N N_I(x) [U_I] . \quad (6.1)$$

With the introduction of enrichment functions, it becomes,

$$u = \sum_{I=1}^N N_I(x) [U_I + H(x)a_I + \sum_{\alpha=1}^4 F_{\alpha}(x)b_I^{\alpha}] . \quad (6.2)$$

Where $N_I(x)$ is the usual nodal shape function, U_I are the usual nodal displacement vectors equivalent to the continuous part of the finite element method. $H(x)$ is the discontinuous function across the crack surfaces, and a_I are the jump function parameters at the jump-enriched nodes. $F_{\alpha}(x)$ is the elastic asymptotic crack-tip functions, and b_I^{α} are the elastic asymptotic function parameters at the tip-enriched nodes.

To describe and exhibit the crack model in the XFEM domain, the level set fields $\phi = \{\varphi, \psi\}$ are interpolated by the nodal enrichment functions. Here φ denotes crack surface function and ψ is used for the determination of the crack front. The schematic diagram of the crack surface function φ and the orthogonal surface function ψ is shown in Fig. 6.1. The shadow region is

assumed as an initial crack. The coordinate of φ and ψ is labelled out in this figure. The crack front is identified as the intersection of these two perpendicular planes - φ and ψ .

The jump function $H(x)$ is defined as the sign of level set $\varphi(x)$, expressed as below,

$$H(x) = \begin{cases} 1 & \text{if } \varphi(x) \geq 0, \\ -1 & \text{otherwise.} \end{cases} \quad (6.3)$$

For the elastic asymptotic crack-tip functions, they are defined as follows,

$$F_\alpha(x) = \left\{ \sqrt{r} \sin\left(\frac{\theta}{2}\right), \sqrt{r} \cos\left(\frac{\theta}{2}\right), \sqrt{r} \sin\left(\frac{\theta}{2}\right) \sin(\theta), \sqrt{r} \cos\left(\frac{\theta}{2}\right) \sin(\theta) \right\}. \quad (6.4)$$

Where (r, θ) is a polar coordinate system of level set fields, that is,

$$r = \sqrt{\varphi(x)^2 + \psi(x)^2}, \quad \theta = a \tan\left(\frac{\varphi(x)}{\psi(x)}\right).$$

Figure 6.1 Schematic diagram of the crack surface function φ and the orthogonal surface function ψ .

6.2.2. Damage model for crack initiation

In order to simulate the crack initiation, the approach within the framework of XFEM is derived from the traction-separation cohesive model. The traction-separation cohesive model is a very general interaction model, can be used for modeling brittle or ductile fracture behaviors. Thus, the XFEM-based cohesive model is adopted to simulate the crack initiation which can occur along an arbitrary propagation path. Also the crack model built by XFEM enrichment elements is not constrained in the FE mesh grids.

Linear elastic traction-separation behavior is adopted in this model. Fig.6.2 shows the curve of a typical linear traction force and separation distance relationship.

Figure 6.2 Typical linear elastic traction-separation response.

The relationship between the traction stresses and the separation distances of the enriched element is expressed in an elastic constitutive matrix, given by below,

$$t = \begin{Bmatrix} t_n \\ t_s \\ t_t \end{Bmatrix} = \begin{bmatrix} K_{nn} & & \\ & K_{ss} & \\ & & K_{tt} \end{bmatrix} \begin{Bmatrix} \delta_n \\ \delta_s \\ \delta_t \end{Bmatrix} = K \delta \quad (6.5)$$

Where t is the traction stress vector that consists of the normal and the two shear tractions, denoting by t_n , t_s and t_t , respectively. δ_n , δ_s and δ_t are the corresponding separation distances. The normal and tangential stiffness

components - K_{nn} , K_{ss} , K_{tt} can be computed based on the elastic properties for an enriched element.

In the XFEM domain, the crack initiation is represented by the beginning of degradation of the initial crack model. It begins when the criterion for the crack initiation is met. There are several approaches to assess the crack initiation, such as the maximum principal stress, the maximum principal strain and the quadratic traction-interaction, and so on. Here the maximum principal stress criterion is adopted.

In the approach of maximum principal stress criterion, the dimensionless fracture parameter f is defined as follows,

$$f = \left\{ \frac{\langle \sigma_{\max} \rangle}{\sigma_{\max}^o} \right\}. \quad (6.6)$$

Where $\sigma_{\max}^o$ represents the maximum allowable principal stress which is one of material properties. $\sigma_{\max}$ is the maximum stress of the model caused by the applied load. The symbol $\langle \rangle$ is the Macaulay bracket used to signify that a pure compression doesn't cause crack initiation.

The expression of the crack initiation criterion is written as below,

$$1.0 \leq f \leq 1.0 + f_{tol}. \quad (6.7)$$

Once the crack initiation criterion is satisfied, i.e., fracture parameter f reaches the desirable value within a given tolerance (usually $f_{tol} = 0.05$), the crack will start to propagate.

6.2.3. Crack evolution model

The damage evolution law is used to describe the rate of crack growth after the crack initiation criterion is satisfied. In viewpoint of energy consumption, the mixed mode theory is adopted to evaluate the equivalent fracture energy release

rate G_{equivC} . One of the mixed-mode models is BK law which was proposed by Benzeggagh and Kenane (1996) as follows:

$$G_{equivC} = G_{IC} + (G_{IIC} - G_{IC}) \left(\frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta . \quad (6.8)$$

Where G_{IC} and G_{IIC} are the critical energy release rates which are the ones of material properties. If the isotropic behavior is assumed, G_{IC} and G_{IIC} will be the same value, resulting in the response independent of η , it can be any value except 0.

6.2.4. Fatigue model

The Paris law as the classic fatigue assessment model is adopted here for the fatigue crack growth. It relates the fatigue crack growth rate $\frac{da}{dN}$ to the relative fracture energy release rate ΔG . The expression of the Paris law is written as follows,

$$\frac{da}{dN} = c \Delta G^m . \quad (6.9)$$

Where $\Delta G = G_{\max} - G_{\min}$, $G_{\max}$ and $G_{\min}$ correspond to the energy release rates when the structure is loaded to $P_{\max}$ and $P_{\min}$, respectively. c and m are the empirical parameters from the experiments. It should be noted that if the energy release rate is below the threshold energy release rate G_{thresh} , there is no fatigue crack initiation or growth.

Based on linear elastic fracture mechanics, the energy release rate and the stress intensity factor have a special relationship [18]. It is expressed as below,

$$\Delta G = \frac{(\Delta K_I)^2}{E'} . \quad (6.10)$$

In which E' has different values for the plane stress and plane strain conditions.

6.3. Modeling and Simulation Process

6.3.1. Material properties

The flawed pipeline with an elliptical embedded crack is made of carbon steel. Young's modulus with value of 209 GPa and Poisson's ratio with value of 0.3 are used. As analyzed in Section 6.2, the maximum principal stress criterion is adopted for the crack initiation. Referring to Liu et al. [122], the value of maximum principal stress (MPS) is 680 MPa here. For the crack evolution model, the critical energy release rate of carbon steel is assumed as 180 N/mm .

In the ABAQUS code, four parameters from material properties are involved in the fatigue simulations. The fatigue crack initiation criterion is defined as:

$$\frac{N_0}{c_1 \Delta K^{c_2}} \geq 1.0 . \quad (6.11)$$

Where c_1 and c_2 are material constants. N_0 is the number of loading cycles when the crack starts to propagate. Here the value “0.5” and “-0.1” are used for c_1 and c_2 , respectively.

Once the fatigue crack propagation starts, the fatigue crack growth rate can be determined by Eq. (6.9), rewritten as follows,

$$\frac{da}{dN} = c_3 \Delta K^{c_4} . \quad (6.12)$$

Where c_3 and c_4 are material constants. In this study, $\frac{da}{dN}$ is in mm/cycle and ΔK is in $\text{MPa}\sqrt{\text{mm}}$. The values of c_3 and c_4 are taken as $3.807\text{e-}10$ and 3.034 , respectively.

6.3.2. Model geometry

Using XFEM and fatigue model, 3-D finite element simulations are conducted for circumferentially flawed pipeline with an elliptical embedded crack. The analyzed pipeline segment has an outer diameter D of 400 mm with the wall

thickness t of 20 mm. The length of the pipeline segment L adopted in all analyses is two times the outer diameter.

Considering symmetry, half flawed pipeline is modelled. The schematic diagram of the pipeline with an elliptical embedded crack is displayed in Fig. 6.3. The circumferential embedded crack is located at the center of section profile, and also in the middle of the longitudinal length. Fig. 6.4 shows the detailed geometry of the elliptical embedded crack from top view. The minor axis length of elliptical crack is treated as the crack height denoted by $2a$, and the semi-major axis length of elliptical crack is represented by c . The aspect ratio is expressed by c/a , varying from 1 to 5. The initial value of $2a$ is assumed as 4, 7, 10 mm for comparison purpose. Since the embedded crack is always located in the middle of the section profile, the distance from the outer surface to the center of embedded crack remains constant, equal to 10 mm in this study.

Figure 6.3 Schematic diagram of the pipeline with an elliptical embedded crack model.

Figure 6.4 Top view: Detailed geometry of the elliptical embedded crack located in the middle of section profile.

ABAQUS version 6.13 is used for fatigue analyses of offshore pipelines subjected to cyclic tension loadings by virtue of XFEM. The overview mesh of a typical model is shown in Fig. 6.5(a). In order to capture the fracture response ahead of crack tip, a finer mesh is required for the cracked region. Fig. 6.5(b) and (c) present the finer mesh along the crack height and top view of finer mesh ahead of the crack front, respectively. With respect to the finer mesh of half elliptical embedded crack, 10~15 elements are assigned ahead of the two crack tips in the radial direction (shown in Fig. 5(b)). The crack front (in the circumferential direction) is described by 15 layers defined over the half crack length symmetrically (shown in Fig. 5(c)). Thus, the smallest element size is $0.5 \times 0.5 \times 0.5 \text{ mm}^3$ in this study, and the incremental size 0.5 mm is used for the crack propagation.

(a)

(b)

(c)

Figure 6.5 (a) Overview mesh of the pipeline model analyzed, (b) Finer mesh along the crack height, and (c) Top view of finer mesh ahead of the crack front.

6.3.3. Boundary conditions and fatigue loadings

The boundary conditions for the simulation cases are set as follows: 1) One end of the pipeline segment is constrained in all directions, i.e. $U1= U2= U3= 0$, while the other end is applied with cyclic tensile loading. 2) Due to the model's symmetry, the two surfaces normal to Z axis are assigned to Z- symmetry, i.e. $U3=0$. The setup of the boundary conditions and cyclic loadings are presented in Fig. 6.6.

Figure 6.6 The setup of the boundary conditions and cyclic tension loading.

Simulations are carried out under cyclic tension loading which has a triangle waveform. High cycle fatigue analysis is of our concern. Generally only the elastic stress range is taken into account for high cyclic fatigue mentioned by DNVGL-RP-0005[8]. The maximum load applied on the pipelines is constrained to 30~40% of the ultimate tensile strength of the pipeline material, so as to ensure that the crack growth is limited to the elastic state. Herein, the maximum amplitude of tension load is taken as 250 MPa.

To distinguish the different cyclic loading ranges, the stress ratio (R) defined as the ratio of minimum stress experienced to the maximum stress experienced during a cycle, is used for fatigue analysis. Two values 0.1 and 0.5 are assumed for R in this study.

6.4. Fatigue Analyses of Offshore Pipelines with An Elliptical Embedded Crack

Fatigue crack growth studies are conducted on the pipelines with an elliptical embedded crack under cyclic tensile loadings. A series of simulation cases are performed to cover the effects of parameters on the fatigue life, such as the embedded crack size, aspect ratio, and the stress ratio. Moreover, comparisons of fatigue crack growth rate have been made among Theoretical analysis [18], BS7910:2005 [9] and the current XFEM results.

6.4.1. Fatigue crack initiation analysis

One typical fatigue crack growth is illustrated in Fig. 6.7. In this figure, the level set contour plot of PSILSM represents the crack front. It can be found that crack initiation has been observed along the crack height direction. And a large amount of the crack extension is observed.

(a)

(b)

Figure 6.7 Illustration of the typical fatigue crack growth: (a) The initial embedded crack before cyclic loading, and (b) the extended crack after a number of cycles is applied to.

The numbers of cycles required for fatigue crack initiation are listed for some specific cases in Table 6.1. From case 1 to case 3, it can be observed that for a given stress ratio and the aspect ratio c/a , the number of cycles required for

crack initiation increases with the decreasing initial crack height. For the smallest crack height $2a=4$ mm, the number of cycles is significantly larger than the other two cases with medium crack heights. From case 4 to case 6, for a given stress ratio and the same crack height, the number of cycles required for fatigue crack initiation decreases with the increasing aspect ratio. In addition, it has also been observed from case 2 and case 4 that for a given initial crack height and length, the number of cycles required for crack initiation increases with the increasing stress ratio.

Table 6.1 Details of cycles of fatigue crack initiation for some cases.

Case No.	$2a$ (mm)	Aspect ratio c/a	Stress ratio R	Cycles for fatigue crack initiation
1	4	1	0.5	152,240
2	7	1	0.5	31,057
3	10	1	0.5	11,629
4	7	1	0.1	18,706
5	7	3	0.1	1,881
6	7	5	0.1	1,752

6.4.2. Fatigue crack growth analysis

Given a certain initial embedded crack size, Fig 6.8 shows the crack height versus number of cycles for different stress ratios. It can be found that for each initial crack height, the number of cycles for the fatigue crack growth increases with increasing the stress ratio.

(a)

(b)

(c)

Figure 6.8 Crack height vs number of cycles for different stress ratios but the same initial crack height: (a) $2a_0 = 4 \text{ mm}$, (b) $2a_0 = 7 \text{ mm}$, (c) $2a_0 = 10 \text{ mm}$.

In the case of $2a_0 = 4 \text{ mm}$, for a certain increment of the crack extension, the case with $R=0.5$ requires almost twice the loading cycles applied to the case with $R=0.1$. It indicates that the fatigue failure occurs relatively earlier for the cases with smaller stress ratio. The similar trend is observed for $2a_0 = 7 \text{ mm}$ and $2a_0 = 10 \text{ mm}$. This observation is in good agreement with Singh et al.[104].

(a)

(b)

Figure 6.9 Crack height vs number of cycles for the initial crack height of 7 mm with different aspect ratios: (a) $R=0.1$, (b) $R=0.5$.

To explore the effect of aspect ratio of embedded crack on fatigue crack growth, the curves of crack height vs. number of cycles for the initial crack height of 7 mm with different aspect ratios are plotted in Fig. 6.9. It is observed that for a

given stress ratio and initial crack height, the number of cycles required for fatigue crack growth decreases significantly as the aspect ratio increases from 1 to 3. However, as the aspect ratio continuously increases to 5, the number of cycles for fatigue crack growth decreases slowly. It demonstrates that under given cyclic loadings and crack heights, the pipeline with an elliptical embedded crack is much more vulnerable than the pipeline with a circular embedded crack.

Furthermore, the plots of the crack height against number of cycles for different initial crack heights are presented in Fig. 6.10. As shown in Fig. 6.10(a), under the stress ratio $R=0.5$, the number of cycles required for the extended crack height $2a_f=16\text{mm}$ in the case of $2a_0=4\text{mm}$ is approximately five times larger than the case of $2a_0=10\text{mm}$. In addition, the curves shown in Fig. 6.10(b) are corresponding to the aspect ratio $c_0/a_0=3$ with stress ratio $R=0.1$. It is clearly observed that with the same extended crack height, the numbers of cycles for $2a_0=7\text{mm}$ and $2a_0=10\text{mm}$ are considerably less than the case of $2a_0=4\text{mm}$.

(a)

Figure 6.10 Crack height vs number of cycles for different initial crack heights:
 (a) aspect ratio $c/a=1$ and stress ratio $R=0.5$; (b) aspect ratio $c/a=1$ and stress ratio $R=0.1$.

6.4.3. Comparisons with the results obtained by theoretical analysis and BS7910:2005

In this section, the theoretical fatigue assessment provided by the theoretical analysis [18] and fatigue assessment procedure from BS7910:2005 [9] for the pipeline with embedded flaws are described. Based on the current XFEM results, comparisons in fatigue crack growth rate, cycle number for allowable crack growth and the final crack front have been made for validation.

6.4.3.1. Theoretical fatigue assessment

For an elliptical embedded flaw under remote cyclic tension, the stress intensity factor range is written by [18],

$$\Delta K = \lambda_s \Delta \sigma \sqrt{\frac{\pi a_i}{Q}} \quad (6.13)$$

Where λ_s and Q are parameters determined by the embedded elliptical crack's aspect ratio. $\Delta\sigma$ is the applied stress range, a_i is the half crack height which increases gradually during the cyclic loading. It should be noted that ΔK is in $MPa \cdot mm^{1/2}$ and a_i is in mm .

Since the half crack height a_i is the semi-minor axis of the elliptical crack, the maximum stress intensity factor will be obtained at the crack tip along the crack height direction. Based on the worse scenario's principle, the stress intensity factor at the crack tip of the crack height ΔK_a is considered for the fatigue crack growth.

Paris Law is adopted for crack growth analysis, expressed below

$$da / dN = C \times (\Delta K_a)^m . \quad (6.14)$$

Where da / dN is in mm / N .

Substitute Eq. (6.12) into Eq. (6.13),

$$da / dN = C \times (\lambda_s \Delta\sigma \sqrt{\frac{\pi a_i}{Q}})^m . \quad (6.15)$$

Thus, if the initial half crack height a_0 and final half crack height a_f is given, the cycle number for the final crack height can be obtained through integrating Eq.(6.14) ,

$$N_i = \frac{1}{C \times (\lambda_s \Delta\sigma \sqrt{\frac{\pi}{Q}})^m} \cdot \frac{1}{(1 - \frac{m}{2})} (a_f^{1-m/2} - a_0^{1-m/2}) . \quad (6.16)$$

6.4.3.2. *Fatigue assessment procedure based on BS7910:2005 for the elliptical embedded flaws*

Based on BS7910:2005, the applied stress intensity factor range in the crack height direction ΔK_a is expressed as,

$$\Delta K_a = (Y\sigma)\sqrt{\pi a} . \quad (6.17)$$

Substituting the circumferential elliptical embedded flaw's geometry and the stress range into Eq. (6.17) leads

$$\Delta K_a = f_w [M_m \Delta \sigma_m + M_b \Delta \sigma_b] \sqrt{\pi a} . \quad (6.18)$$

Where f_w is the correction term of the stress intensity factor for an elliptical flaw, M_m and M_b are the stress intensity magnification factors, $\Delta \sigma_m$ is the primary membrane stress range, $\Delta \sigma_b$ is the primary bending stress range, and a is half crack height of an embedded crack. It should be noted that f_w , M_m and M_b are determined by the pipeline configuration and crack size. Since the fatigue crack growth is considered, these parameter values are modified and updated by the crack extensions.

For the primary membrane stress range $\Delta \sigma_m$ and primary bending stress range $\Delta \sigma_b$, they are determined by the linearization of stress distribution applied to the un-cracked pipeline under one cycle of tensile loading. The expression of $\Delta \sigma_m$ and $\Delta \sigma_b$ are written as follows,

$$\begin{cases} \Delta \sigma_m = (\Delta \sigma_1 + \Delta \sigma_2) / 2 \\ \Delta \sigma_b = (\Delta \sigma_1 - \Delta \sigma_2) / 2 \end{cases} . \quad (6.19)$$

In which $\Delta \sigma_1$ and $\Delta \sigma_2$ are the stresses from the linearized distribution of the actual stress (stress in the normal direction of crack surface) along the pipe's wall thickness.

Once Eq. (6.17) is solved, i.e., ΔK_a is obtained, the applied stress intensity factor range in the crack length direction ΔK_c can be obtained as follows,

$$\Delta K_c = \Delta K_a \left\{ 1 - \frac{(2a/B)[2.6 - (4a/B)]^{0.5}}{[1 + (4a/c)]} \right\} \left(\frac{a}{c} \right)^{0.5}. \quad (6.20)$$

For the fatigue crack growth model, Paris Law is also employed for the fatigue crack growth in the crack height direction as well as in the crack length direction.

Lastly, the cycle number for the allowable crack height can be computed by

$$\Delta N_j = \frac{a_j - a_{j-1}}{(da/dN)_j}. \quad (6.21)$$

6.4.3.3. Comparison of fatigue crack growth rate

Using the fatigue assessment procedures presented in the above, comparisons of fatigue crack growth rate with ΔK_a obtained from XFEM, theoretical analysis and BS7910 are shown in Fig. 6.11 for four different embedded crack geometries.

(a)

(b)

(c)

Figure 6.11 Fatigue crack growth rate with ΔK_a obtained from XFEM, theoretical analysis and BS7910, respectively. (a) $2a_0 = 4mm$ with $c_0/a_0 = 3$, (b) $2a_0 = 4mm$ with $c_0/a_0 = 5$, (c) $2a_0 = 7mm$ with $c_0/a_0 = 3$, (d) $2a_0 = 7mm$ with $c_0/a_0 = 5$. The stress ratio $R=0.1$ is considered here.

First of all, in Fig. 6.11(a) and (b), i.e. for smaller embedded crack of $2a_0 = 4mm$, these symbols of the fatigue crack growth rate obtained by XFEM are closely dispersed along the two curves of “Theoretical Analysis” and BS7910. It shows the quite similar trend with those results obtained by both “Theoretical” and BS7910. Secondly, as shown in Fig. 6.11(c) and (d) for larger embedded crack of $2a_0 = 7mm$, some symbols of the fatigue crack growth rate obtained by XFEM are around the curves of “Theoretical” and BS7910, while others are a little far away from these two curves, and their corresponding values of fatigue crack growth rate are higher. It could be explained by the following: for the elliptical embedded crack, prediction of the fatigue crack growth using “Theoretical” and BS7910 is by considering only the crack height’s variation. While in our XFEM investigation, both the 3-D crack and pipeline geometries are taken into account. As a result, for the large embedded

crack with a certain crack extension, the cycle number may be somewhat reduced, compared to “Theoretical” and BS7910 results which lead to a larger value of fatigue crack growth rate. In a summary, the comparisons of fatigue crack growth rates indicate that the results obtained by XFEM are more reasonable and reliable.

6.4.3.4. Comparison of fatigue life

(a)

(b)

(c)

(d)

Figure 6.12 Crack heights versus number of cycles obtained from XFEM, theoretical analysis and BS7910: (a) $2a_0 = 4\text{mm}$ with $c_0/a_0 = 3$, (b) $2a_0 = 4\text{mm}$ with $c_0/a_0 = 5$, (c) $2a_0 = 7\text{mm}$ with $c_0/a_0 = 3$, (d) $2a_0 = 7\text{mm}$ with $c_0/a_0 = 5$. The stress ratio $R=0.1$ is considered here.

In this section, comparisons of fatigue life predictions obtained from XFEM, “Theoretical” and BS7910 for the four different crack sizes are presented in Fig. 6.12. It is observed that in Figs. 6.12 (a) and (b), the cycle numbers of fatigue life predicted by XFEM are larger than the results from “Theoretical” and BS7910, indicating that the results obtained from “Theoretical” and BS7910 are conservative for a smaller embedded crack. In addition, the case of $2a_0 = 7\text{mm}$ with $c_0/a_0 = 3$ is plotted in Fig. 6.12(c). It shows comparable results among the three methods. However, in the case of $2a_0 = 7\text{mm}$ with $c_0/a_0 = 5$ as shown in Fig. 6.12(d), the slight short fatigue life predicted by XFEM is observed, compared to those from “Theoretical” and BS7910. The possible reason could be that the small ratio of crack height to wall thickness and the large crack aspect ratio accelerate the fatigue crack growth. Overall, the slight variations on fatigue life predicted by XFEM, “Theoretical” and BS7910

suggest that the XFEM results can be acceptable. It can be concluded that the fatigue crack growth behavior of pipeline with an elliptical embedded crack can easily be predicted by XFEM.

6.4.3.5. Comparison of final crack front

To further demonstrate the effect of aspect ratio on crack front of the elliptical embedded crack, the crack shapes of the case of $2a_0 = 7\text{mm}$ at different intervals of cyclic loading are plotted in Fig. 6.13. The two different aspect ratio $c_0/a_0 = 3$ and $c_0/a_0 = 5$ are considered for comparisons. In the figure, the symbols are the data obtained by XFEM, and the lines are the data calculated by BS7910.

(a)

Figure 6.13 Crack shapes of the elliptical embedded crack $2a_0 = 7\text{mm}$ at different intervals of cyclic loadings, obtained from XFEM, theoretical and BS7910, respectively. (a) $c_0 / a_0 = 3$ and (b) $c_0 / a_0 = 5$. The stress ratio $R=0.1$ is considered here.

As expected, both figures show that the elliptical embedded crack is propagating faster along the crack height direction, rather than along the crack length direction. The possible reason could be that higher stress intensity factor was achieved at the crack tips along the crack height direction. In addition, it is found that the longer crack length is (i.e., the crack length increases from 21 mm to 35 mm), the less crack extension is observed in the crack length direction. All these observations indicate that the fatigue crack behaviors are strongly affected by the aspect ratio.

6.5. Concluding Remarks

In this chapter, the XFEM simulations of steel pipelines with an elliptical embedded crack have been performed for cyclic tension loadings. The influences of the stress ratio, crack height and crack length on the fatigue behaviors of the crack are investigated. Furthermore, the significant fatigue parameters, such as fatigue crack growth rate and fatigue life, are compared with the results from theoretical analysis and BS7910 fatigue assessment. The following main conclusions are arrived from the current study:

- 1) Given a certain stress ratio and the aspect ratio $2c/a$, the number of cycles required for crack initiation increases significantly with decreasing the initial crack height.
- 2) Fatigue crack growth analysis demonstrates that under given cyclic loadings and crack heights, the pipeline with an elliptical embedded crack is much more vulnerable than the pipeline with a circular embedded crack.
- 3) Comparisons of the fatigue crack growth rates obtained by XFEM with theoretical analysis and BS7910 indicate that the present XFEM simulations are highly reasonable and reliable.
- 4) The investigation of final crack front demonstrates that for an elliptical embedded crack, the fatigue behavior strongly depends on the stress ratio and the aspect ratio.

CHAPTER 7

Conclusions and Future Work

7.1. Summary and Conclusions

With the current elastic-plastic fracture mechanics investigation, we have developed comprehensive procedures to assess the fracture behaviors of offshore pipelines with various flaws and subjected to extreme loadings. The user-friendly failure assessment diagrams (FADs) have been established for practical applications.

In this thesis, four highly-related engineering problems are studied:

- I) Fracture response of pipeline girth weld with a semi-elliptical surface crack under large plastic bending.
- II) Fracture response of pipeline girth weld with an elliptical embedded crack under large plastic bending.
- III) Fracture analysis of two interacting cracks in a pipeline under large plastic deformations.
- IV) Fatigue analysis of offshore pipeline with an elliptical embedded crack under cyclic tensile loadings.

Extensive investigations have been performed to study the influences of the significant parameters, such as the crack depth, crack aspect ratio and the internal pressure on the evolution of fracture responses. The major conclusions are summarized as follows.

7.1.1. CTOD estimation formulations

As an alternative method for fracture assessment of offshore pipelines, the strain-based design considers the displacement controlled phenomenon which often occurs during the installation operation. Based on the reference strain approach, the CTOD estimation formulations have been developed to evaluate the fracture behavior of flawed pipelines. Firstly, for the pipeline girth weld

with semi-elliptical surface crack (in Chapter 3), Using RSM, the second order polynomial formulation was built for predicting CTOD values. Secondly, for pipeline girth weld with elliptical embedded crack (in Chapter 4), the power law function was employed to describe the relationship between the normalized CTOD value and the global strain. Thirdly, validation tests have demonstrated the reliability of the proposed models in fracture prediction for the cracked pipelines. With these estimation equations, fracture behaviors of offshore pipeline with semi-elliptical surface crack or elliptical embedded crack subjected to the global strain $\varepsilon_g \leq 3\%$, can be estimated with an error limit of $\pm 2\%$.

7.1.2. Effect of strength mismatch

The strength mismatch between the base material of the pipeline and the weld material plays a significant role in the fracture behavior of the flawed girth welded pipelines (as pointed out in DNV-OS-F101). In order to investigate the influence of the strength mismatch, all the three scenarios including under-matched, even-matched and over-matched are considered (in Chapter 4). The results showed that under the same conditions, the weld with under-matched strength is more susceptible to induce the significant fracture response, compared to the other two strength mismatches.

7.1.3. Effect of internal pressure

It should be noted that the value of CTOD strongly depends on the loading path according to the theory of plasticity. Three possible loading sequences for the biaxial loading (bending + internal pressure) are considered. In loading path 1, the internal pressure is exerted on the pipe first, followed by the rotational angle for bending. In loading path 2, the rotation is applied on the pipeline before the internal pressure. In loading path 3, the bending load and the internal pressure are simultaneously imposed. It was found that that the maximum CTOD is obtained through loading path 1. In contrast, loading path 2 leads to the minimum CTOD. This analysis may help to explain why in practice the frequent loading sequence is loading path 1.

Moreover, at any strain level considered, CTOD values from biaxial loading case are much greater than those obtained through pure bending load. The possible explanation for the above results may be made below: From the viewpoint of energy conservation, the additional internal pressure can make the effective yield stress lower, resulting in a smaller plastic zone size. In other words, the internal pressure applied to the inner surface of the pipeline introduces the hoop stress and consequently brings about a larger plastic deformation which in turn results in higher CTOD values. Thus, the biaxial loading can reduce the flaw tolerance capacity and make the pipeline more vulnerable to be broken.

7.1.4. Fracture assessment compared with BS7910:2005 and FADs

Based on BS7910:2005, the descriptions of the fracture assessment of the pipeline with surface cracks or embedded cracks were presented in this study.

For the semi-elliptical surface crack, it was found that BS7910 gives a lower CTOD value for shallower surface crack ($a = 5mm$). However, for the deep surface crack ($a \geq 6mm$), the values of CTOD toughness requirement from BS7910 are significantly greater than those from our current results, particularly for the case of $a = 10mm$.

For the elliptical embedded crack, the comparisons of minimum material toughness obtained from BS7910 and the current results revealed that the values of CTOD toughness requirement from BS7910 are significantly larger than those from current study.

The reason for the significant difference in CTOD toughness requirement between BS7910 and current 3-D fracture analysis results is that the weld metal has under-matched yield stress. With the existence of a large crack, the stress accumulated in the weldment results in higher P_m and P_b . Also the remaining ligament of the backing carbon steel decreases with the increasing crack size.

The effect of lower CRA stress-strain curve becomes significant for the girth welded pipelines. Thus, the required CTOD toughness drastically increases. In summary, the BS7910 code makes over-conservative fracture assessment for pipeline girth welds with an externally circumferential crack under large plastic bending, especially for the deep cracked pipelines.

According to the critical CTOD values of weld metals, the strain-based failure assessment diagrams (FADs) have been established for the preliminary fracture assessment of the flawed pipeline girth weld when the global strain is assumed as 3%.

7.1.5. Interaction factor

The interaction factor was mostly used for the elastic analysis of the infinite plate with multiple cracks. To better understand the interaction of two collinear cracks in pipelines, the interaction factor γ_s is introduced to quantify the interaction effect on nonlinear elastic plastic fracture behaviors.

It can be found that as the aspect ratio of surface crack increases, the γ_s value increases significantly. For instance, in the case of $b_s / a_s = 5$, a considerable increase in γ_s is observed as s / a^* is reduced. This observation implies that the fracture response of the semi-elliptical surface crack is significantly enhanced by the cracks interaction, in particular for the relatively large deep crack case. Furthermore, the estimation of γ_s as a function of the relative positions of the interacting cracks and crack geometry is proposed for the preliminary fracture assessment of the surface crack affected by the multiple cracks interaction.

7.1.6. Fatigue analysis of flawed pipelines under high-cyclic loading

The fatigue analysis of steel pipelines with an elliptical embedded crack has been conducted for high-cyclic loadings through XFEM simulations. Two different stress ratios are considered. The main findings are summarized as below. Given a certain stress ratio and the aspect ratio of $2c/a$, the number of cycles for crack initiation increases significantly with decreasing the initial

crack height. Comparisons of fatigue crack growth rates obtained by XFEM with the theoretical analysis and BS7910 indicate that the present XFEM simulations are reasonable and reliable. Additionally, the investigation of final crack front demonstrates that for an elliptical embedded crack, the fatigue crack behaviors are strongly affected by the stress ratio and the aspect ratio.

To the author's best knowledge, the current research work is among the first ones to investigate the nonlinear elastic plastic fracture behaviors of girth welded pipelines. Several highly desirable engineering problems in offshore industry have been solved. The proposed CTOD estimation scheme provides an alternative solution for such case where the weld metal has under-matched strength as this case is not covered by any codes and standards so far. The strain-based failure assessment diagrams (FADs) have been established for the preliminary fracture assessment of the flawed pipeline girth weld. That is the major novelty and significance parts of this Ph.D. study.

7.2. Recommendations for Future Work – Failure Assessment Due to Corrosion in Offshore Pipelines

The analyses presented in this thesis focused mainly on the fracture response of pipeline girth weld with 3-D elliptical cracks under extreme plastic deformations. Future studies may extend our investigation to offshore pipelines with external corrosion, using the XFEM technique. In ASM handbook it was mentioned [8] that for the hazardous liquid pipeline accidents (from 1994 to 1999) caused by corrosion, 65% were due to external corrosion. For this reason, it is apparent that proper corrosion analysis and control strategy play a significant role in safety, environmental protection and the pipeline operation. Moreover, the further study may focus on the fracture behavior produced by the combination of corrosion and tension loading since the tensile loading is one of primary loadings that most offshore pipelines experience.

Numerous studies were performed on failure pressure or the remaining strength of corroded pipelines in open literature. For example, Goldstein et al. [137] investigated the corroded pipelines subjected to the internal pressure and thermal loading. It was found that the ASME B31G would be much more conservative in the nonstationary regimes. Corrosion fatigue crack growth rate was studied on API pipeline steels analytically and experimentally [138-140]. Moreover, taking failure probability into account, the probabilistic models were developed to predict the remaining strength of metal loss defects and to assess the integrity of corroded pipelines [141-143].

However, all previous studies thus far only considered clean pipelines with corruptions. Consequently, the future work could investigate the suitability of the aforesaid methods in predicting pipelines girth welds. Additionally, the effects of the weld metal and the external CRA (Corrosion Resistance Alloy) on the overall failure pressure of corroded girth welded pipelines can also be studied. The failure assessment for specific offshore pipelines with external corrosion can be developed for the engineering practical applications.

List of Publications

Articles in Journals

1. **Y.M. Zhang**, D.K. Yi, Z.M. Xiao, Z.H. Huang and S.B. Kumar. Elastic-plastic fracture analyses for pipeline girth welds with 3D semi-elliptical surface cracks subjected to large plastic bending. *Int. J. Press. Vessels Piping* 105-106 (2013) 90-102.
2. **Y.M. Zhang**, Z.M. Xiao and W.G. Zhang. On 3-D crack problems in offshore pipeline with large plastic deformation. *Theor. Appl. Fract. Mech.* 67-68 (2014), 22-28.
3. **Y.M. Zhang**, Z.M. Xiao, W.G. Zhang and Z.H. Huang. Strain-based CTOD estimation formulations for fracture assessment of offshore pipelines subjected to large plastic deformation. *Ocean Engineering*, 91 (2014), 64-72.
4. **Y.M. Zhang**, M.Z. Ariffin, Z.M. Xiao, W.G. Zhang and Z.H. Huang. Nonlinear elastic-plastic stress investigation for two interacting 3-D cracks in offshore pipelines. *Fatigue Fract Engng Mater Struct*, 38 (2015), 540-550.
5. **Y.M. Zhang**, D.K. Yi, Z.M. Xiao and Z.H. Huang. Engineering critical assessment for offshore pipelines with 3-D elliptical embedded cracks. *Engineering Failure Analysis*, 51 (2015), 37-54.
6. **Y.M. Zhang**, T.K. Tan, Z.M. Xiao, W.G. Zhang and M.Z. Ariffin. Failure assessment on offshore girth welded pipelines due to corrosion. *Fatigue Fract Engng Mater Struct* (Accepted).
7. D.K. Yi, Z.M. Xiao, S.K. Tan and **Y.M. Zhang**. Elastic-plastic fracture behavior of a radial matrix crack interacting with a circle inclusion with generalized Irwin corrections. *Acta Mech*, 225 (2014), 91-107.

8. Wengang Zhang, Anthony T.C. Goh, **Yanmei Zhang**, Yumin Chen and Yang Xiao. Assessment of soil liquefaction based on capacity energy concept and multivariate adaptive regression splines. *Engineering Geology*, 188 (2015), 29-37.
9. Wengang Zhang, Anthony T. C. Goh and **Yanmei Zhang**. Probabilistic Assessment of Serviceability Limit State of Diaphragm Walls for Braced Excavation in Clays. *ASCE-ASME J. Risk Uncertainty Eng. Syst., Part A: Civ. Eng.*, (2015) 06015001.
10. Wengang Zhang, Anthony T. C. Goh and **Yanmei Zhang**. Updating Soil Parameters Using Spreadsheet Method for Predicting Wall Deflections in Braced Excavations. *Geotechnical and Geological Engineering*, (2015) 10.1007/s10706-015-9914-4.
11. M.Z. Ariffin, **Y.M. Zhang**, Z.M. Xiao. Elastic-plastic fracture analysis on offshore pipelines with interacting 3-D surface and embedded cracks. *Ocean Engineering* (Under review).

Conference papers

1. **Y.M. Zhang**, M.Z. Ariffin, Z.M. Xiao. Fatigue assessment of girth weld pipelines under low cyclic loadings. *Asia Clean Energy Summit*, Singapore, 28-29 Oct 2014.
2. **Y.M. Zhang**, Z.M. Xiao, M.Z. Ariffin and W.G. Zhang. Numerical investigation on two collinear 3-D cracks in offshore pipelines under bending. *ASME 2015 34th International Conference on Ocean, Offshore and Arctic Engineering (OMAE2015)* in St. John's, Newfoundland, Canada, May 31 - June 5, 2015.

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