Plastic collapse load prediction and safety assessment of cracked circular hollow section uni-planar T/Y-joints and multi-planar TT-joints

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2016

Li, T. (2016). Plastic collapse load prediction and safety assessment of cracked circular hollow section uni-planar T/Y-joints and multi-planar TT-joints. Doctoral thesis, Nanyang Technological University, Singapore.

<https://hdl.handle.net/10356/69058>

<https://doi.org/10.32657/10356/69058>

NANYANG
TECHNOLOGICAL
UNIVERSITY

**Plastic Collapse Load Prediction and Safety
Assessment of Cracked Circular Hollow
Section Uni-planar T/Y-joints and Multi-
planar TT-joints**

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School of Civil & Environmental Engineering

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

2016

ACKNOWLEDGEMENTS

The author would like to express his sincere appreciation to his supervisor, A/P Lie Seng Tjhen, not only for his strong recommendation for the author to register his Ph.D. candidature, but also for his consistent encouragement. His rich academic knowledge and life experience give the author a very deep impression.

Many thanks are also given to Dr. Shao Yongbo for providing the author experimental data of cracked circular hollow section (CHS) T-joints and invaluable suggestion in generating the finite element models of cracked CHS T/Y-joints. Without his help, the progress of this work would have been arduous.

The author also would like to express his sincere thanks to his dear friends, Dr. Zhang Yao, Mr. Wang Longqi, Dr. Vipin Sukumarapillai, Dr. Zhao Mingshan, Mr. Zhao Haisheng, Mr. Jin Yanfei, Mr. Chen Cheng and Mrs. Cai Yanqing. Without them, the author's life in Singapore will be not so colourful. Special thanks are also given to the author's wife, Mrs. Zhang Lixia, for her warm company and persistent encouragement.

Last but not least, the author would like to thank everyone who had helped him in one way or another to complete the present study.

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SUMMARY

Fatigue cracks are frequently detected at the hot spot stress location of the chord-brace intersection zone of aged offshore platforms made of circular hollow section (CHS) joints. Since many of them are still in service, it is necessary to provide engineers an efficient and convenient approach to assess the safety and integrity of these joints. Failure assessment diagram (FAD) is an approach widely used in practice and it is described in BS7910 (2013) and API 579-1 (2007). However, the existing provisions in these codes on strength reduction factors are only applicable to uni-planar T-, Y-, K- and X-joints subjected to axial load, and they cannot be used to address cracked CHS joints subjected to in-plane and out-of-plane bending.

This study focuses on the FAD analysis of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints containing a surface crack. Emphasis is put on the determination of the plastic collapse load/moment of cracked CHS joints. Finite element analysis (FEA) is the main tool used extensively. A new and robust mesh generator is developed to create the mesh models of the cracked CHS joints automatically.

Extensive mesh convergence tests are then carried out to determine the accuracy and efficiency of the mesh design for the cracked CHS joints. It is found that relatively coarse mesh can be adopted for calculating the plastic collapse load/moment of the cracked CHS joints, whereas very refined mesh should be used for determining the valid elastic-plastic J -integral at large yielding region ($L_r \geq 0.5$). In addition, the size of the integral domain of the elastic-plastic J -integral should be sufficiently large to produce a valid far-field value. On the mesh convergence tests, load displacement curves of 6 cracked CHS T-joints subjected to axial load are verified against the experimental data reported by other researchers. A multi-planar CHS TT-joint is fabricated and tested under cyclic loading, and the crack propagation is monitored using the alternating current potential drop (ACPD) technique. The joint containing a surface crack is then tested to failure under axial load, and its load displacement curve is used to validate against the FE models.

From extensive FE analyses, the plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to three basic loads are obtained. It is found that the crack area is the dominant factor affecting the plastic collapse load/moment

of these joints. Six strength reduction factor equations for calculating the plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are proposed based on the lower bound of each of the data group.

The plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are then used to validate the Option 1 FAD analysis in BS7910 (2013). The Option 1 FAD curve is for general purpose usage, and it is deemed towards the lower bound. It had been verified against experimental and numerical results for simple configurations such as cracked plain plates. However, such a validation for cracked CHS joints is still very limited. Certain amount of representative cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are chosen and investigated in this study. Generally, it is found that Option 1 FAD curve is not the lower bound for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to single basic loading. However, if the plastic collapse load/moment of cracked CHS joints is determined by multiplying the proposed strength reduction factors to the characteristic strength of the corresponding uncracked joint provided in the codes of practice, then the Option 1 FAD curve may be taken as the lower bound and it provides a safe prediction. This is because the large amount of conservatism is inherent in both the strength reduction factors and the characteristic strength of the corresponding uncracked joint. Otherwise, a penalty factor aiming to reduce the plastic collapse load/moment of cracked CHS joints should be introduced.

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1. Lie, S.T., **Li*, T.**, Shao Y.B. and Vipin, S.P., (2016), “Plastic collapse load prediction of cracked circular hollow section gap K-joints under in-plane bending,” *Marine Structures*, Vol. 50, pp. 20-34.
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LIST OF SYMBOLS

The notation used in the main text is briefly defined in the alphabetical order in the following list. Symbol whose use is confined to only a specific analysis is omitted here and is defined at the appropriate point in the text.

A_c	Crack area
A_{nc}	Non-dimensional crack area
a	Crack depth
a_i, a_f	Crack depth at crack initiation and final fracture
C	Material constant (Paris' constant)
CTOD	Crack tip opening displacement
c	Half crack length
DOB	Degree of bending
DSF	Deformation scale factor
d_0	Diameter of the chord
d_1, d_2	Diameter of the brace
da/dN	Fatigue crack growth rate (m/cycle)
E	Young's modulus
F_{AR}	Strength reduction factor incorporated in BS7910 (2013)
F_p	Penalty factor working on the plastic collapse load/moment of cracked CHS joints
J, J_I	J -integral
J_e	Elastic J -integral
J_{ep}	Elastic-plastic J -integral
$J_{far-field}$	J -integral obtained from the element contour far away from the crack tip
J_{max}	The maximum J -integral obtained from different element contours
J_{min}	The minimum J -integral obtained from different element contours
J_{tip}	J -integral obtained from the element contour nearest to the crack tip
K	Stress intensity factor

K_e	Equivalent stress intensity factor
K_I	Mode-I stress intensity factor
K_{II}	Mode-II stress intensity factor
K_{III}	Mode-III stress intensity factor
K_{mat}	Material toughness measured by stress intensity factor
K_r	Fracture ratio using stress intensity factor
ΔK	Stress intensity factor range
L_r	Ratio of applied load to plastic collapse load
L_{rmax}	Cutting off line of L_r
l_0	Length of the chord
l_1, l_2	Length of the brace
l_{cr}	Length of the weld toe between two surface crack ends
l_{cr1}	Length of the weld toe from datum point to crack end 1
l_{cr2}	Length of the weld toe from datum point to crack end 2
l_w	Entire length of weld toe along brace-chord intersection on the chord side
M_{ci}	Plastic collapse in-plane bending moment
M_{co}	Plastic collapse out-of-plane bending moment
M_i, M_{ai}	Brace end in-plane bending moment
M_o, M_{ao}	Brace end out-of-plane bending moment
M_m, M_b	Plain plate shape factors
M_{Km}, M_{Kb}	Weld toe magnification factors
m	Material constant (Paris' constant)
N_r	Residual fatigue life
N_f	Fatigue life
P, P_a	Applied axial load
P_c	Plastic collapse load
Q_β	Geometry factor to allow for large β effect
R, r_0	Radius of the chord
r, r_1, r_2	Radius of the brace
r_{k-hole}	Radius of key hole at the crack front
SCF	Stress concentration factor
$SNCF$	Strain concentration factor

$SCF_{ax}, SCF_{ipb}, SCF_{opb}$	Peak stress concentration factors subjected to single axial load, in-plane and out-of-plane bending, respectively
$SCF_{ax}(X)$	Distributions of the SCFs under axial load
$SCF_{ipb}(X)$	Distributions of the SCFs under in-plane bending
$SCF_{opb}(X)$	Distributions of the SCFs under out-of-plane bending
T_W	Welding thickness
T_{AWS}	Welding thickness according to AWS (2000)
t_0	Chord thickness
t_1, t_2	Brace thickness
$Y(g)$	Shape factor of the stress intensity factor of cracked CHS joints
Y_c	Crack shape factor
Y_g	Joint geometry factor
Y_i	Crack size factor
Y_s	Joint and crack coupling factor
Y_θ	Intersecting angle factor
α	$2l_0/d_0$, chord length parameter
α_{cr11}	Polar angle of the start of the crack block
α_{cr1}	Polar angle of crack end 1
α_{cr2}	Polar angle of crack end 2
α_{cr22}	Polar angle of the end of the crack block
β	d_1/d_0 , diameter ratio
φ	Angle between two braces of multi-planar CHS TT-joints
γ	$d_0/2t_0$, chord thickness ratio
δ	Crack tip opening displacement
δ_I	Applied CTOD
δ_{mat}	Material toughness measured by CTOD method
δ_r	Ratio of applied CTOD to fracture toughness δ_{mat}
τ	t_1/t_0 , wall thickness ratio
ε_y	Yield strain
θ	Brace to chord angle
σ_b	Bending stresses
σ_f	Flow stress
σ_{HSS}	Hot spot stress

σ_m	Membrane stresses
σ_n	Nominal stress at the brace
σ_{peak}	Peak stress along the weld toe
σ_{ref}	Reference stress used for plastic collapse consideration
σ_u	Ultimate strength of the material
σ_y	Yielding stress
σ_y^u	Upper yield strength

CHAPTER 1

INTRODUCTION

1.1 RESEARCH BACKGROUND

Steel hollow section members are widely used in onshore and offshore structures such as bridges, railways, studio roofs, offshore platforms, offshore wind turbine platforms, pipeline installation risers, and so on. In these structures, a ‘node’ where several hollow section members are welded together is named a joint. At the joint, the main member which other members are welded to its circumference is called the chord, while all the others are generally called the brace. According to different cross-sectional shapes, steel hollow section joints generally can be classified either as circular hollow section (CHS) joint or rectangular hollow section (RHS) joint. Compared to the RHS joints, CHS joints are used more widely in practice due to their excellent structural and mechanical properties. For instance, the frictional force caused by wind or wave acting on the surface of a CHS member is much lower compared to a RHS member. This is mainly due to the continuous smooth curved surface feature of the CHS member. In addition, for CHS joints, the stress concentration effect at the chord-brace intersection zone is much smaller compared to RHS joints. The high stress concentration effect will significantly reduce the fatigue life of the joints when they are subjected to cyclic loading. Therefore, CHS joints are more prevalent in practice, particularly in offshore structures as they are frequently subjected to large amplitude fatigue loading caused by sea wave, current and wind. Figs 1.1 and 1.2 show a typical steel jacket platform and offshore wind turbine platform fabricated from CHS joints. This study focuses on CHS joints only.

Generally, CHS joints can be simply classified into uni-planar or multi-planar according to the geometrical configuration (API-RP-2A, 2005; Eurocode 3, 2005; IIW, 2009). For a uni-planar CHS joint, all the members lie within a same plane, such as T-joint, Y-joint, K-joint and X-joint; whereas for a multi-planar CHS joint, some braces are located at different planes, such as TT-joint, KK-joint, KT-joint and XX-joint. Fig. 1.3 shows some typical CHS joints which are frequently encountered in practice.

In the open seas and laboratory tests, it is found that CHS joints form critical parts of the structures, and thus failure always occurs at these joints under the action of external loadings. These are due to several reasons. Firstly, at the CHS joint, forces transmitted from the braces directly act on a local area of the chord which is vulnerable to local loadings as it is hollow and thin member; secondly, due to geometric discontinuity at the chord-brace intersection zone, stress concentration effect present at this zone will magnify the forces transmitted from the braces. After a long service, fatigue cracks may initiate from the hot spot location of weld toe of the joint due to cyclic loadings caused by sea wave, wind and current; thirdly, welding defects may already be present due to welding process, and they may act as the crack initiation sites.

For CHS joints used in offshore structures, the primary design criterion is the ultimate bearing capacity at the design stage. The supporting structure consisting of CHS joints must be able to resist the design loading that takes into account of all the possible external loadings, including its self-weight, the topside loads of the supporting structure, the cyclic loadings caused by the sea wave, current and wind, as well as the incidental loads caused by occasional event such as storm, earthquake and ship collision. Nowadays, with the assistance of some industry standards such as IIW (2009), CIDECT (Wardenier et al., 2008), API-RP-2A (2005), Eurocode 3 (2005) and design software tools, to determine the bearing capacity of CHS joints is no longer an issue. When an offshore structure is put into service, fatigue life of the offshore structure should be determined properly. According to a statistical study conducted by Health & Safety Executive (HSE, 1993), damage caused by fatigue cracks represented some 25% of all repair works to the steel platforms in the North Sea area (Stacey & Sharp, 1997).

For defect-free CHS joints, fatigue analysis can be carried out by using the S-N curves approach, in conjunction with the hot spot stress. Hot spot stress includes the local stress concentration effect at the chord-brace intersection zone of a CHS joint, and it can be calculated by multiplying the nominal stress with the corresponding stress concentration factor (SCF). Nominal stress can simply be determined by using simple elastic beam theory. Take a CHS T-joint as an example, when the brace member is subjected to axial loading, the nominal stress is equal to the axial loading divided by the brace cross-sectional area. The SCF can be obtained from some design standards such as CIDECT (Zhao et al., 2000) and NORSOK standard (1998).

The S-N curves approach is only applicable to estimate the fatigue life of a defect-free metallic structure, and it does not consider the presence of any flaw such as welding defect and fatigue crack. Once a surface crack is present at the weld toe of a CHS joint, the S-N curves approach may no longer be applicable. In this situation, fracture mechanics is an alternative method which can be used to estimate the fatigue life of a cracked CHS joint. This method essentially combines the stress intensity factor (SIF) and Paris' crack propagation law (Paris & Erdogan, 1963) to predict the fatigue life of a cracked CHS joint. It has been proved to be a reliable tool in estimating the fatigue life of any cracked metallic structure (Paris & Erdogan, 1963; Ritchie, 1998). However, when fracture mechanics method is applied to a cracked CHS joint containing a surface crack, it raises another challenging issue on determining the SIFs along the crack front as there is no analytical solution or design equation available up to now. Researchers have to use numerical method to determine the SIFs of a surface crack present in a CHS joint. Finite element (FE) method has been proved to be an efficient and reliable tool to calculate the SIFs of many cracked metal structures, such as cracked plain plate (Newman & Raju, 1981), welded T-butt joint (Bowness & Lee, 1996), and so on. Now, the main challenge is narrowed down to create the mesh models of different cracked CHS joints.

Due to the complexity of geometry coupled with the surface crack, it is very difficult to manually create the mesh model of a cracked CHS joint using any existing commercial FE software. Therefore, researchers have to develop their own special purpose automatic FE mesh generator to create the mesh models of different cracked CHS joints. For the past decades, several research groups have developed FE mesh generators specifically used to estimate the SIFs of cracked CHS joints such as Cao et al. (Cao et al., 1998), Bowness and Lee (Bowness & Lee, 1995), and Lie et al. (Lie, Lee & Wong, 2003; Lie, et al., 2004). By carefully studying the works done by these three research groups, it is found that there are still many limitations in their FE mesh generators. Therefore, it is decided to develop a completely new and robust program for generating the mesh models of any cracked CHS joint, not only for simple T- and Y-joint, but also for complex multi-planar cracked CHS joints. This is one of the main objectives in this study.

When a surface crack is present at the weld toe of a CHS joint, it is important to check its ultimate bearing capacity as well as the possibility of fracture failure (Laham & Burdekin, 1997). Burdekin (Burdekin, 2002a) adopted a strength reduction factor to account for the

presence of a crack in calculating the plastic collapse load of CHS T-joint and Y-joint under axial tensile loading, and later this reduction factor was slightly modified and incorporated in the BS7910 (2013) standard. The ultimate bearing capacity, which is referred to as the plastic collapse load, of any cracked CHS T-joint and Y-joint can be simply calculated by multiplying this reduction factor with the characteristic strength of the corresponding uncracked joints. The strength reduction factor given in BS7910 (2013) is expressed as

$$F_{AR} = \left[1 - \frac{\text{cracked area}}{\text{intersection length} \times t_0} \right] \left(\frac{1}{Q_\beta} \right)^{m_q} \quad (1.1)$$

where Q_β is a geometry factor, t_0 is the chord thickness, and m_q is a constant dependent on crack type. For CHS joints containing part-through thickness crack, $m_q=0$ and for CHS joints containing through thickness crack, $m_q=1$. It is vitally important to recognize that the reduction factor given in Eq. (1.1) considers only plastic collapse behavior of the cracked CHS T-joints and Y-joints, and it does not take into account of fracture. However, many reports (Gibstein & Moe, 1986; Machida, Hagiwara & Kajimoto, 1987; Qian, 2005; Zerbst, Heerens & Schindler, 2002a) clearly show that fracture failure may occur for cracked CHS joints. In fact, ductile crack extension and tearing behavior before final failure should also be taken into account as they are found to occur in most of the tests. Eq. (1.1) is only applicable to cracked CHS T-, Y-, K- and X-joints subjected to axial tensile loading. For cracked CHS joints subjected to in-plane and out-plane bending, there is no such reduction factor reported till date. Besides, even for cracked CHS T-, Y-, K- and X-joints subjected to axial tensile loading, an extensive validation is still required to calibrate Eq. (1.1) as it is proposed towards the lower bound of the plastic collapse load. Eq. (1.1) may severely under-estimate the plastic collapse load of cracked CHS T-, Y-, K- and X-joints subjected to axial tensile loading.

In practice, the plastic collapse load of a cracked CHS joint cannot reach the value calculated by using the strength reduction factor recommended in BS7910 (2013) due to the fact that fracture tearing failure always occurs earlier. Therefore, the interaction effect of the plastic collapse and the fracture behaviors must be considered together when estimating the safety of a cracked CHS joint. In 1975, Dowling and Townley (Dowling & Townley, 1975) proposed a method to assess the safety of a cracked steel structure based

on two extreme failure behaviors, namely linear elastic fracture and fully plastic collapse. Its philosophy is that the failure mode of any cracked steel structure always behaves somewhere between the linear elastic fracture and fully plastic collapse due to the formation of a plastic zone ahead of the crack front, and an interpolation fitting curve derived from experimental tests was used to represent the failure behavior of a cracked structure. After Dowling and Townley (Dowling & Townley, 1975) had proposed the two-criteria approach, many researchers have carried out extensive research to apply this concept and solve different problems, such as cracked plain plates, cracked pressure vessels and so on. Later, several guidelines used to evaluate the safety and integrity of cracked steel structures were published (R6, 2001; BS7910, 2013; API 579-1, 2007). These standards had been validated against experimental tests and numerical simulation of simple cracked component but without detailed guidelines on cracked CHS joints. The approach used in these standards and the detailed procedures as well as some terms and classifications are essentially the same. The present study will extend this approach for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints. In BS7910 (2013), failure assessment diagram (FAD) is used to assess the safety and integrity of a cracked CHS joint. Fig. 1.4 shows the Option 1 FAD curve which is for the general purpose usage. When the safety and integrity assessment is carried out on a cracked CHS joint using the FAD approach, the CHS joint is deemed to be safe if the assessment point falls inside the FAD curve; otherwise, it is deemed to be unsafe.

Laham and Burdekin (Laham & Burdekin, 1997; Burdekin, 2002a) first applied FAD approach to assess the safety and integrity of cracked CHS T-joint, K-joint and multi-planar TT-joint containing a through thickness crack rather than a surface crack. Later, it was confirmed that FAD approach can be used in practice but one needs to be very careful in choosing a suitable assessment curve. Zerbst (Zerbst, Heerens & Schindler, 2002a) led a study on the fracture behavior of welded CHS T-joints containing a surface crack at the saddle. Four cracked CHS T-joints were tested to failure under brace end axial loading. A series of research work was then carried out by a number of researchers, and the fracture behavior of the cracked welded CHS T-joints were examined by using different methods, including R6 analysis (Marshall & Ainsworth, 2002), UK BS7910 methodology (Burdekin, 2002b), engineering treatment model (Zerbst et al., 2002b), screening method (Schindler, Primas & Veidt, 2002) and design curve conforming to WES 2805-1997 (Zerbst & Miyata, 2002c). Zerbst (Zerbst, Heerens & Schindler, 2002a) summarized all the

finding and concluded that the results of different methods ranged from ‘acceptably conservative’ to ‘slightly non-conservative’. However, it should be noted that when carrying out FAD analyses, some approximations and assumptions are used. For example, in R6 analysis, the SIF of the cracked CHS T-joint is determined by approximating a cracked cylinder with a semi-elliptical surface crack in conjunction with the stress distribution normal to the crack plane. These approximations and assumptions might have adverse effect on the accuracy of the prediction results. Recently, Qian (Qian, 2013) carried out an FAD analysis on CHS X- and K-joints, and it is found that the Option 1 FAD curve in BS7910 (2013) cannot be used directly in analyzing cracked CHS X- and K-joints subjected to axial tensile loading, and a new formula of FAD curve is proposed subsequently.

From the above discussions, it can be seen that when a surface crack is present in a CHS joint, in addition to estimating the fatigue life of the cracked joint, there is also a need to check its bearing capacity to prevent possible fracture failure. This is very important as more and more offshore structures are experiencing a long service life. However, research works related to fracture behavior of cracked CHS joints is very rare, and besides they focus mainly on uni-planar CHS joints subjected to axial tensile loading. Therefore, more research works on fracture behavior of cracked CHS joints subjected to in-plane and out-of-plane bending need to be carried out, and to extend it to cracked multi-planar CHS joints which are more frequently encountered in offshore and onshore structures.

1.2 RESEARCH SCOPE AND OBJECTIVES

The present study focuses on the safety assessment of cracked CHS joints containing a surface crack located at the hot spot stress region. The research targets include the uni-planar CHS T/Y-joint and the multi-planar TT-joint subjected to three basic loadings, respectively. FAD method is used to assess the safety of the analyzed uni-planar and multi-planar CHS joints as it considers the interaction effect of fully plastic collapse and fracture behaviors.

The scope and objectives of this research work are as follows:

- To develop a new flexible and robust mesh generator which can be used to generate high quality mesh models of cracked uni-planar T/Y-joints and multi-planar TT-joints

containing a surface crack at the hot spot stress location. The crack can be located at anywhere along the weld toe, not only fixed at the crown or saddle point. In addition, the new mesh generator can consider different singularities at the crack tip. For example, when calculating the SIF, the mid-side node of the elements enclosing the crack tip should be moved to the $1/4$ point position to produce the $1/r^{1/2}$ strain singularity. However, for elastic-plastic analysis, the mid-side node should be kept at the middle point position in order to produce the $1/r$ strain singularity.

- To estimate the fully plastic collapse load and the crack driving force elastic-plastic J -integral of cracked uni-planar T/Y-joints and multi-planar TT-joint containing a surface crack, and re-examine the strength reduction factor, F_{AR} , recommended in BS7910 (2013).
- To propose new strength reduction factors for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to three basic loads.
- To validate the standard Option 1 FAD curve recommended in BS7910 (2013) by making a direct comparison with the Option 3 curve which is constructed using solely from numerical results.
- To carry out fatigue test on a multi-planar CHS TT-joint, and measure the fatigue crack propagation shape using the alternating current potential drop (ACPD) technique. When the crack depth has reached 80% of the chord thickness, the test is stopped and static axial loading is applied incrementally to the brace until final failure. The corresponding load-displacement curve is plotted to estimate the plastic collapse load of the cracked multi-planar CHS TT-joint.

1.3 CONTRIBUTION AND ORIGINALITY

The original contributions of this research works are as follows:

- A new FE mesh generator is developed and used to generate the mesh models of cracked CHS joints containing a surface crack at the weld toe of the chord. It can consider different singularities at the crack tip, depending on different types of FE analysis. Besides, a finite radius key-hole is introduced at the crack tip for calculating the plastic collapse load and elastic-plastic crack driving force J -integral so as to

ensure convergence of the solutions at all time. The new mesh generator can be easily extended for any other different CHS joint.

- New strength reduction factors for calculating the plastic collapse loads of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to axial load, in-plane and out-of-plane bending are proposed based on the FE results of an extensive parametric study. The strength reduction factors for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to in-plane and out-of-plane bending have never been reported before.
- In carrying out FAD analysis on cracked metallic structures, the Option 1 FAD curve recommended in BS7910 (2013) is applicable for most of simple geometric configurations such as cracked plain plate, welded T-butt joint and pressure vessel. The validity of the Option 1 curve on cracked uni-planar CHS T/Y-joint and multi-planar CHS TT-joint is examined by carrying out numerical FAD analysis.
- The bearing capacity of a cracked multi-planar CHS TT-joint containing a surface crack is studied by carrying out experimental test and FE analysis.
- The additional brace effect is investigated by making a direct comparison between cracked uni-planar CHS T/Y-joint and multi-planar CHS TT-joint.

1.4 SYNOPSIS OF THE THESIS

The synopsis of this thesis is arranged as follows:

- Chapter 2 covers the literature review. The significant works on cracked CHS joints reported by other researchers are reviewed and various parameters used in the subsequent chapters are introduced.
- Chapter 3 illustrates the FE mesh generation of the cracked CHS joints containing a semi-elliptical surface crack at the weld toe of the chord. Detailed calibration works and mesh convergence tests are carried out to verify the accuracy of the generated mesh models.
- Chapter 4 reports the test results of a multi-planar CHS TT-joint. The experimental test is divided into 3 phases. In phase 1, the specimen is subjected to static loading to

measure the hot spot stress distribution along the weld toe. In phase 2, the specimen is subjected to cyclic loading. After the crack has initiated and propagated through 90% of the chord thickness, the fatigue test is stopped. In phase 3, the cracked specimen is subjected under axial tensile loading up to failure. This chapter aims to verify the mesh models of the cracked multi-planar CHS TT-joints by comparing the load-displacement curves obtained from experimental test and FE analysis.

- Chapter 5 numerically investigates the plastic collapse load/moments of cracked uni-planar CHS T/Y-joints subjected to axial loading, in-plane and out-of-plane bending, respectively. Three strength reduction factor equations used to calculate the plastic collapse load/moments of cracked CHS T/Y-joints are proposed based on the lower bound of the FE results.
- Chapter 6 investigates numerically the plastic collapse load/moments of cracked multi-planar CHS TT-joints subjected to axial loading, in-plane and out-of-plane bending, respectively. Similarly, three strength reduction factor equations used to calculate the plastic collapse load/moments of cracked multi-planar CHS TT-joints are proposed based on the lower bound of the FE results.
- Chapter 7 aims to study the safety and conservatism of Option 1 FAD curve given in BS7910 (2013). The Option 3 FAD curves of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are constructed using fully numerical results, and they are compared with the Option 1 FAD curve.
- Chapter 8 summarizes the conclusions of the thesis. Important research findings are highlighted and subsequently, recommendations for future works are given.

Fig. 1.1 Fixed steel jacket offshore platform used for supporting topside facilities
(Wardenier et al., 2008)

Fig. 1.2 Offshore wind turbine platform (Lee et al., 2016)

(a) Uni-planar T-joint

(b) Uni-planar Y-joint

(c) Uni-planar K-joint

(d) Uni-planar X-joint

(e) Multi-planar TT-joint

(f) Multi-planar XX-joint

(g) Multi-planar KT-joint

(h) Multi-planar KK-joint

Fig. 1.3 Typical uni-planar and multi-planar CHS joints

Fig. 1.4 Failure assessment diagram for cracked structure

CHAPTER 2

LITERATURE REVIEW

2.1 INTRODUCTION

CHS joints are critical parts of offshore structure as failure frequently starts at the joint region. For the past decades, extensive research works have been carried out on different types of CHS joints. These research works generally focus on the study of the ultimate bearing capacity (Lu et al., 1994; Lee & Wilmshurst, 1997; Fung et al., 1999; Choo et al., 2004; Qian et al., 2007) and the fatigue life (Gulati, Wang & Kan, 1982; Efthymiou & Durkin, 1985; Hellier et al., 1990; Soh & Soh, 1996; Lee & Morgan, 1998; Mashiri, Zhao & Grundy, 2004; Shao, 2007a; 2007b; Ahmadi, Lotfollahi-Yaghin & Aminfar, 2012) of the defect-free CHS joints. Based on an extensive experimental database of static loading tests, many guidelines such as IIW (2009), API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) have been developed to provide explicit equations to determine the design bearing capacity of different types of CHS joints encountered in practice. For CHS joints used in offshore structures, the fatigue loading caused by sea wave, current and wind is generally much lower than the design bearing capacity. Therefore, the fatigue life rather than the ultimate bearing capacity is the main concern for the offshore structures subjected to fatigue loading in service. S-N curve approach is widely used to predict the fatigue life of the defect-free CHS joints. There are also several guidelines available for predicting the fatigue life of the defect-free CHS joints, such as CIDECT (Zhao et al., 2000) and NORSOK standard (1998). The application of these two guidelines is limited to several uni-planar and multi-planar CHS joints currently, including uni-planar T-, Y-, X-, K-, KT-joints and multi-planar XX-, KK-joints.

In actual conditions, fatigue cracks are frequently detected at the hot spot stress (HSS) region of the offshore structures subjected to cyclic loading. When a fatigue crack is present at a CHS joint, both plastic collapse and fracture behavior must be considered together to assess the safety and integrity of the joint. Fatigue cracks may cause severe damage to the fatigue life and the bearing capacity of the CHS joints. To author's knowledge, research works on the fracture behavior of cracked CHS joints are very limited. This may be due to two factors. Firstly, it is costly and time-consuming to carry out full-

scale fatigue test on a CHS joint in the laboratory. There are very limited experimental tests data available in the literature, and all of them focus exclusively on the uni-planar CHS T-, K- and X-joints (Ritchie & Huijskens, 1988; Zerbst et al., 2002a; Huang, 2003; Shao, 2004; Qian, 2013). This is because it is far more difficult and expensive to fabricate large scale multi-planar CHS joints in the laboratory. Secondly, due to the complexity of the geometry, it is still laborious to manually create the mesh model of a cracked CHS joint using any commercial software. Hence, some researchers have spent a great effort to develop special purpose automatic FE mesh generators to create the mesh models of cracked CHS joints (Rhee, 1989; Nwosu & Olowokere, 1995; Bowness & Lee, 1995; Cao et al., 1998; Lie et al., 2003). There are also very limited numerical data reported in the literature.

This study focuses on the safety and integrity assessment of cracked CHS joints by using failure assessment diagram (FAD) approach. The research work starts with the uni-planar CHS T/Y-joints, and then extended to multi-planar CHS TT-joints. In this chapter, research works related to the fatigue life of the uncracked CHS joints are reviewed as well. This is because some parameters and concepts such as the nominal stress, the hot spot stress and the definition of the fatigue life of uncracked CHS joints are also used in the safety and integrity assessment of cracked CHS joints. Therefore, past research works reviewed in this chapter are divided into three main parts, namely fatigue life of uncracked CHS joints, residual fatigue life of cracked CHS joints, and safety and integrity assessment of cracked CHS joints. Important research works directly and indirectly related to this study are also reviewed in each part of the sub-chapters.

2.2 FATIGUE LIFE OF UNCRACKED CHS JOINTS

The S-N curve approach is widely accepted in estimating the fatigue life of defect-free CHS joints. Essentially, they are a series of curves representing the relationship between the hot spot stress range (ΔS) and the number of cycles (N) defined as the fatigue life of the CHS joints. The fatigue life of different metallic components is usually determined from experimental tests for different geometries and materials. For CHS joints, specific S-N curves are recommended in many design codes such as DEn (1993), IIW (2009) and CIDECT (Zhao et al., 2000). Fig. 2.1 shows typical S-N curves of CHS joints and rectangular hollow section (RHS) joints found in CIDECT (Zhao et al., 2000). Typically, a wall thickness of 16 mm is used as the basis of S-N curve in hollow section joints. For

joints with wall thicknesses other than 16 mm, a thickness correction factor is introduced. The equations of S-N curves considering the thickness effect found in CIDECT (Zhao et al., 2000) are presented in Table 2.1.

2.2.1 Nominal Stress of CHS Joints

Fig. 2.2 shows the geometrical parameters of a typical CHS T/Y-joint. The maximum stress at the brace cross-section far away from the chord-brace intersection region is defined as the nominal stress, σ_n . Nominal stress of a brace subjected to three basic loads are expressed as

for axial load:

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} \quad (2.1)$$

for in-plane bending:

$$\sigma_n = \frac{32d_1M_i}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \quad (2.2)$$

for out-of-plane bending:

$$\sigma_n = \frac{32d_1M_o}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \quad (2.3)$$

where P is the axial load, M_i and M_o are the in-plane bending and out-of-plane bending respectively.

The nominal stress of a CHS joint subjected to single basic load can be defined clearly. However, for a CHS joint subjected to a combination of three basic loads, the nominal stress is not easy to be determined. This is mainly because the nominal stresses under in-plane and out-of-plane bending are non-uniform along the circumference of the brace cross-section. For some particular load cases, such as combination of axial load and in-plane bending, and axial load and out-of-plane bending, nominal stress can be determined by using elastic superposition method. This is because the nominal stress of a CHS joint subjected to axial load is uniform, and thus, at a certain point it will overlap with that under in-plane or out-of-plane bending. Therefore, nominal stresses of a CHS joint subjected to these particular load cases can be expressed as

For axial load and in-plane bending:

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} + \frac{32d_1M_i}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \quad (2.4)$$

For axial load and out-of-plane bending:

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} + \frac{32d_1M_o}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \quad (2.5)$$

Shao (Shao, 2004) proposed an equation to define the nominal stress distribution of a CHS joint subjected to combined loading, and it is expressed as

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} + \frac{32d_1M_i}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \sin \theta_1 + \frac{32d_1M_o}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \cos \theta_1 \quad (2.6)$$

where θ_1 is defined as zero at the saddle. The maximum value of σ is defined as the nominal stress of the CHS joint subjected to combined loading and it is expressed as

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} + \sqrt{\left(\frac{32d_1M_i}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \right)^2 + \left(\frac{32d_1M_o}{\left(\pi \left[d_1^4 - (d_1 - 2t_1)^4 \right] \right)} \right)^2} \quad (2.7)$$

The direction of the nominal stress is determined by

$$\theta_1 = \frac{\pi}{2} - \arctan \frac{M_o}{M_i} \quad (2.8)$$

However, it should be noted that θ_1 calculated from Eq. (2.8) defines the direction of the nominal stress, and generally it does not overlap with the position of the hot spot stress for CHS joints subjected to combined loading.

2.2.2 Hot Spot Stress of CHS Joints

Stress distribution at the chord-brace intersection region of a CHS joint is very complex due to non-uniform stress concentration caused by the geometrical discontinuity and the existence of possible welding defect. Fig. 2.3 shows the typical stress distribution at the intersection region of a CHS X-joint subjected to brace end axial loading. The peak stress σ_{peak} along the weld toe is defined as the hot spot stress. There are two different definitions of the hot spot stress. DEn (1984) has recommended the maximum principal stress at the weld toe as hot spot stress. This stress is ‘the peak value of geometric stress found at the weld toe, and should incorporate the effects of overall tube geometry, i.e. the relative size of the brace and the chord, but omit the concentrating influence of the weld itself which results in a local stress distribution’. While van Wingerde et al. (van Wingerde, Packer & Wardenier, 1996) proposed another definition of hot spot stress, and it is expressed as ‘the extrapolation value at the weld toe position of the structural stress perpendicular to the weld toe’. This definition is more realistic because a surface crack is most likely to initiate at the weld toe, and then propagate along the weld profile. In addition, the hot spot stress defined by van Wingerde et al. (van Wingerde, Packer & Wardenier, 1996) is more convenient to be utilized in practice. Therefore, the definition of the latter hot spot stress proposed is more prevalent, and it is accepted by many researchers and guidelines, such as IIW (2009) and CIDECT (Zhao et al., 2000). Fig. 2.4 illustrates the definition of the extrapolation region of a CHS T-joint, and Table 2.2 lists the details of the extrapolation region recommended by CIDECT (Zhao et al., 2000). In Table 2.2, r_0 and r_1 are the radii of the chord and the brace respectively. In the experimental tests, two or more single strain-gauges can be deployed to measure the local strains. The strain at the weld toe can be derived by the linear or the quadratic extrapolation. For instance, for the linear extrapolation situation, the strain concentration factor can be converted to the stress concentration factor through the specific equation (Shao, 2004; Gao, 2005) given below as

$$\text{SCF} = \frac{(1 + \nu \varepsilon_1 / \varepsilon_2)}{1 - \nu^2} \text{SNCF} \quad (2.9)$$

where SCF is the stress concentration factor, SNCF is the strain concentration factor, ν is the Poisson’s ratio, ε_1 and ε_2 are strain components at the extrapolation region.

Ideally, hot spot stress of a CHS joint should only be dependent on the overall geometry and loads applied on the brace and on the chord. However, in practice, the hot spot stress is also affected by several fabrication factors produced during the welding process. They include the local configuration of the weld such as flat, convex or concave, as well as welding defects such as undercut and lack of fusion. In this study, these fabrication factors will not be considered as they are random and unpredictable (van Wingerde, Packer & Wardenier, 1995; Shao, 2004).

In the fatigue design guidelines, equations and charts for calculating the SCF rather than the hot spot stress are usually provided, and the SCF is defined as

$$SCF = \frac{\sigma_{HSS}}{\sigma_n} \quad (2.10)$$

where σ_{HSS} is the hot spot stress. SCF is an important parameter used in the fatigue design of CHS joints. Many existing standards provide equations and charts for calculating SCFs of different types of CHS joint such as CIDECT (Zhao et al., 2000), IIW (2009) and NORSK standard (1998). For CHS joints subjected to a single basic load, hot spot stress can be determined by Eq. (2.10), in conjunction with the nominal stress and the SCFs given in fatigue design guidelines. However, for a CHS joint subjected to combined load, it is not so easy to determine the hot spot stress due to the uncertainty of its position.

A simple and conservative expression for calculating the hot spot stress of a CHS joint subjected to combined loading is shown below:

$$\sigma_{HSS} = |SCF_{ax} \times \sigma_{ax}| + \sqrt{(SCF_{ipb} \times \sigma_{ipb})^2 + (SCF_{opb} \times \sigma_{opb})^2} \quad (2.11)$$

where SCF_{ax} , SCF_{ipb} and SCF_{opb} are the peak values of stress concentration factors under single axial load, in-plane and out-of-plane bending respectively; σ_{ax} , σ_{ipb} and σ_{opb} are the corresponding nominal stresses. Eq. (2.11) was recommended by API-RP-2A (2005) in its 1993 version. Obviously, hot spot stress in Eq. (2.11) is essentially summing up the products of the nominal stresses and the corresponding peak SCFs under each basic loading, without considering the actual location of the hot spot stress. Therefore, hot spot stress calculated by Eq. (2.11) tends to be too conservative if all three basic loads are involved as the hot spot stresses under each of them located at different positions.

An alternative and more accurate method to predict the hot spot stress of a CHS joint subjected to combined load was proposed by Gulati et al. (Gulati, Wang & Kan, 1982), and it is expressed as

$$\sigma_{\text{HSS}}(X) = \text{SCF}_{\text{ax}}(X) \times \sigma_{\text{ax}} + \text{SCF}_{\text{ipb}}(X) \times \sigma_{\text{ipb}} + \text{SCF}_{\text{opb}}(X) \times \sigma_{\text{opb}} \quad (2.12)$$

where $\text{SCF}_{\text{ax}}(X)$, $\text{SCF}_{\text{ipb}}(X)$ and $\text{SCF}_{\text{opb}}(X)$ are the distributions of the SCFs under three basic loads along the entire weld profile respectively, and X is the angle along the weld toe. The position and value of the hot spot stress can be determined by Eq. (2.12) as long as the distributions of the SCFs under three basic loads are provided. This method has been proven to be accurate and used by many researchers (Yeoh et al., 1995; Soh & Soh 1996; Chang & Dover, 1999; Shao, 2006; 2007a; 2007b). However, there are no explicit equations available for the distributions of SCFs under three basic loads till date. The only way to determine the distributions of SCFs under three basic loads is to carry out experimental tests or FE analyses. Eq. (2.12) is incorporated in the current version of API-RP-2A (2005).

2.2.3 Fatigue Life of CHS Joints

Once the hot spot stress of a CHS joint is determined, its fatigue life can be calculated by using the S-N curves approach. When a CHS joint is subjected to cyclic loadings, the hot spot stress will also fluctuate. The difference between the maximum and the minimum hot spot stresses is defined as the hot spot stress range (ΔS). After the hot spot stress range has been determined, the number of cycles to failure defined as fatigue life can be determined from the corresponding S-N curve. Fatigue failure of any CHS joint is in the form of crack or crack-like defect. There are three different definitions of fatigue failure recommended by DEn (1993), and they are listed as follows:

N_1 : first discernible surface crack detected by any available method. This stage is considered to have passed if the crack length is larger than 20mm.

N_2 : first through-thickness crack as detected either visually or more accurately noticing loss of internal pressure or by monitoring of strain gauges positioned adjacent to the deepest part of the crack.

N_3 : end of test as caused by complete separation of the brace member, extensive cracking, and limitation of loading actuator stroke.

Obviously, it may be too conservative if N_1 is taken as the fatigue life of a CHS joint. This is because the CHS joint can experience much more cycles of fatigue load before the crack or the crack-like defect penetrates the chord thickness, and it has been observed both in laboratory tests and in practice. If N_3 is taken as the fatigue life of a CHS joint, it may be too dangerous as at this stage, there is little time left for repair. Therefore, it is more reasonable to choose N_2 as the final fatigue failure stage of a CHS joint (Huang, 2003; Shao, 2004). It implies that an offshore structure can still be in service after the surface crack is initiated at the joint region. When a crack is present at the weld toe of a CHS joint, its residual fatigue life can be determined by using linear elastic fracture mechanics (LEFM) in conjunction with the Paris' crack propagation law (Paris & Erdogan, 1963).

2.3 RESIDUAL FATIGUE LIFE OF CRACKED CHS JOINTS

2.3.1 Paris' Crack Propagation Law

In order to estimate the residual fatigue life of a cracked CHS joint under fatigue loading, it is not necessary to check the stress at the crack tip as it will not exceed the yield strength of the material due to the formation of the plastic zone. Instead, it is necessary to determine the stress intensity factor K , as recommended in Paris' crack propagation law (Paris & Erdogan, 1963). In Paris' crack propagation law, the stress intensity factor range is associated with the advancement of the crack depth in a number of load cycles, and it is expressed as

$$\frac{da}{dN} = C(\Delta K)^m \quad (2.13)$$

where da/dN is the fatigue crack growth rate in m/cycle; C and m are material constants and ΔK is the range of stress intensity factor. The total number of cycles to final failure is the sum of the number of cycles for crack initiation phase and crack propagation phase, and it can be calculated by integrating both sides of Eq. (2.13). Therefore, the residual fatigue life of a cracked CHS joint can be estimated from

$$N_r = \int_{a_i}^{a_f} \frac{da}{C(\Delta K)^m} \quad (2.14)$$

where a_i and a_f are crack depth at crack initiation and final fracture respectively. In order to obtain a more accurate estimation result, the crack depth from a_i to a_f is normally divided into many integration sub-regions, and then the residual fatigue life N_r can be calculated by summing up the number of cycles in all these sub-regions.

According to LEFM, the basic crack modes are divided into three types, e.g. Mode-I, Mode-II and Mode-III as illustrated in Fig. 2.5. It has been shown that surface cracks in CHS joints are normally in a mixed mode which involves all three basic modes. However, Mode-I is the dominant one, and usually the effect of Mode-II and Mode-III can be ignored (Huang & Hancock, 1988; Rhee, 1989; Bowness & Lee, 1995; Yang, 1996; Huang, 2003; Shao, 2004; Qian et al., 2006). Generally, the SIFs at the deepest point of the surface crack in a CHS joint are the main concern. This is because it is more reasonable to define the fatigue failure as and when the crack penetrates the chord thickness. This has been explained in Sub-section 2.2.3. The general form of the stress intensity factor of a CHS joint is given by

$$K = Y(g)\sigma_n\sqrt{\pi a} \quad (2.15)$$

where $Y(g)$ is the shape factor depends on the geometry of the CHS joint and the shape of the crack, σ_n is the nominal stress and a is the crack depth.

2.3.2 Stress Intensity Factor of Cracked CHS Joints

Huang and Hancock (Huang & Hancock, 1988) carried out FE analysis on the stress intensity factor of cracked CHS T-joints containing a semi-elliptical surface crack subjected to the axial load. The FE mesh models were constructed by using shell elements, with the crack modeled using line spring elements. The surface crack is located at the saddle of the CHS T-joints. It was concluded that the surface crack is in a mixed mode, e.g. K_I , K_{II} and K_{III} have contribution to K_e , but K_I is the dominant one. In addition, it was found that for a shallow crack ($a/t_0 < 0.2$), the shell elements cannot produce the correct results as they do not take account of the local stress field amplified by the stress concentration due to the weld profile. For a very deep crack ($a/t_0 > 0.8$), the FE analyses

produced significantly lower results than that derived from the experimental data. The remaining ligament of the surface crack is almost certainly in a plastic state, and thus, the residual fatigue life estimated from Paris' crack propagation law is no longer reliable for a very deep crack. Rhee (Rhee, 1989) studied the stress intensity factor of cracked CHS X-joints containing a surface crack using 3D solid elements, and then estimated the fatigue crack growth of the surface cracks. It concluded that surface crack in cracked CHS X-joint are in a mixed mode too, but K_I is the dominant one. K_I and K_{II} solutions along the crack front are almost symmetric, whereas K_{III} solutions are approximately anti-symmetric. Nwosu and Olowokere (Nwosu & Olowokere, 1995) evaluated the stress intensity factors for cracked CHS T-joints containing a surface crack by using line spring and shell elements. The distributions of the stress intensity factors (K_I) along the crack front for axial, in-plane and out-of-plane loading were reported. On important conclusion of this report was that the line spring and shell elements were unable to produce good results of SIF at two crack ends of the surface crack. Bowness and Lee (Bowness & Lee, 1995) developed a mesh generator for generating FE mesh models of cracked CHS T-joints using 3D solid elements, considering double curvature of the surface crack along the chord thickness and the weld toe. The SIFs at the deepest points of analyzed CHS T-joints were derived by using displacement extrapolation method. The curvature of the surface crack along the chord thickness was defined by using a 4th order polynomial. It was found that the curvature along the chord thickness had a significant effect on K_{II} and K_{III} rather on K_I . As K_I is the dominant one and has a larger value than K_{II} and K_{III} , the curvature along the chord thickness can be ignored in the FE analysis. Huang (Huang, 2003) carried out fatigue test and FE analysis on cracked CHS T-joints under combined loading. The crack propagation was monitored by using alternating current potential drop (ACPD) technique, and then the SIFs at the crack ends and the deepest points were derived in conjunction with Paris' crack propagation law. In order to calculate the SIFs of the tested CHS T-joints using FE analysis, a mesh generator was developed based on the model proposed originally by Wong (Lie et al., 2003), and then validated against experimental results. Finally, Chiew et al. (Chiew, Lie, Lee & Huang, 2004b) proposed a series of equations for calculating the SIFs at the crack ends and the deepest point of cracked CHS T/Y-joints under different types of basic loading. These equations were regressed from a large number of FE analyses. The general form of all the proposed equations is expressed as

$$K = \sigma_n Y_g Y_s Y_i Y_\theta \sqrt{\pi a} \quad (2.16)$$

where Y_g is the joint geometry factor; Y_i is the crack size factor; Y_s is the joint and crack coupling factor; Y_θ is the factor to allow for the chord to brace angle; σ_n is the nominal stress and a is the crack depth. To continue research work on fatigue analysis of CHS joints, Shao (Shao, 2004) carried out both experimental test and FE analysis on two full scale CHS K-joints subjected to balanced axial loadings. ACPD technique was employed again in the fatigue tests on the two CHS K-joints. The FE mesh models of cracked CHS K-joints were generated by adding a brace to a cracked Y-joint which was originally developed by Wong (Lie et al., 2003). Finally, Shao and Lie (Shao & Lie, 2005) proposed two different equations for calculating the SIFs at the deepest points of cracked CHS K-joints subjected to balanced axial load and in-plane bending, respectively. The general form of all the equations is expressed as

$$K = \sigma_n Y_g Y_c \sqrt{\pi a} \quad (2.17)$$

where σ_n is the nominal stress; Y_g is the geometry factor and Y_c is the crack shape factor.

Apart from the FE analyses and regression equations discussed above, there are also some semi-empirical models available for calculating SIFs of cracked CHS joints (Shao, 2004). Among of them, the one proposed by Bowness and Lee (Bowness & Lee, 1996; Lee & Bowness, 2001; 2002) has been accepted by many researchers. K factors of a cracked T-butt joint is expressed as

$$K = (M_{km} M_m \sigma_m + M_{kb} M_b \sigma_b) \sqrt{\pi a} \quad (2.18)$$

Lee and Bowness (Lee & Bowness, 2001; 2002) approximated K factors of cracked CHS joints by introducing the parameter named degree of bending, DOB in to Eq. (2.18), and then Eq. (2.18) was modified and rewritten in the following form:

$$K = (M_{km} M_m SCF \times (1 - DOB) + M_{kb} M_b SCF \times DOB) \sigma_n \sqrt{\pi a} \quad (2.19)$$

where a is the crack depth, M_{km} and M_{kb} are the weld toe magnification factors, M_m and M_b are the plain plate shape factor, and the subscripts m and b denote membrane and bending load respectively. M_{km} and M_{kb} are parameters proposed by Lee and Bowness (Lee &

Bowness, 2001; 2002). M_m and M_b are parameters proposed by Newman and Raju (Newman & Raju, 1981). SCF is the stress concentration factor and DOB is the degree of bending at the would-be location of the crack and σ_n is the nominal stress in the reference brace of the joint. The DOB, SCF and σ_n can be obtained from uncracked CHS joints, and M_{kj} , M_j ($j=m, b$) can be calculated from parametric equations. It is clear that Eq. (2.19) is a very convenient method for estimating the stress intensity factor of any cracked CHS joint as it avoids the complexity of generating the mesh of the surface crack. Bowness and Lee (Bowness & Lee, 2002) evaluated Eq. (2.19) by making a direct comparison to the SIF of cracked CHS joints, including CHS T/Y-, X-, K-, TT-, KK-, DT- and DK-joints. Comparison of the predicted K factors and the FE results shows good agreement. However, the agreement deteriorates at the deepest point for deep cracks. This is because the stress field used to determine DOB and SCF is based on the uncracked CHS joints. When a surface crack is present, the crack disturbs the stress field causing load redistribution. In addition, DOB and SCF values are determined using the stress at the center of the crack location. Therefore, accuracy deterioration of Eq. (2.19) is expected as well at the crack ends for deep and wide cracks. It should be noted that DOB and SCF values are from FE analyses when Bowness and Lee (Bowness & Lee, 2002) evaluated Eq. (2.19). If regressed equations of DOB and SCF are used, certain amount of conservatism may be brought into K factors of cracked CHS joints.

Lie et al. (Lie et al., 2012) had estimated the stress intensity factors of cracked CHS K-joints subjected to balance axial load, and CHS T/Y-joints subjected to three basic loadings by using both FE analysis and Eq. (2.19). All the parameters in Eq. (2.19) are obtained from relevant codes and references. It is found that in general, Eq. (2.19) overestimates the stress intensity factor of cracked CHS T/Y- and K-joints significantly as compared to the FE results.

2.4 SAFETY AND INTEGRITY ASSESSMET OF CRACKED CHS JOINTS

A defect-free metallic component is deemed to be safe as long as the yield stress is greater than the applied stress. When a crack is present in the component, the guidelines given in existing design codes are no longer valid because it does not take into account of the fracture behavior of the crack. The safety of the structure can then be analyzed in terms of a critical applied load or crack size. The critical applied load and crack size must allow for the crack tip fracture parameters. If the deformation behavior of the structure is linear

elastic and the plastic zone of the crack satisfies the small yielding condition, then stress intensity factor K can be used to determine the critical applied load and crack size. In practice, most cracked components behave in an elastic-plastic manner, and it also may not satisfy the small yielding condition. These make the fracture assessment of such cracked components very complex, and thus difficult to derive explicit solutions. In this situation, other methods are required to evaluate these cracked components that behave in an elastic-plastic manner. In the past decades, several such methods have been developed (Dowling & Townley, 1975; Ainsworth, 2000; Zerbst et al., 2000). In these methods, two different routes are adopted, and they are failure assessment diagram (FAD) and crack driving force (CDF) respectively. In principle, these two routes can be made fully compatible. Some of them prefer to use one of the two routes, whereas others adopt both routes. In the FAD route, a geometry independent failure line is provided. The safety of a cracked component is determined by the position of an assessment point. The assessment point indicates the current state of the local crack area (considering crack tip fracture parameters) and the global behavior of the cracked component (considering plastic collapse of the cracked component). The cracked component is deemed to be safe as long as the assessment point falls inside the shaded area below the failure line; otherwise, it is deemed to be potentially unsafe. In contrast to the FAD route, the crack driving force under external loads and the required fracture resistance of the material must be determined separately in CDF route. As long as the crack driving force under current external loads is less than the fracture resistance of the material, the cracked component is deemed to be safe. Both FAD and CDF approaches aim to produce conservative results. Therefore, if an analysis leads to an unsafe prediction, this does not mean the cracked component will fail. It only implies a higher level analysis is required.

Many guidelines such as R6 Revision 4 (2000), API 579-1 (2007), FITNET (Koçak et al., 2008) and BS7910 (2013) provide procedures to assess the safety and integrity of cracked metallic components. In BS7910 (2013), there is an Annex B which concerns with the safety and integrity of cracked CHS joints. The present study follows the procedure in BS7910 (2013).

2.4.1 FAD Approach in BS7910

In BS7910 (2013), there are three options (Option 1, 2 and 3) of fracture assessment available. The choice of option depends on the materials involved, the input data available

and the conservatism required. In general, a higher option analysis will produce a more accurate prediction results. The three options are as follows:

- (a) Option 1 is the normal assessment route, and does not require detailed stress-strain data. It may also be applicable to materials which display yield discontinuity;
- (b) Option 2 is the specific material assessment route, and it requires the mean uniaxial tensile true stress-strain data;
- (c) Option 3 is the highest analysis level. It is not for general usage, and is specific to a particular material, geometry and loading type.

The philosophy of the fracture assessment in BS7910 (2013) is that the safety of the flawed component is determined by the position of the assessment point in FAD. The position of the assessment point is determined by K_r and L_r .

K_r is expressed as

$$K_r = \frac{K_I}{K_{mat}} \quad (2.20)$$

where K_I is the stress intensity factor considering the primary and secondary loads, and K_{mat} is the fracture toughness.

L_r is expressed as

$$L_r = \frac{P}{P_{L(a, \sigma_y)}} = \frac{\sigma_{ref}}{\sigma_y} \quad (2.21)$$

where $P_{L(a, \sigma_y)}$ is the rigid plastic limit load for flaw size a and yield strength σ_y ; σ_{ref} can be obtained from standard handbooks or FE analysis; σ_f is the flow strength, and it is the arithmetic mean of the yield strength and the ultimate tensile strength of the material.

Option 1 is the normal assessment route for general application in BS7910 (2013), and it is shown in Fig. 2.6. The equations describing the Option 1 FAD curve are expressed as

$$f(L_r) = (1 + 0.5L_r^2)^{-0.5} \left[0.3 + 0.7 \exp(-\mu L_r^6) \right] \quad L_r \leq 1 \quad (2.22)$$

$$f(L_r) = f(1)L_r^{(N-1)/(2N)} \quad 1 < L_r < L_{r\max} \quad (2.23)$$

$$f(L_r) = 0 \quad L_r \geq L_{r\max} \quad (2.24)$$

where:

$$\mu = \min(0.001 \frac{E}{\sigma_y}, 0.6) \quad (2.25)$$

$$N = 0.3 \left(1 - \frac{\sigma_y}{\sigma_u} \right) \quad (2.26)$$

For material which exhibits a yield discontinuity in the stress-strain curve, or for which it cannot be assumed with confidence that no discontinuities exist, Eqs. (2.22) - (2.26) should be replaced by Eqs. (2.27) - (2.30).

$$f(L_r) = (1 + 0.5L_r^2)^{-0.5} \quad L_r < 1 \quad (2.27)$$

$$f(L_r) = (\lambda + \frac{1}{2\lambda})^{-0.5} \quad L_r = 1 \quad (2.28)$$

$$f(L_r) = f(1)L_r^{(N-1)/(2N)} \quad 1 < L_r < L_{r\max} \quad (2.29)$$

$$f(L_r) = 0 \quad L_r \geq L_{r\max} \quad (2.30)$$

where λ is expressed as

$$\lambda = 1 + E \frac{\Delta \epsilon}{R_{el}} \quad (2.31)$$

where $\Delta \epsilon$ and R_{el} can be determined according to BS7910 (2013). As they are not relevant to this study, no more details are provided here.

Option 2 is suitable for all types of parent material and weld metal. It is expected to generate more accurate prediction results than Option 1 but requires significantly more data. It requires a specific stress-strain curve. When $L_r \leq L_{r\max}$, the FAD curve for Option 2 is expressed as

$$f(L_r) = \left(\frac{E \epsilon_{ref}}{L_r \sigma_y} + \frac{L_r^3 \sigma_y}{2E \epsilon_{ref}} \right)^{-0.5} \quad L_r \leq L_{r\max} \quad (2.32)$$

$$f(L_r) = 0 \quad L_r > L_{r\max} \quad (2.33)$$

where ϵ_{ref} is the true strain obtained from the uniaxial tensile stress-strain curve at a true stress, $L_r \sigma_y$.

Option 3 is the highest level in BS7910 (2013) and can be used for ductile tearing analysis of a particular cracked component. The FAD curve of Option 3 is expressed as

$$f(L_r) = \sqrt{J_e / J_{ep}} \quad L_r \leq L_{r\max} \quad (2.34)$$

$$f(L_r) = 0 \quad L_r > L_{r\max} \quad (2.35)$$

where J_e and J_{ep} are the values corresponding to the same load, and they can be obtained from FE analysis.

It is very important to recognize that Option 1 FAD curve is deemed toward the lower bound of all FAD curves. For cracked CHS joints, including T/Y-, X- and K-joints, it has been found that Option 1 FAD curve may produce non-conservative prediction as part of Option 3 curve may fall inside the shaded area enclosed by the Option 1 curve. For cracked multi-planar CHS TT-joints, it is also necessary to check the conservatism of the Option 1 curve by making a direct comparison with the Option 3 curve which is fully constructed using FE results. It is also important to recognize that ductile tearing analyses can be performed with any of the FAD option given in BS7910 (2013) and not just Option 3.

2.4.2 Fracture Assessment of Cracked CHS Joints

As mentioned previously, there are few publications on fracture assessment of cracked CHS joints. Laham and Burderkin (Laham & Burderkin, 1995) applied FAD approach to assess the safety and integrity of cracked CHS joint, but it mainly concentrated on CHS T-joint, K-joint and multi-planar TT-joint with through-thickness crack only. Later, it was confirmed that FAD approach can be used in practice but one needs to be very careful in choosing the suitable assessment curve. Zerbst (Zerbst et al., 2002a) led a study on the fracture behavior of welded CHS T-joints containing a surface crack at the saddle. Four cracked CHS T-joints were tested to failure under brace axial loading in GKSS research center, Germany. A series of research work was then carried out by a number of

researchers, and the fracture behavior of the cracked welded CHS T-joints were examined by using different methods, including R6 analysis (Marshall & Ainsworth, 2002), UK BS7910 (2013) methodology (Burdekin, 2002b), engineering treatment model (Zerbst, Primas, Schindler & Schwalbe, 2002b), screening method (Schindler, Primas & Veidt, 2002) and design curve conforming to WES 2805-1997 (Zerbst & Miyata, 2002c). Zerbst (Zerbst et al., 2002a) summarized all the findings and concluded that the results of different methods ranged from ‘acceptably conservative’ to ‘slightly non-conservative’. Recently, Qian (Qian, 2013) carried out an FAD analysis on CHS X- and K-joints, and it was found that the Option 1 curve in BS7910 (2013) could not be used directly in analyzing cracked X- and K-joints, and a new formula of FAD curve was proposed subsequently.

In order to carry out fracture assessment on cracked CHS joints using FAD approach in BS7910 (2013), several parameters must be determined first, including the plastic collapse load (P_c), the stress intensity factor (K), elastic J -integral (J_e) and elastic-plastic J -integral (J_{ep}). The stress intensity factor, K has already been discussed in Sub-section 2.3.2.

2.4.3 Plastic Collapse Load of Cracked CHS Joints

In FAD analysis for cracked CHS joints, the horizontal axis defines the load ratio and it is expressed as

$$L_r = \frac{P_a}{P_c} \quad (2.36)$$

where P_a is the applied load and P_c is the plastic collapse load. Eq. (2.36) is only valid for cracked CHS joints subjected to axial load. For cracked CHS joints subjected to combined load, L_r is given as

$$L_r = \left| \frac{P_a}{P_c} \right| + \left| \frac{M_{ai}}{M_{ci}} \right|^2 + \left| \frac{M_{ao}}{M_{co}} \right| \quad (2.37)$$

where M_{ai} is the applied in-plane bending, M_{ao} is the applied out-of-plane bending, M_{ci} is the in-plane plastic collapse moment and M_{co} is the out-of-plane plastic collapse moment. It can be seen that plastic collapse load/moment of cracked CHS joints is an important parameter used in FAD analysis as it determines the horizontal position of the assessment point. It is important to recognize that Eqs. (2.36) and (2.37) are different to the original

formulas given in BS7910 (2013) because a term σ_f/σ_y on the right side has been removed. This is because material strain hardening effect is considered in this study. It may be too conservative to add the term σ_f/σ_y to Eqs. (2.36) and (2.37). In addition, Eq. (2.37) is only an approximation for cracked CHS joints subjected to combined loading. The accuracy of Eq. (2.37) has never been verified against any experimental or numerical results.

In BS7910 (2013), a strength reduction factor F_{AR} is recommended to calculate the plastic collapse load of cracked CHS joints in conjunction with the characteristic strength of the corresponding uncracked joints. F_{AR} is given as

$$F_{AR} = \left(1 - \frac{A_c}{t_0 l_w}\right) \left(\frac{1}{Q_\beta}\right)^{m_q} \quad (2.38)$$

where A_c is the crack area, t_0 is the chord thickness, l_w is the weld toe length, Q_β is a parameter used to allow for larger β effect ($\beta > 0.6$), and m_q is used to account for different crack types. For surface crack, $m_q = 0$, whereas for through-thickness crack, $m_q = 1$. For surface crack, A_c is given as

$$A_c = 0.5\pi ac \quad (2.39)$$

Q_β is defined as

$$\begin{cases} Q_\beta = 1 & \beta \leq 0.6 \\ Q_\beta = \frac{0.3}{[\beta(1-0.833\beta)]} & \beta > 0.6 \end{cases} \quad (2.40)$$

It is important to recognize that Eq. (2.38) is only applicable to specific cracked uni-planar CHS joints subjected to axial tensile load only. For cracked CHS joints containing a surface crack subjected to in-plane and out-of-plane bending, currently there are no such similar equations. Therefore, it is necessary to investigate the in-plane and out-of-plane plastic collapse moments of cracked CHS joints containing a surface crack at the weld toe on the chord.

Gibstein and Moe (Gibstein & Moe, 1986) tested the residual static strength of four cracked CHS T-joints. All the four cracked CHS T-joints were designed to produce failure by brittle fracture by controlling the material property and test temperature. It was

concluded that brittle fracture may indeed be a possible failure mode of cracked CHS joints when low toughness, crack like defects and tensile loads are combined. For cracked CHS joints failed by brittle fracture manner, when the crack depth reaches 10-15% of the chord thickness, the ultimate static strength may decrease drastically up to 50%. Tests designed to produce failure by brittle fracture were also carried out by Machida et al. (Machida, Hagiwara & Kajimoto, 1987), and similar findings were observed. Skallerud et al. (Skallerud et al., 1994) carried out both experimental tests and FE analyses on uncracked and cracked CHS T-joints subjected to axial tensile loading and out-of-plane bending. All tested specimens failed in a ductile mode with stable crack growth at the last loading stage.

A series of research programs on the safety and integrity assessment of cracked CHS joints were performed at University of Manchester Institute of Science & Technology (UMIST), UK. In these research programs (Burdekin & Fordin, 1987; Cheaitani & Burdekin, 1994; Laham & Burdekin, 1995; Burdekin & Yang, 1997; Burdekin & Cowling, 1998; Hadley et al., 1998), experimental tests and FE analyses were combined to investigate the ultimate strength of cracked CHS joint. A fracture procedure based on FAD was proposed. Stacey et al. (Stacey et al., 1996a; 1996b) summarized all the experimental test and FE results in UMIST up to 1996. In 2002, Burdekin (Burdekin, 2002a) summarized all the experimental test and FE results on cracked CHS joints in UK again. In his report, some new research works after 1996 are covered. The main conclusion of all the research works in UMIST can be summarized as:

- 1) The plastic collapse load of cracked CHS T/Y-, X- and K-joints can be determined by multiplying a strength reduction factor on the corresponding characteristic strength of uncracked CHS joints. The strength reduction factor was incorporated in BS7910 (2013) later and expressed as Eq. (2.38).
- 2) In order to take into account of the fracture behavior, FAD analysis should be carried out on cracked CHS joints. FAD curves of cracked CHS joints can be constructed through *J*-integral by using Eqs. (2.34) and (2.35).

The most recent research work related to the plastic collapse load of cracked CHS joints were carried out by Lie et al. (Lie et al., 2014). Five cracked CHS T-joints containing single surface crack with various crack area were tested to failure. All the cracked

specimens failed in a ductile manner. It was found that the strength reduction factor, F_{AR} in BS7910 (2013) is very conservative.

In reviewing the research works on the prediction of the plastic collapse load of cracked CHS joints, it was found that only the load displacement curves of cracked CHS joints were plotted in most references, together with the corresponding load displacement curves of the uncracked joints. Very few references provided the detailed procedure to determine the plastic collapse load. Cheaitani and Burdekin (Cheaitani & Burdekin, 1994) suggested determining the plastic collapse load by using the twice elastic compliance (TEC) shown in Fig. 2.7, and it is expressed as

$$\tan \varphi = 2 \tan \theta' \quad (2.41)$$

Load displacement curves of both uncracked and cracked CHS joints are plotted together. The plastic collapse load of the uncracked CHS joint can be determined by using the twice elastic compliance or other compliances, and then, the plastic collapse loads of the corresponding cracked CHS joints can be determined under the same criteria of the uncracked joint. The inherent assumption of such strategy is that plastic collapse behaviour of the uncracked and cracked CHS joints are reached simultaneously. It is apparent that this assumption may not be true. As the crack is present on the chord, the net area of the chord used to carry the load transferred from the brace is reduced. Therefore, the plastic collapse behaviour of the cracked CHS joints should occur earlier comparing to the uncracked joint. The main challenge is to determine the plastic collapse load of the uncracked CHS joints. Both twice elastic compliance and Lu's deformation limit (Lu et al., 1994) have been used to determine the plastic collapse load of the uncracked CHS joints in this study. This is because twice elastic compliance is the only one which has been used consistently to determine the plastic collapse load of cracked CHS and RHS joints. One of the objectives of this study is to validate the accuracy of the twice elastic compliance on determining of the plastic collapse loads of uncracked CHS joints. Lu's deformation limit has been widely accepted to determine the ultimate strength of uncracked CHS joints. It has never been used to determine the plastic collapse load of cracked CHS joints. Another objective of this study is to examine the suitability of Lu's deformation limit to determine the plastic collapse load of uncracked and cracked CHS joints.

2.4.4 Elastic and Elastic-plastic J -integral of Cracked CHS Joints

J -integral originally proposed by Rice (Rice, 1968) is a very important parameter in fracture mechanics. It can be expressed as

$$J_I = \int_{\Gamma} \left[w dy - T_i \frac{\partial u_i}{\partial x} ds \right] \quad (2.42)$$

Where Γ is the path of the integral that encloses the crack tip, w is the strain energy density for an elastic body, T_i is the traction vector defined according to the outward normal along Γ , and u_i is the displacement vector.

As mentioned previously, the Option 3 FAD curve specified in BS7910 (2013) is constructed using the values of elastic J -integral (J_e) and elastic-plastic J -integral (J_{ep}). Finite element method can be used to calculate both J_e and J_{ep} quite accurately. In ABAQUS (2002) software, an interaction integration method proposed by Shih and Asaro (Shih & Asaro, 1998) is adopted to calculate the J -integral. The detailed derivation and calculation of the J -integral can be found in ABAQUS (2002), and in the reference presented by Shih and Asaro (Shih & Asaro, 1988). This interaction integration method has the advantage of being insensitive to mesh refinement, and it can also be used for calculating the stress intensity factors of cracked CHS joints.

Laham and Burdekin (Laham & Burdekin, 1995) investigated the elastic-plastic J -integral, J_{ep} of cracked CHS K-joints containing a through-thickness crack. Four contours were used in the FE modes of cracked CHS K-joints. Values from the first contour nearest to the crack front were omitted. J_{ep} was simply determined as the average of the other three contours. It is difficult to evaluate the accuracy of J_{ep} calculated by Laham and Burdekin (Laham & Burdekin, 1995) as very limited information is available. Yang (Yang, 1996) studied both J_e and J_{ep} of cracked CHS joint containing a through-thickness or surface crack. It was found that the difference of J_{ep} from different integral contours increases after excessive gross yielding occurs at the crack region. Yang (Yang, 2006) studied the fracture behavior of cracked RHS joints. Similar conclusion on J_{ep} was obtained, i.e. path independence of J -integral is no longer valid when large external loading is applied on cracked RHS joints.

Path dependence of J -integral always occurs in gross plasticity regime. Many researchers (Yagawa & Takahashi, 1984; Brocks & Yuan, 1989; Yuan & Brocks, 1991; Yan & Mai, 1998; Brocks & Scheider, 2001) recommend taking J -integral from contours far away from the crack tip but not close to the boundary. Generally, J -integral satisfies the relationship below

$$J_{\text{tip}} \leq J(r) \leq J_{\text{far-field}} \quad (2.43)$$

where J_{tip} and $J_{\text{far-field}}$ is the value from the contour nearest to the crack tip and far away from the crack tip, respectively. r is the distance from the crack tip. Therefore, the highest J -integral with increasing domain size is the closest to the $J_{\text{far-field}}$, and it should be taken as J_{ep} in the FE analysis.

2.5 DISCUSSIONS

For carrying out FAD analysis on cracked CHS joints containing a semi-elliptical surface crack, the main difficulty arises from the calculations of the stress intensity factors and the plastic collapse loads. For the stress intensity factors of cracked CHS joints, there are very limited available parametric equations published in the literature. For instance, Huang (Huang, 2003) and Shao (Shao, 2004) proposed separate equations based on FE results of cracked CHS T- and K-joints containing a semi-elliptical surface crack. Their FE results of the stress intensity factors have been validated against several experimental data. Huang (Huang, 2003) and Shao (Shao, 2004) carried out fatigue tests on three CHS T-joints and two CHS K-joints to validate their FE results. However, the validity range of some parameters used in the equations such as β and γ proposed by Huang (Huang, 2003) and Shao (Shao, 2004) are very large. Therefore, more experimental validation works covering the entire validity range of each parameter will be needed before these equations can be widely accepted.

The semi-empirical model for calculating the stress intensity factors of various cracked CHS joints given by Bowness and Lee (Bowness & Lee, 2001; 2002) was proposed based on FE results. It is a general method for calculating the stress intensity factors of various types of cracked CHS joints, including uni-planar and multi-planar joints. One limitation of this model is that the parameters SCF and DOB are generated based on FE results of the uncracked joints. This introduces uncertainty on the accuracy of the stress intensity factors

because the SCF and DOB cannot reflect the true stress and deformation of the corresponding cracked joints at the crack position, especially when the crack size is very large. In addition, for multi-planar CHS joints such as TT-joints, equations of SCF and DOB are unavailable. It can be concluded that there are still additional research works needed to be done before Bowness and Lee (Bowness & Lee, 2001; 2002) equation can be widely used in practice.

Another difficulty of using FAD method to assess the safety and integrity of cracked CHS joints is from the calculation of the plastic collapse load/moment. Plastic collapse load/moment is a very important parameter in FAD analysis because it determines the horizontal position of the assessment point. Currently, a strength reduction factor F_{AR} is recommended in BS7910 (2013) to calculate the plastic collapse load of cracked CHS joints. This F_{AR} is only applicable for uni-planar CHS T-, Y-, K- and X-joints subjected to axial load. For in-plane and out-of-plane bending, there is no such F_{AR} available. The strength reduction factors for in-plane and out-of-plane bending are very important because most of CHS joints are subjected to combined loading instead of single load in practice. In addition, there is no specification stating that F_{AR} in BS7910 (2013) is also applicable to multi-planar CHS joints. Therefore, it is also very important to study and propose a similar strength reduction factor for multi-planar CHS joints because most of CHS joints used in practice are multi-planar in nature.

Table 2.1 Equations for $S_{\text{rhs}}-N_f$ curves for CHS and RHS joints

For $10^3 < N_f \leq 5 \times 10^6$	$\log(S_{\text{rhs}}) = \frac{1}{3} \left(12.476 - \log(N_f) \right) + 0.06 \log(N_f) \log\left(\frac{16}{t_0}\right)$ or $\log(N_f) = \frac{12.476 - 3 \log(S_{\text{rhs}})}{1 - 0.18 \log\left(\frac{16}{t_0}\right)}$
For $5 \times 10^6 < N_f < 10^8$ (variable amplitude only)	$\log(S_{\text{rhs}}) = \frac{1}{5} \left(16.327 - \log(N_f) \right) + 0.402 \log\left(\frac{16}{t_0}\right)$ or $\log(N_f) = 16.327 - 5 \log(S_{\text{rhs}}) + 2.01 \log\left(\frac{16}{t_0}\right)$

Table 2.2 Linear extrapolation of stresses to the weld toe recommended by CIDECT (Zhao et al., 2000)

Distances from weld toe		Chord		Brace	
		Saddle	Crown	Saddle	Crown
CHS	$L_{r,\min}^*$	$0.4t_0$		$0.4t_1$	
	$L_{r,\max}^{**}$	$0.09r_0$	$0.4\sqrt{r_0 t_0 r_1 t_1}$	$0.65\sqrt{r_1 t_1}$	

*Minimum value for $L_{r,\min}$ is 4mm**Minimum value for $L_{r,\max}$ is $L_{r,\min} + 0.6t_1$

Fig. 2.1 S-N curves for fatigue design of CHS and RHS joints in CIDECT (Zhao et al., 2000)

Fig. 2.2 Geometrical parameters of a CHS T/Y-joint

Fig. 2.3 Stress distribution at the brace and chord surface of an X-joint

Fig. 2.4 Definition of extrapolation region on the chord surface of a CHS T-joint

Fig. 2.5 Three basic crack modes

Fig. 2.6 Option 1 failure assessment diagram (FAD) curve

Fig. 2.7 Twice elastic compliance criteria

CHAPTER 3

FINITE ELEMENT MESH GENERATION OF CRACKED CHS JOINTS

3.1 INTRODUCTION

Finite element (FE) method is frequently used to estimate the fatigue life and to evaluate the safety and integrity of cracked circular hollow section (CHS) joints. The complexity of the geometry coupled with the crack makes it very difficult to manually create the mesh model of a cracked CHS joint using any existing commercial FE software. Therefore, researchers have to develop their own special purpose automatic FE mesh generators to create the mesh models of different cracked CHS joints. In the earlier days, they used shell elements incorporating line spring elements due to performance limitation of computers used at those times (Huang & Hancock, 1988; Rhee, 1989). This approach can vastly decrease the node number, and hence, the number of degrees of freedom but it cannot accurately capture the steep stress gradient at the chord-brace intersection region resulting in inaccurate and unreliable results. In recent years, researchers tend to use solid elements (Bowness & Lee, 1998; Cao, Yang, Packer & Burdekin, 1998; Lie, Lee & Wong, 2003) and some of them have developed systematic approaches to create the FE mesh models of uni-planar cracked CHS joints.

Bowness and Lee (Bowness & Lee, 1998) developed a method to generate the FE mesh models of cracked CHS joints containing a surface crack using solid elements based on a T-butt joint model. In this approach, a quarter of the 3D T-butt mesh model with half a surface crack is generated first, and then it is mapped onto a CHS T-joint to create the mesh of the chord-brace intersection zone. For this mesh generator, the crack location at the deepest point of the surface crack is fixed only at the saddle or crown location of the generated cracked CHS joint. However, in practice, the deepest point can be located at anywhere along the weld toe of a cracked CHS joint, depending on the geometry and the loading conditions. Besides, as only a quarter mesh model is constructed, it means that there are always two identical opposite cracks in a cracked CHS T-joint. In reality, the surface crack is unsymmetrical in shape. FE mesh models of cracked CHS joints generated

by Bowness and Lee (Bowness & Lee, 1998) can only be used to calculate the stress intensity factor (SIF). Cao et al. (Cao et al., 1998) used another approach to generate the mesh model of a cracked CHS T-joint containing a surface crack. In their approach, a three-dimensional (3D) mesh generation procedure for CHS T-joint is developed based on a two-dimensional (2D) model mapping technique. In the program, a group of crack elements is generated and inserted into a flat plate model, and then it is mapped to form the cracked CHS T-joint mesh model. It is difficult to fully evaluate the effectiveness of the FE mesh models of cracked CHS T-joint created by Cao et al. (1998) as there is very few information available.

The most recent mesh generator for creating the FE mesh models of different cracked CHS joints is developed by the research group in Nanyang Technological University (NTU) (Lie, Lee & Wong, 2003; Lie et al., 2005). This mesh generator has been successfully used to calculate the SIFs of cracked CHS T- and K-joints containing a surface crack. As a continuation of the research work carried out by this research group, the mesh generator was also used to calculate the plastic collapse load and elastic-plastic J -integral of cracked CHS T-joints. It is found that the models generated by using this mesh generator have failed occasionally due to various limitations. Figs. 3.1(a) and (b) show the surface crack block generated by using this mesh generator and they are used to illustrate its limitations.

The first limitation of this mesh generator is from the overall mesh design concept. In order to generate the mesh models, mesh divisions along the chord-brace intersecting curve and the weld toe have to be equally divided into 32, 48 or 64 segments to form the mesh model of a cracked CHS joint. A surface crack block is then created and inserted into the corresponding uncracked model. As the crack length of the surface crack varies widely in practice, each crack end can be located within one segment, or can be located exactly at the border of two segments (Fig. 3.1(a)). The crack end is designed within one segment in this mesh generator. Otherwise, the mesh model of cracked CHS joint cannot be created. Therefore, this mesh generator is not robust enough to account for the flexible nature of the crack end position. In other words, when any one of the two crack ends is located exactly at the boarder of two segments, this mesh generator will fail completely.

The second limitation of this mesh generator is due to the poor mesh quality near the surface crack front (Fig. 3.1(b)). Even if the mesh model of a cracked CHS joint is created successfully, it does not guarantee a convergence in the calculations. In this mesh

generator, five types of element are adopted to construct the mesh of the surface crack block. The initial objective of using five types of element is to smoothen the mesh transition between the crack front and other regions. However, the mesh in the transition region has not been designed properly because some prism, tetrahedron and pyramid elements are generated always with very sharp angles (less than 10°). This can be visually observed from Figs. 3.1(a) and (b). When running an elastic-plastic analysis for calculating the plastic collapse load and elastic-plastic J -integral of a cracked CHS joint, the calculation is frequently aborted when large strain is reached in this transition region. This convergence problem is caused by the poor mesh quality.

The third limitation of this mesh generator is the element type used at the crack tip. Originally, the FE mesh models created are used to calculate the SIFs along the crack front. In this mesh generator, 8 number of 15-node collapsed elements are designed to enclose the crack front and they share one node at the crack tip. In reality, when large loading is applied, crack blunting will occur at the crack tip. As 8 elements enclosing the crack front share one node at the crack tip, crack blunting cannot develop freely. A type of untied nodes at the crack tip is recommended by Yang (Yang, 2006). For the untied node crack tip, 8 nodes at the crack tip are deployed and they occupy the same position. As these 8 nodes are untied, crack blunting can develop freely. However, the case of untied node crack tip will also raise another convergence problem in the elastic-plastic analysis. As all the nodes occupy the same position, they may collide with each other, and thus making the convergence of the calculation unachievable after loading is applied at the brace end. Therefore, it is necessary to redesign a new type of mesh at the crack tip to make crack blunting developing freely and avoiding the convergence problem in the elastic-plastic analysis.

In order to carry out failure assessment diagram (FAD) analysis on cracked CHS joints containing a surface crack at the chord weld toe, the plastic collapse load and elastic-plastic J -integral must be determined cautiously. From the above discussions, it can be seen that FE mesh models of cracked CHS joints created by the previous mesh generator cannot be used to carry out FAD analysis. Therefore, it is decided to develop a new and robust mesh generator for creating the FE mesh models of any cracked CHS joint. This chapter will focus mainly on the mesh generation of uni-planar cracked CHS T/Y-joint. Firstly, the geometry of the uncracked and cracked CHS T/Y-joints is introduced.

Subsequently, mesh generation of a cracked plain plate, T-butt joint and CHS T/Y-joint are illustrated in sequence. Meanwhile, some mesh design key points such as element type at the crack tip, mesh design of the surface crack front and mesh refinement schemes are presented. Finally, extensive tests are carried out to verify the accuracy and convergence of the new developed mesh generator on cracked CHS T/Y-joints.

3.2 GEOMETRY ANALYSIS OF UNCRACKED CHS T/Y-JOINTS

CHS T/Y-joint is the simplest configuration in the CHS joint family. Any other different types of CHS joint can be constructed by adding more braces on the chord of a CHS T/Y-joint. Figs. 3.2(a) and (b) illustrate the geometry of a uni-planar CHS T/Y-joint and a multi-planar CHS TT-joint. Dimensionless geometrical parameters and several critical locations are also indicated in Figs. 3.2(a) and (b). It can be seen that the multi-planar CHS TT-joint can be constructed by adding one out-of-plane brace on the uni-planar T/Y-joint.

3.2.1 Uncracked CHS T/Y-joint without Weld

In order to generate the mesh model of a circular tube, a flat plate mesh model is generated first, and then a mapping technique is used to form the mesh model of the tube. Fig. 3.3 shows the mapping of a plane ($X=R$) to a circular surface ($X^2+Y^2=R^2$). If the circular tube is cut by a longitudinal line 1-1 and opened out along the line, a point A on the tube will be mapped to the point A' on the plane $X=R$. From the cross-section X-X shown in Fig. 3.3, the arc length y' is equal to the distance 1-A' on the plane. Obviously, this can be used to define the mapping relationship between the two surfaces. The relationship between the circular surface and the planes, i.e. the X - Y - Z and the X' - Y' - Z' co-ordinate systems, can be defined as

$$R\omega = y' \quad (3.1)$$

where ω is the local polar angle defined on the chord circle.

By using this mapping technique, a space curve on the circular surface is transformed to a planar curve on a flat plane as illustrated in Fig. 3.4. The chord and brace two coordinate systems can be defined

–for the chord

$$X^2 + Y^2 = R^2 \quad (3.2)$$

–for the brace

$$x^2 + y^2 = r^2 \quad (3.3)$$

The intersecting curve of the chord and the brace, which is a 3-D curve, is defined by Eqs. (3.2) and (3.3) and can be expressed in the X - Y - Z coordinate system as

$$\left[(X - R) \cos \theta - Z \sin \theta \right]^2 + Y^2 = r^2 \quad (3.4)$$

As shown in Figs. 3.3 and 3.4, the outer surface of the chord can then be transformed to a flat plane and it is defined on the Y' - Z' plane by

$$R^2 \sin^2 \left(\frac{Y'}{R} \right) + \left[Z' \sin \theta + R \left(1 - \cos \frac{Y'}{R} \right) \cos \theta \right]^2 = r^2 \quad (3.5)$$

Although Eq. (3.5) can be used to define the intersecting curve directly, it is more convenient to define the intersecting curve using a circle on a planar system. Therefore, a local planar u - v coordinates system is used to define the circle, and then it is mapped to fit the intersecting curve in the Y' - Z' plane. The Y' - Z' plane will then be further mapped or wrapped around to form a tube. As a result, a 3D intersecting curve is formed in the X - Y - Z coordinates system after completing two mappings as illustrated in Fig. 3.5 (plan view). In Fig. 3.5, α is defined as the polar angle in the u - v coordinate system. The angle ψ is the corresponding angle defined in the Y - Z plane. From Fig. 3.5, a circle of radius r can be expressed as

$$u^2 + v^2 = r^2 \quad (3.6)$$

$$\begin{cases} u = r \sin \alpha \\ v = r \cos \alpha \end{cases} \quad (3.7)$$

If Eq. (3.6) is made equivalent to Eq. (3.5), then after carrying out some simple calculations, it can be seen that the intersecting curve in the X - Y - Z coordinate system can be expressed as

$$\begin{cases} X = R \cos(\sin^{-1} \frac{u}{R}) \\ Y = R \sin(\sin^{-1} \frac{u}{R}) = u \\ Z = Z' = \left[v - R \left(1 - \cos \frac{Y'}{R} \right) \cos \theta \right] \frac{1}{\sin \theta} \end{cases} \quad (3.8)$$

As for the angle ψ , it is the corresponding polar angle defined in the Y - Z plane and can be expressed as

$$\psi = \tan^{-1} \left(\frac{Y}{Z} \right) \quad (3.9)$$

3.2.2 Weld Shape

Fig. 3.6 shows the typical weld shape of an arbitrary CHS joint. In reality, the geometry of the weld of a CHS joint is very complex. It depends not only on the dimensions of the CHS joint but also on the welding process. The latter means that for a same CHS joint, the weld shape may not be the same if different operators are carrying out the welding process. This has been proved by Huang (Huang, 2003). Huang (Huang, 2003) has carried out fatigue tests on three CHS T-joints with the same dimensions. It is found that the weld leg length on the chord and the brace are different for the three CHS T-joints. In order to obtain the most accurate numerical results, it is better to use the true weld size in the FE mesh models. Obviously, this is not feasible in an extensive parametric study. Therefore, Wong (Wong, 2001) had spent great effort on the weld shape analysis, and proposed a systematic approach to construct the weld of the CHS T/Y-joint based on the specification of AWS (2000) code. Later, Huang (Huang, 2003) and Shao (Shao, 2004) applied the same approach to construct the weld of the CHS T- and K-joint, respectively.

According to the AWS (2000) specifications, the minimum weld thickness T_{AWS} of the weld should satisfy the condition given below

$$T_{AWS} = K_{AWS} \times t_1 \quad (3.10)$$

where t_1 is the brace thickness, and K_{AWS} is the welding thickness parameter specified in the AWS (2000) specifications, and it is dependent on the dihedral angle γ as shown in Fig. 3.7. In engineering practice, the true weld thickness T_w must satisfy the condition given below

$$T_w \geq T_{AWS} \quad (3.11)$$

Figs. 3.8(a) and (b) show the modelling of the weld joint investigated by Wong (Wong, 2001), Huang (Huang, 2003) and Shao (Shao, 2004). The weld thickness on the chord surface is expressed as

$$\begin{cases} T_w = T_1 + T_2 - T_3 & 30^\circ \leq \gamma < 90^\circ \\ T_w = T_1 + T_2 + T_3 & 90^\circ \leq \gamma < 180^\circ \end{cases} \quad (3.12)$$

where γ is the dihedral angle shown in Figs. 3.8(a) and (b). T_1 is expressed as

$$T_1 = k_1 \times t_1 \quad (3.13)$$

where

$$k_1 = \frac{1}{\sin \gamma} \quad (3.14)$$

T_2 is expressed as

$$T_2 = k_2 \times t_1 \quad (3.15)$$

where

$$k_2 = F_{OS_{outer}} \left(1 - \left(\frac{\gamma_o - \theta_s}{180^\circ - \theta_s} \right)^m \right) \quad (3.16)$$

where $F_{OS_{outer}}$ is a scale factor, m is a constant, and θ_s is the smallest intersecting angle and it is equal to 30° .

T_3 is expressed as

$$T_3 = k_3 \times t_1 \quad (3.17)$$

where

$$k_3 = F_{OS_{inner}} \left(1 - \left(\frac{\gamma_i - \theta_s}{180^\circ - \theta_s} \right)^n \right) \quad (3.18)$$

where $F_{OS_{inner}}$ is a scale factor and n is a constant.

On the brace, the weld thickness is expressed as

$$T_w = T_1 + T_4 \quad (3.19)$$

When $30^\circ \leq \gamma < 90^\circ$, T_4 is expressed as

$$T_4 = \frac{T_3}{\cos \gamma_i} \quad (3.20)$$

When $90^\circ \leq \gamma \leq 180^\circ$, T_4 is expressed as

$$T_4 = \frac{T_3}{\cos(180^\circ - \gamma_i)} \quad (3.21)$$

Wong (Wong, 2001) has spent great effort on investigating the values of all involved parameters including $F_{OS_{outer}}$, $F_{OS_{inner}}$, θ_s , γ_i , γ_o , m and n . The appropriate ranges for these

parameters are $F_{OS_{outer}} \geq 0.3$ (typically, $F_{OS_{outer}} = 0.3$), $0 \leq F_{OS_{inner}} \leq 0.25$ (typically, $F_{OS_{inner}} = 0.25$), $\theta_s \geq 30^\circ$, $\theta_s \leq \gamma_i < 180$, $\theta_s \leq \gamma_o < 180$, $m = 2.0$ and $n = 0.4$.

Figs. 3.9(a) and (b) show the weld size on the chord and the brace reported by Huang (Huang, 2003), together with the weld leg length of the FE mesh model. It can be seen that the weld leg length on the chord of the FE mesh model generated by Huang (Huang, 2003) is much smaller than the true weld size. However, for the weld leg length on the brace, it is larger than the true weld size. Figs. 3.11(a) and (b) show the weld size of two CHS K-joints reported by Shao (Shao, 2004). The same trend is observed for the weld size of two CHS K-joints.

The weld leg length on the chord modelled by Wong (Wong, 2001) generally is much smaller than the true weld size. This will cause a more severe stress concentration effect at the intersection region of the CHS joints. Therefore, a larger stress concentration factor (SCF) is expected when the weld is modelled by using the systematic approach developed by Wong (Wong, 2001), and this has been validated by Shao (Shao, 2004). However, the plastic collapse loads of the uncracked and cracked CHS joints may be significantly underestimated if the weld shape modelled by Wong (Wong, 2001) is used. As the effect of the weld on the plastic collapse load of CHS joints has not been taken into account in all the prevalent standards such as IIW (2009), Eurocode 3 (2005) and API-RP-2A (2005), there are evidence to show that the weld may have as much as 20% effect on the strength of the CHS joint, depending on the geometry of the joint (Lee & Wilmschurst, 1995; Cao, Yang, & Packer 1997; Lee, 1999). In addition, the surface crack block has to be redesigned in the new mesh generator. The weld shape modelled by Wong (Wong, 2001) cannot be easily incorporated in the new mesh generator. Therefore, a new type of weld shape is specified in the new mesh generator.

From the above discussions, it can be seen that in fact T_2 is the most important part for the weld leg on the chord. This is mainly because the surface crack is located at the weld toe of the chord. In addition, T_3 and T_4 become 0 for a uni-planar CHS T- and multi-planar CHS TT-joint. T_3 and T_4 are expected to have negligible effect on the accuracy of the FE results. Therefore, in the new mesh generator, the weld thickness T_w on the chord is defined as

$$T_w = T_1 + T_2^* \quad (3.22)$$

where T_1 is defined in Eq. (3.13), and T_2 is defined as

$$T_2^* = k_2^* \times t_1 \quad (3.23)$$

where

$$k_2^* = \max \left(F_{OS_{outer}} \left(1 - \left(\frac{\gamma_o - \theta_s}{180^\circ - \theta_s} \right)^m \right) \right) \quad (3.24)$$

Therefore, the range of k_2^* is $k_2^* \leq 0.3$. A typical value of 0.3 is recommended for k_2^* in this study.

The weld thickness T_w on the brace is defined as

$$T_w = T_1^* \quad (3.25)$$

where

$$T_1^* = k_1^* \times t_1 \quad (3.26)$$

where

$$k_1^* = \min \left(\frac{1}{\sin \gamma_o} \right) \quad (3.27)$$

Therefore, the range of k_1^* is $k_1^* \geq 1$. A typical value of 1 is recommended for k_1^* in this study.

Figs. 3.11(a) and (b) show the weld size on the chord and the brace adopted in the new mesh generator, together with the values reported by Shao (Shao, 2004). It can be seen that for the weld leg length on the chord, the weld size in the new mesh generator is in between the weld modelled by Shao (Shao, 2004) and the true weld found in the experimental tests. However, this is not always true for the weld leg length on the brace. It can be seen that the weld leg length on the brace is smaller than the weld modelled by

Shao (Shao, 2004) and the true weld measured in the experimental tests at the two crown positions. Nevertheless, the weld leg length on the brace is still acceptable as the surface crack is located at the weld toe of the chord instead of the brace.

3.3 GEOMETRY ANALYSIS OF CRACKED CHS T/Y-JOINTS

In order to generate the FE mesh model of a cracked CHS T/Y-joint, parameters used to define the joint and the surface crack should be defined respectively. Geometry of the uncracked CHS T/Y-joint has been defined in the previous section. In this section, the parameters used to define the surface crack are introduced.

3.3.1 Crack Shape

In practice, the surface crack detected in a cracked CHS joint is always in a 3-Dimensional form. Fig. 3.12 shows a typical surface crack face in a CHS K-joint after the fatigue test (Shao, 2004). By using an alternative current potential drop (ACPD) technique, the propagation process of the surface crack is monitored and recorded from the initiation stage up to the failure. After the specimen is tested to failure, the crack shape is manually measured as well. Fig. 3.13 shows the crack shape defined by the depth verses the length reported by Shao (Shao, 2004). It can be seen that the crack shape resembles a semi-ellipse. In addition, it can also be seen that the deepest point of the surface crack is not exactly located at the crown position. This explains physically the motivation of developing a new mesh generator to allow for a flexible position of the surface crack present in any CHS joint.

The surface crack present in a CHS joint can be constructed by double mapping a 2D semi-elliptical surface crack as shown in Fig. 3.14. There are two parameters used to characterize a 2D semi-elliptical surface crack, and they are the crack depth a and the crack length $2c$. It is more convenient to use dimensionless crack depth and length, i.e. a/t_0 and c/a . The crack depth of a surface crack varies from 0 to t_0 . Therefore, the value of a/t_0 can easily be employed when carrying out FE analysis. Nevertheless, this is not the case for the crack length $2c$. According to previous works (Rhee, 1989; Ma & Kam; 1991; Kam et al., 1995; Huang, 2003; Shao, 2004), the length $2c$ of the surface crack depends not only on the geometry but also on loading types and material properties. The length $2c$ along the intersection at the weld toe varies widely, and it can easily be measured in practice.

3.3.2 Crack Face

When a surface crack initiates and propagates through the chord thickness of a CHS joint, it tends to deviate slightly from the chord thickness direction resulting in a double curvature crack face (Bowness & Lee, 1995; Huang, 2003; Shao, 2004). Bowness and Lee (Bowness & Lee, 1995) constructed a double curvature crack surface by a 4th-order polynomial and studied carefully the effect of the crack curvature along the chord thickness on the SIFs of cracked CHS T-joints. It is concluded that the surface crack at CHS joints is governed by the Mode-I crack opening mode, and the crack curvature along the chord thickness direction has negligible influence on the SIFs because the Mode-II and Mode-III values are very small in comparison to Mode-I values. The Mode-II and Mode-III SIFs appear to only have a slight effect on the propagation direction of the surface crack. The same conclusion was obtained by Yang (Yang, 1996). Therefore, in the present study, the crack face is assumed to be perpendicular to the chord surface. This assumption has been successfully used to study the fatigue behavior of cracked CHS T- and K-joints by Huang (Huang, 2003) and Shao (Shao, 2004), respectively.

Fig. 3.15(a) shows the geometry of a 2D semi-elliptical surface crack present in a cracked plain plate. W_o is a point at the crack mouth of the surface crack. By drawing a dash line passing through W_o point and parallel to the thickness direction of the cracked plain plate, a point D which is the intersecting point of the drawn line and the bottom face of the cracked plain plate, can be determined. Length of line W_o -D is equal to the thickness of the cracked plain plate, t_0 . The intersecting point of line W_o -D and the crack front is indicated as point C_r . After the double mapping process, the 2D crack face shown in Fig. 3.15(a) is converted into a 3D shape shown in Fig. 3.15(b). In order to illustrate the double mapping relationship, the 3D crack face is drawn on the background of the true crack face as shown in Fig. 3.15. In order to use the double mapping approach, the following six assumptions should be satisfied:

1. Point W_o is on the upper face of the cracked plain plate. After the double mapping process, it is at the weld toe on the chord;
2. Point D is on the bottom face of the cracked plain plate. After the double mapping process, it is on the inner surface of the chord;
3. Point C_r is located at the crack front, and it is on the line W_o -D;

4. Length of line W_o -D is equal to the thickness of the cracked plain plate and the chord of the cracked CHS joint;
5. Length of W_o - C_r is the depth of the surface crack at point W_o ; and
6. Line W_o - C_r -D passes through Z-axis in such a way that $X=Y=0$ after the double mapping process.

Fig. 3.15(c) shows the XY-cross section containing line W_o - C_r -D. The coordinates of point W_o in X-Y-Z coordinate system is expressed as

$$W_o = \begin{Bmatrix} X_{W_o} \\ Y_{W_o} \\ Z_{W_o} \end{Bmatrix} \quad (3.28)$$

Through simple geometry analysis shown in Fig. 3.15(c), one can determine the coordinates of point D and C_r as expressed below

$$D = \begin{Bmatrix} X_D \\ Y_D \\ Z_D \end{Bmatrix} = \begin{Bmatrix} \frac{R_2}{R_1} X_{W_o} \\ \frac{R_2}{R_1} Y_{W_o} \\ Z_{W_o} \end{Bmatrix} \quad (3.29)$$

$$C_r = \begin{Bmatrix} X_{C_r} \\ Y_{C_r} \\ Z_{C_r} \end{Bmatrix} = \begin{Bmatrix} \left(1 - \frac{d}{R_1}\right) X_{W_o} \\ \left(1 - \frac{d}{R_1}\right) Y_{W_o} \\ Z_{W_o} \end{Bmatrix} \quad (3.30)$$

By jointing a series of points of W_o , C_r , and D, the 3D surface crack face present in CHS joints can be determined.

3.4 MESH GENERATION OF CRACKED CHS T/Y-JOINTS

In the new mesh generator, mesh of the chord-brace intersection zone of a cracked CHS T/Y-joint is constructed by double mapping a cracked T-butt joint, which is created by

adding a vertical attachment and weld on a cracked plain plate. In this section, FE mesh models of cracked plain plate, T-butt joint and CHS T/Y-joint are generated in sequence. The double mapping process used to generate the FE mesh model of the cracked CHS T/Y-joint is elaborated. In addition, several key points in the mesh generation are introduced, including element types, mesh design of the surface crack front and mesh refinement schemes.

3.4.1 Element Selection

Two types of element shown in Fig. 3.16(a) and (b) are used at the crack tips of the surface crack. The 15-node collapsed element is adopted for calculating the SIF and elastic J -integral, where its middle nodes at element edges intersecting with the crack front are moved to the quarter position to produce the well-known $1/r^{1/2}$ strain singularity. It is adopted at the crack tips of the FE mesh models used for calculating the SIF and elastic J -integral in the present study. As for elastic-plastic analyses, a plastic zone will form ahead of the crack tip, thus, the $1/r^{1/2}$ strain singularity is no longer valid at the crack tip. Instead, when the plastic zone is formed, all elements enclosing the crack tips are deemed to produce a $1/r$ strain singularity, which corresponds to the actual crack tip strain field for a fully plastic material. The second type element shown in Fig. 3.16(b) is adopted at the crack tips of the FE mesh models used for calculating the plastic collapse load and elastic-plastic J -integral. It can be seen that a finite radius key-hole is introduced for the second type element. The finite radius key-hole is critical in avoiding the elements enclosing the crack tips from collapsing. The second type element is strongly suggested in API 579-1 (2007) when large strain is involved around the crack front. The radius of the key-hole should be at least five times smaller than the deformed crack tip radius, where the deformed radius is approximately half of crack tip opening displacement (CTOD). For other regions of the cracked CHS joint, a 20-node cubic element is used in the mesh model.

3.4.2 Mesh Generation of a Cracked Plate

Fig. 3.17 shows the geometry of a cracked plain plate containing a central semi-elliptical surface crack. In order to create its FE mesh model, a sub-zone technique is used in the mesh generation process. Among all the sub-zones, the crack zone (surface crack block) shown in Fig. 3.18 is the most crucial one. It can be seen that the surface crack block is

further sub-divided into three parts, i.e. Part A, Part B and Crack Tube in which the crack front is located.

In practice, the crack depth and length of the surface crack of any cracked plain plate, T-butt joint and CHS joint vary extensively. Therefore, it is decided to set the number of divisions along the crack front as a variable so that mesh refinement of the crack block can be carried out conveniently. If the length of the surface crack is relatively small, the crack front can be divided into fewer segments only, while if the length of the surface crack is longer, more segments are used along the crack front. In addition, the number of element rings enclosing the crack tip is also set as a variable so that it is easy to refine the mesh along the crack front and check the path independence of the J -integral (Yang, 1996). Fig. 3.19 shows the mesh of the surface crack block of a central cracked plain plate. By cutting through the surface crack block along the free crack face, the mesh design of the crack front is revealed as shown in Fig. 3.20. It can be seen clearly that in order to construct the semi-elliptical crack front and refine the mesh, the length of some segments near the crack ends are reduced.

Figs. 3.21(a) and (b) show the mesh design by cutting across the Crack Tube at the crack tip. It can be seen that 16 elements surrounding the crack tips are used. In Figs. 3.21(a) and (b), the dimensions of the cutting cross-section are indicated as well. In generating the mesh of the Crack Tube, a sweep process is employed herein. Firstly, the Crack Tube is cut into sufficient segments along the crack front and the cutting cross-sections are set perpendicular to the tangent of the crack front at the cutting points. Then, nodes are deployed on the cutting cross-sections. Simultaneously, the elements of the Crack Tube are created. By sweeping the cutting cross-section shown in Figs. 3.21(a) and (b) along the surface crack front, 3D-coordinates of nodes on the Crack Tube are determined. Finally, dimensions of certain cutting cross-sections are reduced to form the transition elements. Fig. 3.22 shows the mesh after the sweep process. After connecting the convex nodes properly to form some transition elements shown in Fig. 3.23, the mesh of the Crack Tube is completed, and it is shown in Fig. 3.24. Mesh generation of Part A and Part B are quite straightforward and they are shown in Figs. 3.25 and 3.26 respectively.

Transition zone is necessary in mesh design of any cracked component. This is mainly based on two considerations. Firstly, transition zone can reduce the total number of elements required to construct the FE mesh models; secondly, transition zone is designed

for connecting elements of the surface crack block to the adjacent regions. There are three transition sub-zones in the mesh model of the central cracked plane plate containing a surface crack, and they are Mesh4, Mesh5 and Mesh6 as shown in Figs. 3.27, 3.28 and 3.29, respectively. After merging the crack block and all the transition elements together, the FE mesh model of the central cracked plain plate is completed (Fig. 3.30). It should be noted that the only difference between FE models of the central cracked plain plate in elastic and elastic-plastic analysis is within the elements at the crack tip.

3.4.3 Mesh Generation of Cracked T-butt Joint

The FE mesh model of a cracked T-butt joint can be generated by simply adding a vertical attachment and the fillet welds to a central cracked plain plate. Therefore, the mesh generation of the cracked T-butt joint is straightforward as there is no complex mathematical derivation involved in it. Figs. 3.31 and 3.32 show the geometry and the mesh of a T-butt joint containing a surface crack respectively. From Fig. 3.32, it can be seen that apart from the mesh of the vertical attachment and the welds, there are also other transition zones which may or may not be used in the mesh generation of the cracked CHS joint. Overall, adding the mesh of the vertical attachment and the weld to an existing cracked plain plate is not a difficult task, and thus, it will not be explained further herein.

3.4.4 Mesh Generation of Cracked CHS T/Y-joint

As discussed earlier, the deepest point of the surface crack in a cracked CHS joint is not always located exactly at the crown or the saddle. Therefore, it is necessary to develop a procedure to determine the positions of the two crack ends and the deepest point accordingly. In practice, the two crack ends are determined by measuring the distance from the crack ends to the datum point (zero point) along weld toe. The length of the crack block is defined as l_{cr} as illustrated in Fig. 3.33. It is important to note that the length of the surface crack which is the distance between two crack ends along the weld toe is equal to $l_{cr} - H_{cra}$, where H_{cra} is the height of the cutting cross-section of the Crack Tube shown in Figs. 3.21(a) and (b). In the new mesh generator, an approximation procedure is employed to determine the positions of two crack ends. This procedure is first developed by Lie et al. (Lie, Lee & Wong, 2003) for mesh generation of cracked CHS T/Y-joints. Later, Shao (Shao, 2004) adopted the same procedure in the mesh generation for cracked CHS K-joints. Firstly, a circle corresponding to the outer weld intersection is defined in a local

polar u - v coordinates as illustrated in Fig. 3.34. In the local polar coordinates, the circle is divided into 36,000 segments by a polar angle which is measured from the datum point, and then the circle is mapped to the weld toe on the chord by using Eq. (3.8). Thereafter, an iteration method is used to define the two crack ends by gradually increasing the polar angle. When the distance from the datum point to a certain point on the 3D weld toe of the chord approximates to l_{cr1} , the point can be taken as the first crack end. The second crack end can be defined using the same strategy.

Fig. 3.34 shows the local polar u - v coordinates with four polar angles, α_{cr11} , α_{cr1} , α_{cr2} and α_{cr22} , and they refer to the start of the surface crack block, the crack end 1, the crack end 2 and the end of the surface crack block in the cracked T-butt joint which are illustrated in Fig. 3.35. It can be seen that the outer weld intersection curve is divided into three parts by the polar angles α_{cr11} and α_{cr22} , and the datum point. The mesh of the chord-brace intersection zone is constructed through the T-butt mesh model. Therefore, the length of the T-butt mesh model is equal to the circumference of the circle in the local polar coordinate. Fig. 3.36 illustrates the procedure of generating the chord-brace intersection zone of a cracked CHS T/Y-joint. After the mesh of all the sub-zones are generated, they are merged together to form the cracked T/Y-joint. Mesh of each subzone is shown in Figs. 3.37, 3.38 and 3.39. The completed FE mesh models of a cracked CHS T-joint and a cracked CHS Y-joint are shown in Figs. 3.40 and 3.41, respectively.

As mentioned early, FE mesh model of a cracked multi-planar CHS TT-joint can be created by adding one out-of-plane brace on a cracked uni-planar CHS T-joint. Fig. 3.42 shows the completed mesh model of a typical cracked multi-planar CHS TT-joint.

Figs. 3.43 and 3.44 show the FE mesh models of an uncracked CHS T/Y-joint and an uncracked multi-planar CHS TT-joint, respectively.

3.4.5 Mesh Refinement Strategy

For the FE mesh models of cracked CHS joints, two types of mesh refinement schemes are provided in the new mesh generator. The first type is used to refine the local mesh of the Crack Tube (Fig. 3.22). Element rings enclosing the crack front and the number of divisions (number of cutting cross-sections) along the crack front are set as two variables so that it is easy to refine the mesh of the Crack Tube. The second type is used to refine the

global mesh of the entire chord member. Number of divisions along the chord-brace intersecting curve is set as a variable as shown in Figs. 3.45(a) and (b). It has been validated that a minimum of 40 divisions along the chord-brace intersecting curve is sufficient to capture the stress distribution at the chord-brace intersection zone of a typical CHS joint (Shao, 2004). In addition, it is found that the crack depth and length of a surface crack in any cracked CHS joint can vary widely in practice. If the crack depth is relatively shallow such as $a/t_0=0.2$, the dimensions of the Crack Tube, W_{cra} and H_{cra} indicated in Figs. 3.21(a) and (b) must be sufficient small so as to avoid affecting the mesh generation of Part B of the surface crack block. For a shallow surface crack, the mesh transition between the Crack Tube and Part A is not smooth as element size of Part A is significant larger than elements of the Crack Tube. This may affect the accuracy of the SIF results. In order to smooth out the mesh transition between the Crack Tube and Part A, a novel mesh refinement scheme is designed to increase the element layers at Part A. Figs. 3.46(a) and (b) shows the details of such mesh refinement scheme with 3 element layers at Part A. Simultaneously, the number of element layers of the entire chord along its thickness direction is increased to 3 so that the global mesh of the chord is refined as well. The number of element layers at Part A is also set as a variable in the new mesh generator.

3.5 MESH CONVERGENCE TEST

In this section, extensive mesh convergence tests are carried out on different FE mesh models for calculating the SIFs, elastic J -integral, elastic-plastic J -integral and plastic collapse load of a cracked CHS T/Y-joint. The mesh convergence test is carried out mainly on two aspects, including the effect of element layers at Part A (Fig. 3.18) of the surface crack block and element rings enclosing the crack front. For the required elements along the chord-brace intersecting curve, it has been validated that 40 elements are sufficient to capture the stress distribution at the intersection zone (Shao, 2004). Therefore, mesh convergence test on the number of elements along the chord-brace intersecting curve is not covered in this study. Experimental results reported by other researchers (Zerbst, Heerens & Schwalbe, 2002; Lie, Li & Shao, 2014) are also compared with the FE results so as to make the latter more convincing.

As mentioned previously, the FE mesh model of a cracked CHS T/Y-joint is created by carrying out double mapping the T-butt joint generated by adding a vertical attachment and the fillet welds to a cracked plain plate. Therefore, the FE mesh model of the cracked plain

plate is a by-product of the FE mesh model of a cracked CHS T/Y-joint. Although the cracked plain plate is not the main focus of the research in this study, a comprehensive mesh convergence test is beneficial to calibrate the numerical results. Figs. 3.47 and 3.48 show the typical FE mesh model of cracked plain plate generated by using FEACrackTM (2003) software and the new mesh generator, respectively.

3.5.1 Stress Intensity Factor (SIF) and Elastic J -integral

In this sub-section, mesh convergence tests are carried out on FE mesh models used for obtaining the SIFs and elastic J -integral. For a typical cracked CHS joint, the surface crack is deemed to be in a mixed mode, including K_I , K_{II} and K_{III} . In ABAQUS software (2011), K_I , K_{II} and K_{III} are extracted from elastic J -integral using an interaction integral method. It means that the values of K_I , K_{II} , K_{III} and elastic J -integral converge simultaneously. Considering K_I is the dominant crack mode, only convergence test on K_I is presented in this study.

Figs. 3.49(a) and (b) show the effect of elements layer at Part A (Fig. 3.19) on the $Y(g)$ (dimensionless K_I) of a cracked plain plate with different crack depth. The cracked plain plate is subjected to a uniform distribution loading at one end parallel to the crack face. The other end of the cracked plain plate is fixed in all directions. In order to obtain an accurate K_I values, it can be seen that at least 2 element layers should be designed at Part A. In Figs. 3.49 (a) and (b), K_I is converted to the shape factor $Y(g)$ using the equation given below

$$K_I = Y(g)\sigma_n\sqrt{\pi a} \quad (3.31)$$

where $Y(g)$ is the shape factor depending on the geometry of the cracked component and the crack shape, σ_n is the nominal stress and a is the crack depth. For a cracked plain plate, the nominal stress σ_n is defined as the applied loading divided by the area of the cross-section perpendicular to the loading direction. For CHS joints subjected to different types of loading, the nominal stress σ_n is defined in Chapter 2. The magnitude of σ_n is set as 1MPa in the FE analyses.

Figs. 3.50(a) and (b) show the effect of element layers at Part A on the distribution of $Y(g)$ along the entire crack front of a cracked CHS T/Y-joint. It is found that for a cracked CHS

T/Y-joint with $a/t_0 \leq 0.5$, at least 2 element layers must be designed at Part A. For a cracked CHS T/Y-joint with $0.5 < a/t_0 \leq 0.8$, 1 element layer designed at Part A appears to be sufficient to produce accurate SIFs. In this study, 3 element layers at Part A are adopted as an optimization choice for a cracked CHS T/Y-joint with different crack depth. The 3rd layer is designed to remove uncertainties such as geometry size effect in the extensive parametric study carried out in the next stages.

Figs. 3.51(a) and (b) show the effect of element rings enclosing the crack front on the distribution of $Y(g)$ along the entire crack front of a cracked plain plate and a CHS T/Y-joint, respectively. It can be seen that 3 element rings are sufficient to produce accurate shape factors at the crack tips far away from the crack ends. Due to the boundary layer effect and the weld profile, the square root singularity at a small region near the crack end is not satisfied (Newman & Raju, 1981; Shivakumar & Raju, 1990). Therefore, the SIFs at two crack ends obtained in this study are not accurate because square root singularity is not satisfied completely. The singularity at the crack ends of a surface crack is still being exploited by researchers (Shin & Cai, 2004; Heyder, Kolk & Kuhn, 2005; de Matos & Nowell, 2008; Madia et al., 2011; Lebahn, Heyer & Sander, 2013). Therefore, this study can only focus on the SIFs at the crack tips far away from the crack ends. In addition, from the recorded crack shape development reported by Huang (Huang, 2003) and Shao (Shao, 2004), it is found that the surface crack in CHS joints will quickly propagate to a certain length along the weld toe at the initiation stage. After that, the surface crack will mainly propagate through the chord thickness direction until it becomes a through-thickness crack. Therefore, the SIF at the deepest point of a semi-elliptical surface crack is the main focus in this study.

As reported in ABAQUS Benchmark Manual (2011) and Benchmark and Validation of FEACrackTM (2003), SIFs produced from the first element ring is not accurate and should be discarded. In addition, SIFs is derived from J -integral in ABAQUS (2011) software. In order to check the path independence of the J -integral and save the computation cost, 4 element rings enclosing the crack front are adopted when calculating the SIF and elastic J -integral in this study. The angle ϕ is illustrated in Fig. 3.52.

The SIFs data reported by Ritchie and Hujskens (Ritchie & Hujskens, 1988) is used by many researchers to validate their own FE mesh models of cracked CHS T/Y-joints. Ritchie and Hujskens (Ritchie & Hujskens, 1988) carried out fatigue test on a CHS T-joint

subjected to axial cyclic loading and estimated the SIFs at the crack deepest point after a crack initiating and propagating at the saddle location. Table 3.1 lists the dimensions of the cracked CHS T-joints and the crack shapes used for calibration purposes. Fig. 3.53 shows the comparison of the shape factors $Y(g)$ obtained in this study and results reported by other researchers (Bowness & Lee, 1998 ; Lie, Li & Cen, 2000; Shen & Choo, 2012). It can be seen that the differences among different curves remain rather large. The differences may be due to two facts. Firstly, the SIFs estimated from experimental test shown in Fig. 3.53 are the average value of 5 curves reported by Ritchie and Hujskens (Ritchie & Hujskens, 1988). In fact, they only estimated the range of the SIFs at the deepest points of 5 cracked CHS T-joints rather than providing the accurate SIF values. Therefore, the difference between the SIFs obtained in this study and the average SIFs shown in Fig. 3.53 appears to be acceptable as the average SIFs curve may not represent accurate results. Secondly, the FE mesh models generated by Bowness and Lee (Bowness & Lee, 1998; Lee & Bowness, 2002) are very coarse and may not be fine enough to produce accurate SIFs. In addition, they reported two different $Y(g)$ curves with large differences within four years. Therefore, it is reasonable to doubt the accuracy of $Y(g)$ curves reported by Bowness and Lee (Bowness & Lee, 1998; Lee & Bowness, 2002). It can be seen that the shape factors obtained by Shen and Choo (Shen & Choo, 2012) agree quite well with the results obtained in this study. This is because FE mesh models generated by Shen and Choo (Shen & Choo, 2012) were created by modifying the cracked nozzle in FEACrackTM (2003), and the mesh quality of their FE mesh models is very high.

3.5.2 Elastic-plastic J -integral

In this sub-section, mesh convergence test is carried out on FE mesh models used for calculating the elastic-plastic J -integral. In order to calculate the elastic-plastic J -integral, the true stress-strain data should be used for the steel material property. The stress-strain data reported by van der Vegate (van der Vegate, 1995) is used in this study. Fig. 3.54 shows the true stress-strain data used in FE analysis. The yield and the ultimate strength are 355 MPa and 630 MPa respectively.

According to reports published by Yang (Yang, 1996) and Yang (Yang, 2006), the elastic-plastic J -integral tends to show low path independence when the applied load is very close to the plastic collapse load. The elastic-plastic J -integral calculated from different element rings has a difference less than 10%. However, both Yang (Yang, 1996) and Yang (Yang,

2006) did not provide any explanation on this difference. As mentioned earlier, elastic J -integral of the first contour should be discarded as it is not accurate. Elastic-plastic J -integral of the first contour should also be discarded. In addition, it is also observed that elements from other rings such as the second and third may collapsed severely due to the large loading being applied. Therefore, J -integral values calculated from these element rings are not accurate and should be discarded as well. Fig. 3.55 shows the deformation of the crack tip of a cracked CHS T/Y-joint with 12 element rings enclosing the crack front. The magnitude of the applied loading is approximately equal to the plastic collapse load of the cracked CHS T/Y-joint. It can be seen that elements from the first 3 rings are distorted severely. Therefore, in this study, J -integral values from each element rings are taken as valid only when J -values calculated from element rings distorted severely are discarded. In this study, the elastic-plastic J -integral is taken as the maximum valid J -integral values, and it is generally from the most outer contour.

As large loading has a significant effect on the J -integral values, it has to be taken into account in the mesh convergence test. In this sub-section, large loadings are applied to the cracked plain plate and the cracked CHS T/Y-joint to study the convergence efficiency of the FE mesh models. Similar to the mesh convergence test on the FE mesh models used to calculate the SIF and elastic J -integral, mesh convergence test on FE mesh models used to calculate the elastic-plastic J -integral are carried out on two aspects, e.g. element layers at Part A and element rings enclosing the crack front.

Figs. 3.56(a) and (b) show the distributions of the elastic-plastic J -integral of a cracked plain plate subjected to different uniform distribution loading (σ_n). FE results obtained using the new mesh generator is compared with J_{ep} obtained from FEACrackTM (2003) software. It can be seen that 3 element layers at Part A are sufficient to produce accurate J_{ep} for the cracked plain plate. Figs. 3.57(a) and (b) show the distribution of the J_{ep} of a cracked CHS T/Y-joint subjected to different axial tensile loading. It can be seen that 3 element layers at Part A are sufficient to produce accurate J_{ep} for the cracked CHS T/Y-joint as well.

It should be recognized that yielding already occurs at Part A of the cracked CHS T/Y-joint when the applied loading σ_n at the brace end reaches $0.4f_y$ (Fig. 3.55). The corresponding applied loading is 464kN when σ_n at the brace end reaches $0.4f_y$, and it is calculated using the equation below

$$\sigma_n = \frac{4P}{\left(\pi \left[d_1^2 - (d_1 - 2t_1)^2 \right] \right)} \quad (3.32)$$

In addition, the plastic collapse load of the corresponding uncracked CHS T/Y-joint is calculated by using Eurocode 3 (2005), and it is about 463kN. This implies that loading applied on the cracked CHS T/Y-joint is quiet close to its actual plastic collapse load.

Figs. 3.58(a) and (b), and Figs 3.59(a) and (b) show the effect of element rings enclosing the crack front on the distribution of J_{ep} along the entire crack front of a cracked plain plate and a cracked CHS T/Y-joint, respectively. It can be seen that 4 element rings are sufficient to produce accurate J_{ep} at the crack tips far away from the crack ends. However, as mentioned earlier, elements of the first several rings may be distorted severely when the applied loading is very large, hence only 8 element rings enclosing the crack front of the cracked CHS T/Y-joint is recommended.

3.5.3 Load Displacement Curves

In this sub-section, mesh convergence test is carried out on FE mesh models for calculating the plastic collapse load of cracked CHS T/Y-joints. As mentioned in Section 2.4.3 of Chapter 2, the plastic collapse load of the uncracked and cracked CHS joints are determined from the load displacement curves by using twice elastic compliance and Lu's deformation limit in this study. Therefore, an accurate prediction of the plastic collapse load of a cracked CHS joint can be obtained if the load displacement curve obtained from the FE analysis is reliable, and the plastic collapse load of the corresponding uncracked joints is determined properly. The determination of the plastic collapse load of the uncracked joints will be introduced in subsequent relevant chapters. In this section, FE results of load displacement curves of 6 cracked CHS T-joints are compared with the experimental data reported by other researchers (Zerbst, Heerens & Schwalbe, 2002; Lie, Li & Shao, 2014) and used to verify the accuracy of the FE models generated by the new mesh generator. The information of the specimen tested by Zerbst et al. (Zerbst, Heerens & Schwalbe, 2002), including its dimensions, the crack shape and the material property can be found in their publication. The information of 5 specimens tested by Shao (Lie, Li & Shao, 2014) is summarized in Tables 3.2 to 3.4. All the 6 specimens are subjected to axial tensile loading.

It has been reported that a relatively coarse mesh can be used for the surface crack block when calculating the plastic collapse loads of cracked CHS and RHS joints (Yang, 1996; Yang, 2006; Lie, Yang & Gho, 2009). This is because the mesh around the crack front has a slight influence on the load displacement curves of cracked CHS and RHS joints. FE results presented in this sub-section confirm this founding. Figs. 3.60 to 3.62 show the comparison of load displacement curves of 6 cracked CHS T-joints. It can be seen that FE results obtained in this study agree quiet well with the experimental data before the surface crack propagates. One element layer at Part A and 2 element rings enclosing the surface crack front are recommend for calculating the plastic collapse load of the cracked CHS T/Y-joint.

3.6 RESULTS AND DISCUSSIONS

This chapter merely focuses on the mesh generation and mesh convergence test of the cracked CHS T/Y-joint. This is because a cracked CHS T/Y-joint is the most basic configuration in any CHS joint family. Other different types of CHS joint including multi-planar TT-joint can be created by adding several braces on the chord of the CHS T/Y-joint. Therefore, it is expected that all the requirements such as element layers at Part A and element rings enclosing the crack front, the FE mesh model of the cracked CHS T/Y-joint are also suitable for the cracked multi-planar CHS TT-joint.

By carrying out extensive mesh convergence tests, the minimum mesh requirements for different usage of FE mesh models can be obtained. For calculating the SIFs and elastic J -integral of a cracked CHS joint, 3 element layers at Part A and 4 element rings enclosing the surface crack front are recommended. As for calculating the elastic-plastic J -integral of cracked CHS joints, 3 element layers at Part A and 8 element rings enclosing the surface crack front are recommended. While for calculating the plastic collapse load/moment of cracked CHS joints, 1 element layer and 2 element rings enclosing the surface crack front are recommended. Number of elements along the chord-brace intersecting curve can be determined automatically by the new mesh generator, depending on the crack length. The minimum element number along the chord-brace intersecting curve is set as 40.

It is important to recognize that the minimum mesh requirement recommended herein is also adequate for cracked CHS T/Y-joints subjected to in-plane and out-of-plane bending. Experimental evidence shows that CHS joints are mainly subjected to axial loading.

Besides, experimental data for cracked CHS joints subjected to in-plane and out-of-plane bending are very rare. Therefore, considering the length limitation, only FE results of the cracked plain plate and cracked CHS T/Y-joint subjected to axial loading are presented in this chapter.

It is crucial to pay attention on the determination of the elastic-plastic J -integral. It is necessary to check the deformation of elements enclosing the crack front individually. Otherwise, invalid elastic-plastic J -integral may be obtained. This is because elements of the first several element rings may be distorted severely under large applied loading. If elements of all the element rings are distorted severely, it is necessary to increase the integral domain size (dimensions of the Crack Tube) manually and repeat the calculations.

From the above extensive mesh convergence tests being carried out, it can be seen that the new mesh generator is robust and reliable. By generating the FE mesh model of a cracked CHS T/Y-joint, a cracked plain plate and a cracked T-butt joint can be created as the two by-products. In addition, it can be seen that the efficiency of the new mesh generator is very high. For instance, a cracked CHS T/Y-joint with 20,000 elements can be created within 3mins. Therefore, the new mesh generator can be used confidently to carry out FAD analysis on the cracked uni-planar T/Y-joint and multi-planar TT-joint described in the next chapters.

Table 3.1 Dimensions and crack sizes of cracked CHS T-joint (Ritchie et al., 1988)

Dimensions	d_0 (mm)	d_1 (mm)	t_0 (mm)	t_1 (mm)	l_0 (mm)	l_1 (mm)
	914	457	32	16	3900	1250
Surface crack	1 st	2 nd	3 rd	4 th	5 th	—
a (mm)	6.5	11.2	14.3	18.0	24.2	—
$2c$ (mm)	30.2	60.9	91.0	123.3	202.2	—

Table 3.2 Geometry of analyzed cracked CHS T- and Y-joints (Lie et al., 2014)

No.	d_0 (mm)	d_1 (mm)	l_0 (mm)	l_1 (mm)	t_0 (mm)	t_1 (mm)	θ
T1	180	133	2000	400	8	6	90°
T2	159	45	2000	300	8	6	90°
T3	159	45	2000	300	8	6	90°
T4	219	108	2000	400	6	6	90°
T5	219	108	2000	400	6	6	90°

Table 3.3 Crack shape and location of cracked CHS T- and Y-joints (Lie et al., 2014)

No.	$2c$ (mm)	a (mm)	Deepest point deviation (mm)	Crack location
T1	55	4.3	7.5	crown
T2	65	2.3	5	crown
T3	60	5.1	3.5	crown
T4	100	4.0	10	saddle
T5	80	3.7	15	saddle

Table 3.4 Material properties of cracked CHS T-joints (Lie et al., 2014)

No.	Yield strength (MPa)	Ultimate strength (MPa)	Elastic modulus (GPa)	Elongation (%)
T1	389.5	591.9	171.8	24.5
T2	349.1	626.2	198.0	28.0
T3	321.0	669.8	199.5	30.5
T4	322.0	594.3	189.8	25.0
T5	290.2	585.5	161.0	25.5

(a) External view

(b) Internal view

Fig. 3.1 Mesh of the surface crack block created by previous mesh generator (Huang, 2003)

(a) Uni-planar CHS T/Y-joint

(b) Multi-planar CHS TT-joint

Fig. 3.2 Geometries and notations of uni-planar CHS T/Y-joint and multi-planar CHS TT-joint

Fig. 3.3 Mapping of a plane to a circular surface

Fig. 3.4 Co-ordinate systems for a general CHS T/Y-joint

Fig. 3.5 Double mapping of a circle to a 3D intersecting curve in plane view

Fig. 3.6 Typical weld shape in CHS joints

Fig. 3.7 Inner and outer dihedral angles

Fig. 3.8 Modelling of the weld in FE mesh models generated by Huang (Huang, 2003) and Shao (Shao, 2004)

(a) Chord

(b) Brace

Fig. 3.9 Weld size of three CHS T-joint specimens with same dimensions (Huang, 2003)

(a) Chord

(b) Brace

Fig. 3.10 Weld size of two CHS K-joint specimens (Shao, 2004)

(a) Chord

(b) Brace

Fig. 3.11 Weld size used in this study

Fig. 3.12 Crack surface of a CHS K-joint (Shao, 2004)

Fig. 3.13 Crack shape of the surface crack of CHS K-joint (Shao, 2004)

Fig. 3.14 Mapping of a 2D crack face to 3D curved plane

(a) Typical 2D semi-elliptical surface crack

(b) Typical 3D surface crack in CHS joint

(c) XY -cross section containing dash line W_o - D

Fig. 3.15 Definition of the 3D surface crack face in cracked CHS joints

Fig. 3.16 Element types used at the crack tip

Fig. 3.17 A cracked plain plate containing a central semi-elliptical surface crack

Fig. 3.18 Three sub-zones of the surface crack block

Fig. 3.19 Mesh of the surface crack block in a central cracked plain plate

Fig. 3.20 Mesh design of the crack front

(a) Elastic analysis

(b) Elastic-plastic analysis

Fig. 3.21 Mesh design of the Cutting Cross-section of the Crack Tube at the crack tip

Fig. 3.22 Mesh of a Crack Tube after a sweep process

Fig. 3.23 Transition elements of a Crack Tube along its thickness direction

Fig. 3.24 Mesh of a completed Crack Tube

Fig. 3.25 Mesh of Part A of the surface crack block.

Fig. 3.26 Mesh of Part B of the surface crack block

Fig. 3.27 Transition zone of Mesh4

Fig. 3.28 Transition zone of Mesh5

Fig. 3.29 Transition zone of Mesh6

Fig. 3.30 Mesh of a cracked plain plate containing a surface crack

Fig. 3.31 Geometry of a T-butt joint containing a surface crack

Fig. 3.32 Different mesh zones of a T-butt joint containing a surface crack

Fig. 3.33 Location of a surface crack defined on global coordinate system shown on the Y-Z plane

Fig. 3.34 Location of a surface crack defined on a local polar U-V coordinate system

Fig. 3.35 Mesh of the surface crack block in a cracked plain plate

Fig. 3.36 Double mapping a T-butt to form chord-brace intersection zone of a cracked T/Y-joint

Fig. 3.37 Sub-zone Mesh-AB

Fig. 3.38 Sub-zone Mesh-E

Fig. 3.39 Sub-zone mesh of ExtenCHL or ExtenCHR

Fig. 3.40 The completed mesh of a cracked CHS T-joint

Fig. 3.41 The completed mesh of a cracked CHS Y-joint ($\theta=60^\circ$)

Fig. 3.42 The completed mesh of a cracked multi-planar CHS TT-joint ($\phi=90^\circ$)

Fig. 3.43 The completed mesh of an uncracked CHS T/Y-joint ($\theta=60^\circ$)

Fig. 3.44 The completed mesh of an uncracked multi-planar CHS TT-joint ($\varphi=90^\circ$)

(a) 40 number of divisions (coarse) (b) 96 number of divisions (dense)

Fig. 3.45 Mesh refinement program along the chord-brace intersecting curve

(a) 1 element layer at Part A

(b) 3 element layers at Part A

Fig. 3.46 Mesh refinement through the chord thickness direction

Fig. 3.47 FE mesh model of a cracked plain plate generated by FEACrackTM (2003)

Fig. 3.48 FE mesh model of a cracked plain plate generated by new mesh generator

(a) $a/t_0=0.2$

(b) $a/t_0=0.5$

Fig. 3.49 Effect of element layers at Part A of cracked plain plates
($c/a=5$, $W=320\text{mm}$, $L=320\text{mm}$, $t_0=16\text{mm}$, axial loading)

(a) $a/t_0=0.2$

(b) $a/t_0=0.5$

Fig. 3.50 Effect of element layers at Part A of cracked CHS T/Y-joints
 $(\alpha=14, \beta=0.5, \gamma=18, \tau=0.6, \theta=60^\circ, c/a=8, t_0=10\text{mm}, \text{axial loading})$

(a) Central cracked plain plate

($a/t_0=0.5$, $c/a=5$, $W=320\text{mm}$, $L=320\text{mm}$, $t_0=16\text{mm}$, axial loading)

(b) Cracked CHS T/Y-joint

($\alpha=14$, $\beta=0.5$, $\gamma=18$, $\tau=0.6$, $\theta=60^\circ$, $a/t_0=0.5$, $c/a=8$, $t_0=10\text{mm}$, axial loading)

Fig. 3.51 Effect of element rings enclosing the crack front on $Y(g)$

Fig. 3.52 Definition of crack front angle ϕ

Fig. 3.53 Comparison of the shape factors at the deepest point of 5 cracked CHS T-joints

Fig. 3.54 True stress-strain curve used in calculating elastic-plastic J -integral

Fig. 3.55 Close view of the deformation of the crack tip of a cracked CHS T/Y-joint

subjected to axial tensile loading with $\sigma_n = 0.4f_y$

($\alpha=14$, $\beta=0.5$, $\gamma=18$, $\tau=0.6$, $\theta=60^\circ$, $a/t_0=0.5$, $c/a=8$, $t_0=10\text{mm}$)

(a) $\sigma_n = 0.1 f_y$

(b) $\sigma_n = 0.4 f_y$

Fig. 3.56 Effect of element layers at Part A of central cracked plain plates
($a/t_0=0.5$, $c/a=5$, $W=320\text{mm}$, $L=320\text{mm}$, $t_0=16\text{mm}$, axial loading)

(a) $\sigma_n = 0.1f_y$

(b) $\sigma_n = 0.4f_y$

Fig. 3.57 Effect of element layers at Part A of cracked CHS T/Y-joints
($\alpha=14$, $\beta=0.5$, $\gamma=18$, $\tau=0.6$, $\theta=60^\circ$, $a/t_0=0.5$, $c/a=8$, $t_0=10\text{mm}$, $\sigma_n=0.1f_y$, axial loading)

(a) $\sigma_n = 0.1 f_y$

(b) $\sigma_n = 0.4 f_y$

Fig. 3.58 Effect of element rings enclosing the crack front of cracked plain plate

($a/t_0 = 0.5$, $c/a = 5$, $W = 320\text{mm}$, $L = 320\text{mm}$, $t_0 = 16\text{mm}$, axial loading)

(a) $\sigma_n = 0.1f_y$

(b) $\sigma_n = 0.4f_y$

Fig. 3.59 Effect of element rings enclosing the crack front of cracked CHS T/Y-joint
 $(\alpha=14, \beta=0.5, \gamma=18, \tau=0.6, \theta=60^\circ, a/t_0=0.5, c/a=8, t_0=10\text{mm}, \text{axial loading})$

Fig. 3.60 Load-displacement curves of cracked CHS T-joint reported by Zerbst et al.
(Zerbst, Heerens & Schwalbe, 2002)

Fig. 3.61 Load-displacement curves of cracked CHS T-joints containing a surface crack at the crown (Lie, Li & Shao, 2014)

Fig. 3.62 Load-displacement curves of cracked CHS T-joints containing a surface crack at the saddle (Lie, Li & Shao, 2014)

CHAPTER 4

EXPERIMENTAL TEST OF A MULTI-PLANAR CHS TT-JOINT

4.1 INTRODUCTION

The experimental tests on cracked multi-planar CHS joints have not been reported so far in the literature. The previous related works focus mainly on the prediction of plastic collapse load (Paul, 1992; Scola & Redwood, 1990) and the hot spot stress (Karamanos, Romeijn & Wardenier, 2000; 2002) of uncracked multi-planar CHS TT-joints.

This chapter reports the experimental test results on a multi-planar CHS TT-joint. The experimental test is divided into three phases. Firstly, the stress distribution along the weld toe on the chord and braces of two chord-brace zones are measured using strain gauges. After that, 16 channels of probes are welded on the chord surface where the hot spot stress is located. These probes are connected to the alternating current potential drop (ACPD) equipment which is capable to monitor the fatigue crack propagation. When the crack depth has reached 80% (10mm) of the chord thickness, the fatigue test is stopped. Finally, static axial load is applied incrementally on the brace until final failure. Load-displacement curve obtained from the static test is then plotted and used for validating the finite element model of the cracked multi-planar CHS TT-joint.

4.2 TEST DESIGN

A multi-planar CHS TT-joint is fabricated at ambient temperature in the laboratory. Full penetration butt weld (Fig. 4.1) is used to weld the braces on to the chord. Fig. 4.2 shows the set-up of the test rig and the specimen. It can be seen that three cylindrical bolts are used to support the specimen. Therefore, the chord cannot extend freely along its axial direction, but the two ends of the chord are free to rotate. A thick plate of 30mm is welded to the vertical brace end. Four pieces of reinforced plate are then welded to the vertical brace end and the thick plate to prevent it from potential local failure. The thick plate is then connected to the actuator using 12 M10.8 high strength bolts. The horizontal brace is connected to the short built-up column using a cylinder bolt. Therefore, the end of the horizontal brace is also free to rotate but it cannot extend freely. The three short built-up

columns are connected to the strong floor using high strength long bolts. Prior to the start of the test, finite element (FE) analysis is carried out to estimate the plastic collapse load of the uncracked multi-planar CHS TT-joint, and it is found that the resistance of the strong floor is sufficient.

The geometry of the specimen is shown in Table 4.1. The dimensions used in Table 4.1 are defined as follows:

d_0	:	Chord diameter of multi-planar CHS TT-joint
d_1, d_2	:	Brace diameters of multi-planar CHS TT-joint
t_0	:	Chord thickness of multi-planar CHS TT-joint
t_1, t_2	:	Brace thicknesses of multi-planar CHS TT-joint
l_0	:	Chord length of multi-planar CHS TT-joint
l_1, l_2	:	Brace lengths of multi-planar CHS TT-joint
φ	:	Angle between two braces of multi-planar CHS TT-joint

Several coupon test specimens are prepared (Fig. 4.3) and tested before testing the multi-planar CHS TT-joint. The stress-strain curve of the chord and the brace material are shown in Fig. 4.4.

4.3 HOT SPOT STRESS DISTRIBUTION

In order to predict the crack initiation site, strain gauges are deployed along the two chord-brace intersection zones to investigate the stress distribution as shown in Figs. 4.5(a) and (b). The two chord-brace zones are named as chord-brace zone 1 (vertical brace) and chord-brace zone 2 (horizontal brace) respectively. From the prior FE analysis, it is found that the surface crack may initiate at the crown location of the chord at the chord-brace

zone 1. Therefore, strain gauges are deployed along the entire weld toe on the chord and the vertical brace at the chord-brace zone 1. At the chord-brace zone 2, strain gauges are deployed along half of the entire weld toe on the chord and the horizontal brace. Fig. 4.6 shows the strain gauges at the two chord-brace zones. Four pieces of strain gauges in the direction perpendicular to the strain gauges at the crown position are attached as shown in Fig. 4.7. They are used to measure two important strain components used to convert the strain concentration factors to the stress concentration factors introduced earlier as Eq. (2.9) in Chapter 2. The relationship between the strain concentration factors and the stress concentration factors are expressed as

$$SCF = \frac{(1 + \nu \varepsilon_1 / \varepsilon_2)}{1 - \nu^2} SNCF \quad (4.1)$$

where ε_1 is obtained from strain gauges perpendicular to strain gauges at the crown and obtain from the strain gauges at the crown. In this study, $\varepsilon_1 / \varepsilon_2$ is equal to 0.2, and Poisson's ratio is 0.3. Therefore, Eq.(4.1) becomes to

$$SCF = 1.16 SNCF \quad (4.2)$$

The linear interpolation boundaries are determined from CIDECT (Zhao et al., 2000), and they are tabulated in Table 4.2.

A magnitude of 10kN preload is first applied to check the data logging system as well as to remove any possible frictional force between the specimen and the test rig. After that, the actuator is held at the position where the applied load reading is 0, and all the strain gauges are then initialized. In the loading process, load is firstly applied to 30kN, then the actuator is held in place and the strain readings are recorded. The load is then increased to the next load of 50kN, and the corresponding strain gauge readings are recorded again.

All the strain data is converted to stress by multiplying 1.16 time of the Young's modulus 205GPa. Fig. 4.8 shows the stress distribution on the chord of the chord-brace intersection zone 1. Gauge 1 and Gauge 2 represent the strain gauges away and close to the weld toe, respectively. It can be seen that the hot spot stress which is the maximum stress along the weld toe is located at the second crown position (Fig. 4.5(a)). The stresses at the two

crown points are different. It is found that the vertical brace is not exactly located at the middle of the chord, and there is a 3mm misalignment at the chord axial direction. The misalignment of the vertical brace causes additional bending effect so as to make the stress at the two crown points different. It can also be seen that the stress distribution on the side of the horizontal brace is higher than at the opposite side due to the constraint applied at the horizontal brace end.

Fig. 4.9 shows the stress distribution along the weld toe on the chord at the chord-brace zone 1 after the cylindrical bolt used to constrain the horizontal brace is removed. It can be seen that the stresses at the weld toe on the side of the horizontal brace decrease. The stresses at the weld toe on the opposite side have a slight variation. The hot spot stress is still located at the second crown position.

Fig. 4.10 shows the stress distribution along the weld toe on the vertical brace of chord-brace zone 1 with the horizontal brace end pinned using the cylindrical bolt. It can be seen that the maximum stress is located at the inside saddle position of the vertical brace. The magnitude of the maximum stress on the vertical brace is much smaller as compared to the hot spot stress on the chord. Therefore, it is predicted that the fatigue crack will initiate at the chord rather than at the vertical brace. The stress distribution on the side of the horizontal brace is higher than the opposite side due to the constraint applied at the horizontal brace end.

Fig. 4.11 shows the stress distribution along the weld toe on the vertical brace with the horizontal brace end free of any constraint. It can be seen that the maximum stress is shifted to the outside saddle of the vertical brace. In addition, it is found that stress distribution along the weld toe of the vertical brace on the side of the horizontal brace decreases due to the unconstraint of the horizontal brace.

Figs. 4.12 – 4.15 show the stress distribution along the weld toe of the chord and the horizontal brace at the chord-brace zone 2. It can be seen that most of the recorded strain data is under compression, and the strain data under tension is very small. It implies that the fatigue crack will not initiate along the weld toe at the chord-brace zone 2. Therefore, no further discussion is provided for the stress distributions at the chord-brace zone 2.

From the above static test, it can be concluded that the horizontal brace can cause significant influence on the stress distribution at the chord-brace zone of the vertical brace only when the horizontal brace is subjected to external load or constraint. As the horizontal brace end is constrained in the fatigue test, it can be concluded that the fatigue crack will initiate at the second crown position of the chord at the chord-brace zone 1.

4.4 FATIGUE TEST

In this study, the alternating current potential drop (ACPD) technique is used to monitor and record the crack initiation and propagation. It is based on the ‘skin effect’, a characteristic of high-frequency current flowing through a ferromagnetic material, whereby the majority of the current is confined to a thin skin at the surface of the material. At a frequency of 5kHz, ferromagnetic mild steel has an approximately 0.1mm skin depth (Lie et al., 2001). In the cracked specimen, the high-frequency current is injected at points on the end face of the specimen on either side of the crack surfaces, and it flows along a path defined by the crack surfaces as shown in Fig. 4.16. A linear surface potential gradient exists along this current path. A reference potential can be measured on an uncracked region to define the strength of the potential gradient. The crack depth can then be estimated by normalizing the cross-crack measurement with respect to the reference measurement (Lie et al., 2001).

Measurements of potential drops across the crack and the uncracked region adjacent to the crack are given by

$$V_C \propto (\Delta_R + 2d_1) \quad (4.3)$$

$$V_R \propto \Delta_R \quad (4.4)$$

where V_R is the reference potential drop, V_C is the cross-crack potential drop, Δ_R is the reference probe gap, Δ_C is the cross-crack probe gap, and d_1 is the crack depth at that particular probe or site. From Eqs. (4.3) and (4.4), the crack depth can be obtained as follows:

$$d_1 = (\Delta_R/2)(V_C/V_R - \Delta_C/\Delta_R) \quad (4.5)$$

For the multi-planar CHS TT-joint tested in this study, Δ_C and Δ_R are both equal to 10mm, and then Eq. (4.5) reduces to

$$d_1 = (\Delta/2)(V_C/V_R - 1.0) \quad (4.6)$$

where Δ is the probe spacing.

In this study, the ACPD crack depth probing distances around the fatigue crack locations are shown in Fig. 4.17. Specially manufactured probes of diameter of 1 mm and length of 10mm are spot-welded on the chord surface tightly as shown in Fig. 4.18. All these probes are connected to the U20 Crack Microgauge (Lugg, 2008) as shown in Fig. 4.19. The U20 Crack Microgauge is connected to a computer using the RS232 serial interfaces, and it is controlled by the LIMOS software (Lugg, 2008) that provides the automated instrument control, data storage facilities and dedicated graphical output under the Windows environment. During the test, the crack depth is scanned and recorded automatically.

The sinusoidal amplitude load shown in Fig. 4.20 is used in the fatigue test. The hot spot stress range is 320MPa which is equal to 75.2% of the yield strength of the chord material. Fig. 4.21 shows the surface crack at the crown of the tested multi-planar CHS TT-joint.

Fig. 4.22 illustrates the process of the crack initiation and propagation of the multi-planar CHS TT-joint. It can be seen that the crack shape is nearly symmetrical. The deepest point of the crack is at the hot spot location at the crack initiation stage, and then it moves 20mm towards the inside saddle at the end of the fatigue test. Fig. 4.23 shows the comparison of the crack shapes among the ACPD measurement, semi-elliptical and manual measurement. It can be seen that the crack depth measured using the ACPD is underestimated. The actual crack is an approximation of a semi-ellipse in shape. Therefore, it is reasonable to assume a semi-elliptical crack shape in the finite element analysis. The crack length can be accurately detected by using the ACPD technique. However, the crack depth data measured using the ACPD technique may not be very accurate at the two crack ends.

Fig. 4.24 shows the crack growth at the hot spot location (second crown). After the crack depth reaches 5mm which is 40% of the chord thickness, the crack propagates rapidly through the chord thickness. This is similar to the previously tested CHS T-joints (Huang, 2003), but different to the CHS K-joints (Shao, 2004). For CHS K-joints reported by Shao

(Shao, 2004), the crack propagates rapidly after the crack depth reaches 80% of the chord thickness.

In Fig. 4.25, the fatigue life of the specimen is plotted against the S-N curves. The hot spot stress range is calculated by multiplying the nominal stress range with the stress concentration factor at the hot spot stress location. It can be seen from Fig. 4.20, the loading range ΔP is 120kN. Therefore, the nominal stress range can be calculated as

$$\Delta\sigma_n = \frac{4\Delta P}{\pi[d_1^2 - (d_1 - t_1)^2]} = \frac{4 \times 120000}{3.14 \times [88.9^2 - 66.9^2]} = 44.57 \text{MPa} \quad (4.7)$$

where $\Delta\sigma_n$ is the nominal stress range and ΔP is the loading range. The stress concentration factor at the hot spot stress location can be determined using the linear interpolation method incorporated in CIDECT (Zhao et al., 2000) together with Eq. (4.2). From Fig. 4.8, it can be seen that the stress at the first interpolation point (5mm away from the weld toe) is 68.43MPa, and the stress at the second interpolation point (11.4mm away from the weld toe) is 52.77MPa under the loading of 30kN; the stress at the first interpolation point (5mm away from the weld toe) is 117.73MPa and the stress at the second interpolation point (11.4mm away from the weld toe) is 90.08MPa under the loading of 50kN. By using the linear interpolation method, the stress concentration factor can be determined as 7.23 and 7.50 under the loading of 30kN and 50kN, respectively. The average 7.37 is taken as the stress concentration factor at the hot spot stress location in this study. Finally, the hot spot stress range can be calculated as

$$\Delta\sigma_n = 7.37 \Delta\sigma_n = 328.48 \text{MPa} \quad (4.8)$$

It can be seen that S-N curve approach can be used to determine the fatigue life of multi-planar CHS TT-joint.

4.5 LOAD DISPLACEMENT CURVE

In order to produce a more accurate FE results, a 3D model shown in Fig. 4.26 is generated. Fig. 4.27 shows the mesh details of the connection between the specimen and

the built-up column. Contact is defined between the surfaces of the cylindrical bolt and the built-up column. The weld has the same material properties as the chord used in the FE analysis. This is because failure occurs on the chord instead at the weld. It implies that the strength of the weld and the chord is matched. Load-displacement curve of the cracked multi-planar CHS TT-joint is compared with the FE results and the corresponding uncracked joint in Fig. 4.28. It can be seen that the slope of the load displacement curve (cracked joint) of the FE result is larger than the experimental result at the elastic stage. This is due to the gaps existed between the cylindrical bolts and the bolt holes in the experimental test. In the FE analysis, these gaps are removed so that the stiffness of the entire structure is increased, and thus, the slope of the load displacement curve is higher at the elastic stage. At the elastic-plastic stage, load displacement curves of the specimen agree quite well. When the brace end displacement reaches 37mm, the plastic collapse load of the cracked TT-joint is about 371kN, which is about 71.9% of the corresponding uncracked joint at the same displacement level. The crack length and depth measured manually are 150mm and 11.5mm, respectively. Therefore, the crack area A_c is 1354.8mm². The length of the weld toe measured manually is 314mm, and thus, the crack area is 34.5% of the total area which is the product of chord thickness and weld toe length of the chord. It means that the plastic collapse load of the uncracked CHS TT-joint decreases by 28.1% when the crack area reaches 34.5% of the total area. According to the strength reduction factor equation given in BS7910 (2013), F_{AR} is 0.655 when the crack area reaches 34.5% of the total area. It can be seen that 0.665 is much smaller than 0.719. It appears that the strength reduction factor F_{AR} in BS7910 (2013) may also be valid for the multi-planar CHS TT-joints investigated in this study. This may due to the fact that the loading and boundary conditions of the cracked multi-planar CHS TT-joint tested are very similar to the cracked uni-planar CHS T-joints under tensile loading. In fact, the additional brace of the multi-planar CHS TT-joint in the test has a small influence on the load displacement curve. This will be further described subsequently.

In order to further explore the similarity between the uni-planar CHS T-joints and the multi-planar CHS TT-joints tested, load displacement curves of the cracked and the uncracked CHS T-joints generated by removing the horizontal brace of the multi-planar CHS TT-joint, are produced in this study. Fig. 4.29 shows the comparison of the load displacement curves of the CHS T-joint and TT-joint. It can be seen that load displacement curves of the CHS T-joint are always slightly lower than the corresponding curves of the

CHS TT-joint. This is mainly due to the additional stiffness caused by the horizontal brace of the CHS TT-joint. The strength reduction factor of the cracked CHS T-joint shown in Fig. 4.29 is 0.703, which is quite close to the corresponding cracked CHS multi-planar TT-joint.

4.6 RESULTS AND DISCUSSION

A multi-planar CHS TT-joint is tested under static and fatigue loading conditions. The main objective of the test is to verify the accuracy of the FE models of cracked multi-planar CHS TT-joints generated by the new mesh generator. In order to do so, load displacement curve of the cracked multi-planar CHS TT-joint obtained from FE analysis is compared to the curve recorded in the test. It is found that the FE models of cracked multi-planar CHS TT-joints generated by the new mesh generator are reliable. Meanwhile, based on the observation and analysis in this chapter, the following conclusions can be drawn:

- (1) The stress distribution along the weld toe of the multi-planar CHS TT-joint is different to that of the uni-planar T-joint, and it is not symmetrical about the plane determined by the brace and the chord. This is mainly due to the additional stiffness caused by another brace of the multi-planar CHS TT-joint. The hot spot stress location of a multi-planar CHS TT-joint may also be located at the crown.
- (2) The fatigue crack initiates from the hot spot stress location. However, as the crack size increases, the deepest point of the fatigue crack is observed to shift to other position along the weld toe due to load shedding effect. It is observed that the semi-elliptical shape assumption of the fatigue cracks for uni-planar CHS joints is also valid for the multi-planar CHS TT-joint. This can be observed from the crack propagation data recorded from the test.
- (3) S-N curves recommended in CIDECT (Zhao et al., 2000) can produce a safe prediction of the fatigue life of the multi-planar CHS TT-joint tested in this study.
- (4) It is found that the strength reduction factor F_{AR} recommended in BS7910 (2013) is able to produce a conservative prediction of the plastic collapse load of the tested multi-planar CHS TT-joint. In addition, the strength reduction factor of the cracked multi-planar CHS TT-joint is quite close to the corresponding cracked uni-planar CHS T-joint, which is generated by removing the horizontal brace of the cracked multi-planar CHS TT-joint. It is

very important to recognize that the conclusion drawn here is for the multi-planar CHS TT-joints with one brace subjected to tensile loading and the other brace simply pinned. For the multi-planar CHS TT-joints with two braces subjected to axial load, the strength reduction factors will be investigated in the subsequent chapters.

Table 4.1 Dimensions of the multi-planar CHS TT-joint

d_0 (mm)	d_1, d_2 (mm)	t_0 (mm)	t_1, t_2 (mm)	l_0 (mm)	l_1 (mm)	l_2 (mm)	φ
219	88.9	12.5	11	1600	600	700	90°

Table 4.2 Boundaries of the linear stress interpolation region

		Chord		Brace	
		Saddle	Crown	Saddle	Crown
CHS	$L_{r,min}$	5mm		4.4mm	
	$L_{r,max}$	9.9mm	11.4mm	14.4mm

Fig. 4.1 Full penetration butt weld

Fig. 4.2 Set-up of test rig and TT-joint specimen

Fig. 4.3 Coupon testing

Fig. 4.4 Stress-strain curve of chord and brace

(a) chord-brace zone 1 (vertical brace)

(b) chord-brace zone 2 (horizontal brace)

Fig. 4.5 Strain gauges positions at the chord-brace zone 1 and 2

Fig. 4.6 Strain gauges at chord-brace zone 1 and 2

Fig. 4.7 Strain gauges used to determine ϵ_1

Fig. 4.8 Stress distribution on the chord of chord-brace intersection zone 1 with the horizontal brace end constrained

Fig. 4.9 Stress distribution on the chord of chord-brace intersection zone 1 with horizontal brace end free

Fig. 4.10 Stress distribution on the brace of chord-brace intersection zone 1 with the horizontal brace end constrained

Fig. 4.11 Stress distribution on the brace of chord-brace intersection zone 1 with horizontal brace end free

Fig. 4.12 Stress distribution on the chord of chord-brace intersection zone 2 with the horizontal brace end constrained

Fig. 4.13 Stress distribution on the chord of chord-brace intersection zone 2 with horizontal brace end free

Fig. 4.14 Stress distribution on the brace of chord-brace intersection zone 2 with the horizontal brace end constrained

Fig. 4.15 Stress distribution on the brace of chord-brace intersection zone 2 with horizontal brace end free

Fig. 4.16 The ACPD theory and notations

Fig. 4.17 ACPD probes spot welded positions at the chord-brace zone 1

Fig. 4.18 Close-up view of the probes on chord surface

Fig. 4.19 Close-up view of the probes and connecting cables

Fig. 4.20 Sinusoidal amplitude load used in the fatigue test

Fig. 4.21 Surface crack at the weld toe after fatigue test

Fig. 4.22 ACPD crack propagation data using an interval of 1800 cycles

Fig. 4.23 Comparison of different crack shapes

Fig. 4.24 Growth of crack depth at the hot spot location

Fig. 4.25 Fatigue test result plotted against the S-N curves

Fig. 4.26 FE 3D model of the tested multi-planar CHS TT-joint

Fig. 4.27 Close-up view of the mesh details at the connection between the specimen and built-up column

Fig. 4.28 Comparison of load displacement curves between FE analyses and experimental test

Fig. 4.29 Comparison of load displacement curves between CHS T- and TT-joints.

CHAPTER 5

PLASTIC COLLAPSE LOADS OF CRACKED UNI-PLANAR CHS T/Y-JOINTS

5.1 INTRODUCTION

Plastic collapse load of a cracked CHS joint is an important parameter used in the failure assessment diagram analysis. Basically, it is the static strength of the cracked joint without allowing for fracture of the crack and crack propagation. If ductile tearing occurs, then failure may be governed by ductile instability instead of collapse. Plastic collapse load can be estimated by using finite element (FE) analysis if ductile tearing or plastic collapse occurs before the brace is entirely pulled out from the chord (Stacey & Sharp, 1996a; 1996b). It is important to recognize that plastic collapse load is applicable for the crack size being analyzed or tested, for fully plastic response. If the failure modes of the cracked joint and its material property behave in a very brittle manner, then the plastic collapse load of the cracked joint cannot be determined experimentally. However, it was reported by some researchers that ductile tearing is frequently observed in the laboratory tests (Qian, 2005; Zerbst et al., 2002a) even if the test temperature is as low as -10°C (Stacey & Sharp, 1996a; 1996b). Therefore, it is still meaningful to investigate the plastic collapse load of cracked CHS joints using finite element analysis.

In BS7910 (2013), a strength reduction factor F_{AR} is recommended to calculate the plastic collapse load of cracked CHS joints in conjunction with the characteristic strength of the corresponding uncracked joints. The strength reduction factor F_{AR} in BS7910 (2013) is believed to be very conservative. Besides, it is important to recognize that this F_{AR} is only applicable to specific cracked CHS joints subjected to axial tensile load only. For cracked CHS joints subjected to in-plane and out-of-plane bending, currently there are no guidelines available in any standard or other references.

In this chapter, the plastic collapse load/moment of cracked CHS T/Y-joints containing a semi-elliptical surface crack at the weld toe of the chord member is investigated taking advantage of the new mesh generator. Three basic loads, namely axial tensile load, in-plane and out-of-plane bending are covered. Firstly, the plastic collapse loads of uncracked

CHS T/Y-joints are carefully determined as it provides the basis of estimating the plastic collapse load of the cracked joints. Secondly, an elaborate parametric study is then carried out to investigate the effect of surface crack on the plastic collapse loads. Finally, three sets of reduction factor for cracked CHS T/Y-joints subjected to three basic loads are proposed based on the numerical results.

5.2 MATERIAL PROPERTY

The stress-strain data reported by van der Vegate (van der Vegate, 1995) is used in this study. Fig. 5.1 shows the true stress-strain data used in FE analysis. The yield and the ultimate strength are 355MPa and 630MPa respectively. The stress-strain data reported by van der Vegate (van der Vegate, 1995) has also been adopted by Qian (Qian, 2005).

5.3 GEOMETRY SCOPE OF PARAMETRIC STUDY

In the parametric study, it would be ideal to cover the entire range of all geometric parameters of CHS T/Y-joints used in practice. However, due to some limitations and various considerations, the following ranges are specified:

$$0.2 \leq \beta \leq 0.8$$

$$10 \leq \gamma \leq 25$$

$$60^\circ \leq \theta \leq 90^\circ$$

For β , the range of $0.2 \leq \beta \leq 1$ is specified in many prevalent standards such as IIW (2009), API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008). However, for CHS T/Y-joints with larger β values approaching to 1, a special mesh design of the chord-brace intersection zone need to be employed due to the unique weld profile (Qian, 2005), and such a special mesh design is not attempted in this study. Due to the limitation of the new mesh generator, the maximum value of β equal to 0.8 is used in this study.

For γ , a maximum value of 25 generally is specified in many standards. In Eurocode 3 (2005), the minimum value of γ is specified as 10 for CHS T/Y-joints. In addition, it is noted when γ ratio is less than 10, a CHS tube should be treated as a thick member (Wang, Wu & Qian, 2011). For thick chord member, lamellar tearing failure was observed in the laboratory test (Qian, 2013). This type of failure mode cannot be taken into account in this study. Therefore, a minimum γ ratio of 10 is specified.

For θ , its range generally varies from 30° to 90° . When θ is close to 30° , it is difficult to guarantee good welding quality at the weld heel in practice. In the new mesh generator, the mesh quality at the weld heel may deteriorate when θ approaching 30° . Therefore, a moderate θ value of 60° is chosen to study the effect of the surface crack on the plastic collapse loads.

For τ , it has no effect on the plastic collapse loads of uncracked CHS T/Y-joints; it is also mentioned in the above standards. Therefore, τ is fixed as 1 and its variation is ignored in the parametric study.

CHS T/Y-joints failing at the brace are not covered in this study. For CHS T/Y-joints subjected to in-plane and out-of-plane bending, the brace is intentionally shortened to 400mm so as to ensure that failure occurs on the chord.

The thickness of the chord is fixed as 10mm. Other geometries such as the brace diameter and thickness, and the chord diameter can be derived using equations indicated in Fig. 5.2.

CHS T/Y-joints with β and γ equal to 0.2 and 10 are omitted in this study as the brace diameter is too small.

It is commonly accepted that chord length should be at least four times the chord diameters to eliminate end effects on joint strength, which means α should be greater than 8. In this study, three types of chord members are used, including $\phi 200 \times 10$, $\phi 360 \times 10$ and $\phi 500 \times 10$. The corresponding α values are 22, 18, and 18 respectively.

From previous study reported by Laham and Burdekin (Laham & Burdekin, 1997; Burdekin, 2002), it was found that crack area is the main factor affecting the bearing capacity of cracked CHS joints. For a surface crack (BS7910, 2013), the crack area can be calculated using the following equation:

$$A_c = 0.5\pi ac \quad (5.1)$$

In this study, the dimensionless crack area A_c is defined as

$$A_{nc} = \frac{100A_c}{t_0 l_w} \% \quad (5.2)$$

where t_0 is the chord thickness and l_w is the length of the weld toe on the chord. The ranges of A_{nc} and a/t_0 are specified as

$$\begin{aligned} 0 &\leq A_{nc} \leq 25\% \\ 0.2 &\leq a/t_0 \leq 0.8 \end{aligned}$$

For CHS T/Y-joints subjected to basic loading, a surface crack is frequently detected at the crown, heel or saddle position of the chord due to the hot spot stress. In this study, the hot spot stress is first calculated using API-RP-2A (2005) and NORSOK (1998) standards to determine the position of the surface crack. Later, for each uncracked CHS T/Y-joint, the maximum principal stress at the weld toe of the chord is individually investigated so as to double-check the hot spot stress position. For all the analyzed CHS T/Y-joints subjected to axial tensile load and out-of-plane bending, the surface crack is located at the saddle position of the chord. For CHS T/Y-joints subjected to in-plane bending, the surface crack is located at the crown where it is on the tensile side of the brace.

5.4 UNCRACKED CHS T/Y-JOINTS SUBJECTED TO AXIAL LOAD

Plastic collapse load of an uncracked CHS T/Y-joint essentially is its ultimate strength. For consistency purpose, plastic collapse load/moment is used instead of the ultimate strength in this study. The plastic collapse load/moment of a cracked CHS joint is obtained from the load displacement or moment rotation curve under the same twice elastic compliance or Lu's deformation limit used to determine the plastic collapse load/moment of the corresponding uncracked joint. In order to do so, load displacement or moment rotation curves of the uncracked and cracked CHS joints should be plotted together. In this study, the plastic collapse load/moment is normalized by the yield stress of the material. The plastic collapse load/moment of cracked CHS joints has been defined in Section 2.4.3 of Chapter 2. In this section, firstly, plastic collapse loads of uncracked CHS T/Y-joints subjected to axial tensile load are investigated. After that, plastic collapse loads of cracked CHS T/Y-joints are determined. The strength reduction factor F_{AR} is then proposed using the following equation:

$$F_{AR} = \frac{\text{Plastic collapse load / moment of cracked CHS joints}}{\text{Plastic collapse load / moment of uncracked CHS joints}} \quad (5.3)$$

Consequently, the effect of non-dimensional geometry parameters including β , γ and θ on the F_{AR} are investigated. Finally, a set of strength reduction factor is suggested based on the numerical results.

5.4.1 Boundary Conditions

In reality, a CHS joint is constrained by adjacent joints. The stiffness of the constraint generated by adjacent joints is flexible. It could be very strong which can be approximated as the stiffness of a fixed boundary condition, or it could be very weak which can be approximated as the stiffness of a pinned boundary condition. In the FE analysis, two types of boundary condition shown in Figs. 5.3(a) and (b) are applied to uncracked CHS T/Y-joints to study the effect of the boundary condition. They are designed to represent the pinned and fixed boundary conditions respectively. For the pinned boundary condition, only nodes indicated in red are fixed in all degree of freedom to simulate the weak constraint generated by adjacent joints. For the fixed boundary condition, all nodes at the ends of the chord are fixed in all degree of freedom to simulate the strong constraint generated by adjacent joints.

Fig. 5.4 shows the effect of boundary condition on the load displacement curves of a CHS T/Y-joint subjected to axial tensile load. The displacement mentioned here refers to the spatial movement of the brace end. It can be seen that the difference between two load displacement curves is rather large. This is because the two load displacement curves shown in Fig. 5.4 represent accurately the member strength of two joints instead of the joint strength, which is the local bearing capacity of the chord-brace intersection zone. The large difference between the two curves shown in Fig. 5.4 is mainly due to the chord bending under different boundary conditions. The two load displacement curves shown in Fig. 5.4 are used to determine the plastic collapse loads of two CHS joints by using the twice elastic compliance. In fact, the twice elastic compliance has a limitation in determining the plastic collapse load of CHS joints under axial loading because it cannot eliminate the chord bending effect. Lu's deformation limit (Lu et al., 1994) is becoming more and more popular to determine the plastic collapse load of CHS joints. The plastic

collapse load, which is identical to the joint strength of the two CHS joints, can be determined from the two load displacement curves by using Lu's deformation limit of 3% of the chord diameter. Lu's deformation limit will be introduced later. For the two joints shown in Fig. 5.4, value of Lu's deformation limit is 10.8mm, and it is reached when the displacement is 11.5mm and 33.5mm for the fixed and pinned boundary conditions and the corresponding normalized load is 22.2 and 16.6, respectively. Therefore, the difference between the two CHS joints with fixed and pinned boundary conditions is 33.7%. In this study, the pinned boundary condition is adopted for CHS T/Y-joints subjected to axial tensile load. This is mainly due to safety reason as conservative design strength of CHS joints is preferred in practice. The arguments on the member strength, joint strength and the limitation of the twice elastic compliance will be further discussed later.

5.4.2 Deformation Characteristic

For CHS T/Y-joints subjected to axial tensile load, the load displacement curve normally behaves as continuously increasing without showing a peak value. For such kind of load displacement curve, it is widely agreed that a deformation limit should be adopted to determine the plastic collapse load (Zhao, 2000; Kostas, Packer & Puthli, 2003; Qian, 2005). Therefore, it is important to investigate the deformation characteristic of uncracked CHS T/Y-joints subjected to axial tensile load.

For uncracked CHS T/Y-joints subjected to tensile axial load, two types of failure mode have been observed in the laboratory tests, namely chord plastification and punching shear failures. It is found that local bulging and internal ovalization of two chord side-walls occur on the chord for all analyzed joints. Figs. 5.5(a) and (b) show two representative cases of deformed CHS T/Y-joints. It can be seen that for the CHS T/Y-joint with $\gamma=25$ and $\beta=0.2$, local bulging of the chord is the dominant deformation and the internal ovalization is minor. Besides, the overall bending of the entire chord is also minor. It is reasonable to conclude that punching shear may occur for this type of joint. For CHS T/Y-joints with medium β and γ , such as 0.8 and 18 respectively, local bulging and internal ovalization of two chord-side walls are present simultaneously. Chord plastic failure is expected to occur for this type of joint.

5.4.3 Load Displacement Curve

In this study, load displacement curve of the CHS T/Y-joint is defined as the load applied to the brace end versus the displacement of the brace end. As expected, for all uncracked CHS T/Y-joints subjected to axial tensile load, load displacement curves always behave as continuously increasing without showing a peak value. Fig. 5.6 shows load displacement curves of two uncracked CHS T/Y-joints of which the deformation characteristic is investigated in previous section. It can be seen that although the load displacement curves behaves as continuously increasing, the slope of the curves are quite different after yielding occurs. The slope of load displacement curve of the joint failing in punching shear is much higher.

5.4.4 Plastic Collapse Load

There are several criteria which can be used to determine the plastic collapse load/moment of uncracked CHS T/Y-joints (Kosteski, Packer & Puthli, 2003). Among of them, Lu's deformation limit is the most widely accepted one currently (Lu et al., 1994). It has also been found from extensive references that twice elastic compliance (TEC) is the only criterion that has been used to determine the plastic collapse load of cracked CHS joints, such as cracked T- and K-joints containing a through thickness crack (Yang, 1996). However, no researcher has ever investigated the accuracy of the twice elastic compliance to determine the plastic collapse load of cracked CHS joints. Therefore, twice elastic compliance is also adopted in this study. It should be recognized that initially both above criteria are recommended for determining the plastic collapse load of uncracked CHS joints rather than cracked joints.

The usage of twice elastic compliance is a very straightforward one. Therefore, it will not be explained further in this section. In Lu's deformation limit, a local deformation limit of 3% chord diameter at the chord-brace intersection zone is suggested as the ultimate deformation limit, and the corresponding load at the brace end is taken as the plastic collapse load. Lu's limit is a type of local deformation. However, the definition of the local deformation and its position are not apparent in Lu's limit. Some researchers interpret the local deformation as the relative ovalization of two chord side-walls (Lesani, Bahaari & Shokrieh, 2013), while others interpret it as the local bulging or indentation on the chord (Kosteski, Packer & Puthli, 2003). As the local bulging and internal ovalization of two

chord side-walls at the chord-brace intersection zone are present simultaneously for CHS T/Y-joints subjected to axial tensile load, both of them are taken into account in this study. Local bulging and internal ovalization of two chord side-walls which reaches 3% of chord diameter first is taken as Lu's limit.

Fig. 5.7 shows two interpretations of Lu's limit. For determining the local bulging on the chord, vertical displacements of nodes indicated in red are output first so that the relative displacements of the top and bottom nodes can be calculated. By doing so, the overall bending of the chord is eliminated. The relative displacement in this case is defined as the local bulging of the chord. Another interpretation of Lu's limit, relative internal ovalization is also shown in Fig. 5.7. The main difficulty of calculating relative internal ovalization is from that the positions of nodes with maximum internal displacement are varied for CHS T/Y-joints with different β values. Therefore, in this study, value of local bulging is determined first under each loading increment. When the local bulging reaches 3% of chord diameter, the relative internal ovalization is checked individually. It is found that value of local bulging always reaches 3% of chord diameter first for all the analyzed joints. Therefore, the brace end load corresponding to the local bulging of 3% of chord diameter is taken as the plastic collapse load for uncracked CHS T/Y-joints subjected to axial tensile load.

Fig. 5.8 shows an example of the application of Lu's limit. For this case, the value of Lu's limit is 15mm. The deformation values at load increment 27 and 28 are determined first, and they are 13.44mm and 15.68mm respectively. It can be seen that Lu's limit is reached in between load increment 27 and 28. As the loads at load increment 27 and 28 are known, the linear interpolations method is used to determine the plastic collapse load of the joint.

Figs. 5.9(a) and (b) show the applications of twice elastic compliance and Lu's limit to determine the plastic collapse load of the two representative CHS T/Y-joints subjected to axial tensile load. From Fig. 5.9(a), it can be seen that for CHS T/Y-joints which punching shear failure may occur, a significant discrepancy between plastic collapse loads determined using twice elastic compliance and Lu's limit is observed. Twice elastic compliance appears to be not applicable for such kind of joints.

5.4.5 Results and Discussions

Figs. 5.10(a), (b) and (c) summarize the non-dimensional plastic collapse loads of 22 uncracked CHS T/Y-joints subjected to axial tensile load. The FE results are compared to the plastic collapse loads calculated using three prevalent codes, including API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008). The plastic collapse loads shown in Fig. 5.10 have been normalized with respect to the yield stress. It can be seen that the plastic collapse loads obtained from FE analyses increase as β increases for all the three γ values. For uncracked CHS T/Y-joints with $\gamma=10$ and 18, it is observed that $\beta=0.5$ appears to be at the middle of the data band. When β is smaller than 0.5, the plastic collapse loads determined from the FE analyses are generally above the curves of the three codes of practice for uncracked CHS T/Y-joints with $\gamma=10$ and 18. In contrast, when β is larger than 0.5, the plastic collapse loads determined from the FE analyses are generally below the curves of the three codes of practice. For uncracked CHS T/Y-joints with $\gamma=25$, the plastic collapse loads determined from the FE analyses are generally above the curves of the three codes of practice. However, it can also be seen that the plastic collapse loads determined by using the twice elastic compliance for $\beta=0.2$ and 0.4 may not be reliable as there is a large difference between the FE results and values calculated by using the three codes of practice. It can be seen that the plastic collapse loads determined by using Lu's deformation limit are more acceptable for uncracked CHS T/Y-joints with $\gamma=25$. Overall, it can be concluded that the plastic collapse loads of uncracked CHS T/Y-joints subjected to axial load determined by using Lu's deformation limit are more acceptable because the percentage difference between the values determined by using Lu's deformation limit and the average of the three codes is within the range of -26.2% to 68.8%. The percentage difference among the values calculated from the three codes of practice is within the range of -33.1% (the negative maximum difference over the maximum of the three values calculated using the three codes) to 49.3% (the maximum difference over the minimum of the three values calculated using the three codes).

As discussed in the above, twice elastic compliance appears to be not applicable to determine the plastic collapse load of CHS T/Y-joints subjected to axial tensile load for uncracked CHS T/Y-joints with small β ($\beta=0.2, 0.4$) and large γ ($\gamma=25$), as seen in the comparison shown in Fig. 5.10(c). Before further discussing the reason, it is necessary to clear the two concepts, joint strength and member strength. Joint strength refers to the

bearing capacity of the chord-brace intersection zone, and it is the research target of this study. Meanwhile, member strength refers to the bearing capacity of the member, and it is not the research target of this study. Plastic collapse load determined from twice elastic compliance is difficult to be taken as the bearing capacity of the joint because the overall bending of the chord member is not eliminated. It indicates that plastic collapse load determined from twice elastic compliance is very sensitive to the chord length for CHS T/Y-joints subjected to axial tensile load.

Fig. 5.11 shows three load displacement curves of a same CHS T/Y-joint with varying chord lengths. It can be seen that twice elastic compliance is not able to determine the true plastic collapse load of the joint. Plastic collapse loads determined from Lu's limit for the three load displacement curves are quite close, and they are 16.3, 15.6 and 15.1 respectively. Therefore, it can be concluded that Lu's limit is more suitable to determine the plastic collapse load of uncracked CHS T/Y-joints subjected to axial tensile load. In order to reach Lu's limit faster, α is changed to 12 from 22 for CHS T/Y-joints with $\gamma=10$ in this section.

5.5 CRACKED CHS T/Y-JOINTS SUBJECTED TO AXIAL TENSILE LOAD

In this section, plastic collapse load of cracked CHS T/Y-joints subjected to axial tensile load is studied. Firstly, plastic collapse loads of cracked CHS T/Y-joints subjected to axial tensile load are determined. Secondly, the strength reduction factor F_{AR} defined in Eq. (5.3) is calculated and the effect of various parameters such as crack area, β , γ , and θ are investigated. Finally, a set of F_{AR} equations is proposed based on the FE results.

5.5.1 Strength Reduction Factor F_{AR}

Figs. 5.12(a) and (b) show two series of non-dimensional load displacement curves of cracked CHS T/Y-joints. For the crack joints, the dimensionless crack area is increased from 10% to 25% with an increment of 5%. By examining all the plotted dimensionless load displacement curves, it is found that plastic collapse load of the cracked joint decreases as the crack area increases. It appears surface crack with less than 10% has small effect on the plastic collapse loads for all the analyzed cracked joints. From Fig. 5.12(a), it can be seen that the plastic collapse loads of the cracked joints do not decrease significantly when Lu's limit is reached. When twice elastic compliance limit is reached,

plastic collapse loads of the cracked joints with dimensionless crack area greater than 15% decrease rapidly. However, the plastic collapse load of the uncracked joint determined from twice elastic compliance is much larger than that determined from Lu's limit as well as the other three prevalent codes mentioned earlier. Therefore, it may not be necessary to check the bearing capacity of the cracked joints at twice elastic compliance limit.

5.5.2 Effect of θ

Figs. 5.13(a) and (b) show the effect of θ on F_{AR} . It can be seen that θ has a slight effect on F_{AR} .

5.5.3 Effect of β

Figs. 5.14(a), (b) and (c) show the effect of β on F_{AR} . The values of F_{AR} plotted in dash line are determined using twice elastic compliance, whereas the values of F_{AR} plotted in solid line are determined using Lu's limit. From the distributions of F_{AR} determined from Lu's limit, it can be seen that β has no significant effect on F_{AR} .

5.5.4 Effect of γ

Figs. 5.15(a), (b) and (c) show the effect of γ on F_{AR} . It can be seen that the distribution of F_{AR} determined from Lu's limit is more regular. Overall, γ has a slight effect on F_{AR} either.

5.5.5 Effect of Crack Area

Figs. 5.16 and 5.17 show the effect of crack area on F_{AR} . It can be seen that the plastic collapse load of the cracked joint decreases as the crack area increases. From Fig. 5.16, it can also be seen that the three F_{AR} curves diverge significantly from other curves. This is because punching shear failure may occur for the corresponding three uncracked joints ($\beta=0.2, \gamma=18$; $\beta=0.2, \gamma=25$ $\beta=0.4, \gamma=25$), and plastic collapse loads determined using twice elastic compliance are not accurate, and therefore, the corresponding values of F_{AR} are not acceptable. In addition, it can be seen that values of F_{AR} determined from Lu's limit are much larger than that calculated using BS7910 (2013) curve. The surface crack with less than 10% has a small effect on the plastic collapse loads of cracked CHS T/Y-joints. It has a large effect when the dimensionless crack area A_{nc} reaches 25%. Therefore, the BS7910 (2013) curve appears to be very conservative.

5.5.6 Proposed Equation of F_{AR}

A linear line is adopted to represent the lower bound of all values of F_{AR} determined using Lu's limit. The lower bound is expressed as

$$F_{AR} = (1 - 0.350 \frac{A_c}{t_0 l_w}) \quad (5.4)$$

From Fig. 5.18, it can be seen that all values of F_{AR} lie above the proposed linear curve. Therefore, the proposed curve can be safely used to predict the plastic collapse moment of CHS T/Y-joints subjected to axial tensile load within the specified range.

5.6 UNCRACKED CHS T/Y-JOINTS SUBJECTED TO IN-PLANE BENDING

In this section, plastic collapse moments of 22 uncracked CHS T/Y-joints subjected to in-plane bending are studied. As mentioned earlier, length of the brace is intentionally reduced to 400 mm for all the CHS T/Y-joints so as to assure that failure occurs on the chord. Firstly, the effect of the boundary condition and deformation character at the chord-brace intersection zone are investigated. Then, load displacement curves of uncracked CHS T/Y-joints subjected to in-plane bending are discussed. Load displacement curves of uncracked CHS T/Y-joints subjected to in-plane bending are converted to moment rotation curves. Consequently, plastic collapse moments of all the analyzed CHS T/Y-joints are determined. Finally, FE results are compared with values calculated using API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) codes.

5.6.1 Boundary Conditions

Two types of boundary conditions, namely the pinned and the fixed ends discussed in Sub-section 5.4.1 are applied to uncracked CHS T/Y-joints subjected to in-plane bending. Figs. 5.19(a) and (b) show the effect of the boundary conditions on the moment rotation curves. It can be seen that moment rotation curves with pinned boundary condition are slightly lower. It indicates that the boundary condition has a small effect on the bearing capacity of CHS T/Y-joints subjected to in-plane bending. In this study, the pinned boundary condition is adopted for the uncracked CHS T/Y-joints subjected to in-plane bending.

5.6.2 Deformation Characteristic

It is observed that failure typically occurs due to fracture through the chord wall on the tensile side of the brace and plastic bending and buckling of the chord on the compression side for uncracked CHS T/Y-joints subjected to in-plane bending tested in laboratory. Fig. 5.20 shows the typical deformation of a CHS T/Y-joint subjected to in-plane bending. It can be seen that the deformation of the FE model is quite reasonable. Local bulging and indentation occurs at the chord on the tensile and compression sides of the brace respectively. Meanwhile, slight external ovalization at the two chord side-walls is observed. The amplitude of the external ovalization varies for CHS T/Y-joints with different β values, but it is small compared to the magnitudes of local bulging and indentation. The failure modes of all the analyzed CHS T/Y-joints are similar.

5.6.3 Moment Rotation Curve

By examining all the analyzed CHS T/Y-joints subjected to in-plane bending, it is found that the trend of all moment rotation curves is quite similar and they behave as continuously increasing. However, the slope of all moment rotation curves is moderate after excessive yielding occurs. The trend of all moment rotation curves can be observed from Figs. 5.19(a) and (b).

5.6.4 Plastic Collapse Moment

Plastic collapse moments of all the analyzed CHS T/Y-joints are determined from the moment rotation curves. Both twice elastic compliance and Lu's limit are used to determine the plastic collapse moment. Fig. 5.21 shows an example of the application of Lu's limit to determine the plastic collapse moment of a CHS T/Y-joint subjected to in-plane bending. For this case, the value of Lu's limit is 15mm. The deformation values at load increment 28 and 29 are 14.12mm and 16.83mm respectively. The linear interpolation method is used to determine the plastic collapse moment of the joint. For all the analyzed CHS T/Y-joints subjected to in-plane bending, the magnitude of the indentation at the chord on the compression side of the brace is always reached Lu's limit first, and it indicates that the plastic collapse moment of CHS T/Y-joints subjected to in-plane bending is governed by local indentation rather than local bulging on the chord in FE analysis.

Figs. 5.22(a), (b) and (c) show the applications of twice elastic compliance and Lu's limit to determine the plastic collapse loads of three typical CHS T/Y-joints subjected to in-plane bending. It can be seen that plastic collapse moment determined using twice elastic compliance is always reached first as the slope of the moment rotation curve is moderate after excessive yielding occurs at the chord-brace intersection zone. It appears that for CHS T/Y-joints subjected to in-plane bending, the plastic collapse moment determined from twice elastic compliance is acceptable. This is different to CHS T/Y-joints subjected to axial tensile load. This is mainly because the global bending effect is small for CHS T/Y-joints subjected to in-plane bending. As the slope of the moment rotation curve is moderate, the plastic collapse moment determined from twice elastic compliance is not being underestimated significantly comparing to the value determined using Lu's limit.

5.6.5 Results and Discussions

Figs. 5.23(a), (b) and (c) summarize the non-dimensional plastic collapse moments of all the analyzed uncracked CHS T/Y-joints subjected to in-plane bending. The FE results are compared with plastic collapse moments calculated using API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008). It can be seen that plastic collapse moments determined using twice elastic compliance and Lu's limit are within or very close to the data band of the three codes. The plastic collapse moments determined using twice elastic compliance are consistently lower than results determined using Lu's limit, and they appear to be acceptable. However, it is important to recall that the brace length has been intentionally shortened to ensure failure occurs on the chord. Otherwise, twice elastic compliance cannot be used to determine the true plastic collapse moment of the joint if the brace member fails due to bending. The percentage difference between the values determined by using Lu's deformation limit and the average of the three codes is within the range of -25.2% to 75.2%. The percentage difference between the values determined by using twice elastic compliance and the average of the three codes is within the range of -27.1% to 41.2%. The percentage difference among the values calculated from the three codes of practice is within the range of -38.0% (the negative maximum difference over the maximum of the three values calculated using the three codes) to 61.3% (the maximum difference over the minimum of the three values calculated using the three codes). Therefore, it can be concluded that the in-plane plastic collapse moments determined are reliable.

5.7 CRACKED CHS T/Y-JOINTS SUBJECTED TO IN-PLANE BENDING

In this section, plastic collapse moments of cracked CHS T/Y-joints subjected to in-plane bending are studied. Firstly, plastic collapse moments of cracked CHS T/Y-joints subjected to in-plane bending are determined. Then, the strength reduction factor F_{AR} defined in Eq. (5.3) is calculated. Consequently, the effect of various parameters such as crack area, β , γ , and θ are investigated. Finally, a set of F_{AR} equations is suggested based on the FE results.

5.7.1 Strength Reduction Factor F_{AR}

It is found that the trend of moment rotation curves of all the cracked CHS T/Y-joints subjected to in-plane bending are quite similar. Fig. 5.24 shows a set of typical moment rotation curves of a CHS T/Y-joint containing various size surface cracks. The dimensionless crack area A_{nc} is increased from 5% to 25% with an increment of 5%. Deformation limits of twice elastic compliance and Lu's limit are also indicated in Fig. 5.24. It can be seen that for cracked joints with 5%, 10% and 15% crack area, the trend of the moment rotation curves is similar to the uncracked joint, behaving as continuously increasing. However, when the crack area exceeds 20%, the moment rotations curves decreases after excessive yielding, and a peak value is observed.

5.7.2 Effect of θ

Figs. 5.25(a) and (b) show the effect of θ on F_{AR} . It can be seen that values of F_{AR} of CHS Y-joints are consistently lower than CHS T-joints. It indicates that for cracked CHS T/Y-joints subjected to in-plane bending, the effect of the surface crack on CHS T-joints is the most severe.

5.7.3 Effect of β

Figs. 5.26(a), (b) and (c) show the effect of β on F_{AR} . Values of F_{AR} plotted in dash line indicate that plastic collapse moments are determined using twice elastic compliance, whereas values of F_{AR} plotted in solid line indicate that plastic collapse moments are determined using Lu's limit. It can be seen that the distributions of F_{AR} determined from twice elastic compliance are more regular. This is because plastic collapse moments determined using twice elastic compliance are very conservative as yielding is confined

within a certain region of the chord-brace intersection zone when twice elastic compliance limit is reached. It appears that β has no significant effect on F_{AR} determined from twice elastic compliance. In contrast, distributions of F_{AR} determined from Lu's limit are slightly irregular. This is mainly because yielding might spread across the entire chord-brace intersection zone for cracked joints containing large size crack. As mentioned in Sub-section 5.7.1, for joints containing large size crack such as 20% and 25%, the moment rotations curves decrease after excessive yielding, and peak values of moment rotation curves are observed when Lu's limit is reached. Therefore, for CHS T/Y-joints containing large size crack, values of F_{AR} determined from Lu's limit are moved further down in the plots. It implies that F_{AR} determined from twice elastic compliance may not be safe. Overall, β has no significant effect on F_{AR} .

5.7.4 Effect of γ

Figs. 5.27(a), (b) and (c) show the effect of γ on F_{AR} . It can be seen that the distribution of F_{AR} determined from twice elastic compliance is more regular due to the same reasons explained in Sub-section 5.7.3. Values of F_{AR} determined from twice elastic compliance may not be safe particular for joints containing large size crack. Overall, γ has no significant effect on F_{AR} either.

5.7.5 Effect of Crack Area

Figs. 5.28 and 5.29 show the effect of crack area on F_{AR} . It can be seen that crack area has significant effect on F_{AR} . The relationship between the crack area and F_{AR} is almost linear. In order to make a direct comparison, F_{AR} determined from BS7910 (2013) is also incorporated in Figs. 5.28 and 5.29. It should be bear in mind that this F_{AR} originally is only valid for CHS joints subjected to axial tensile load. It can be seen that all F_{AR} data points determined from twice elastic compliance are above the BS7910 (2013) curve. However, most of F_{AR} data points determined from Lu's limit are below the BS7910 (2013) curve.

5.7.6 Proposed Equation of F_{AR}

A linear line is adopted to represent the lower bound of all F_{AR} values. The lower bound is expressed as

$$F_{AR} = (1 - 1.368 \frac{A_c}{t_0 l_w}) \quad (5.5)$$

From Fig. 5.30, it can be seen that all F_{AR} data points determined using Lu's limit lie above the proposed linear curve. Therefore, the proposed curve can be safely used to predict the plastic collapse moment of CHS T/Y-joints subjected to in-plane bending within the specified range.

5.8 UNCRACKED CHS T/Y-JOINTS SUBJECTED TO OUT-OF-PLANE BENDING

In this section, plastic collapse moments of uncracked CHS T/Y-joints subjected to out-of-plane bending is studied. Firstly, the effect of the boundary conditions and deformation characteristic at the chord-brace intersection zone is investigated. After that, moment rotation curves of uncracked CHS T/Y-joints subjected to out-of-plane bending are discussed. Consequently, plastic collapse moments of all the analyzed joints are determined. Finally, FE results are verified against values calculated using the three codes as well as experimental results reported by other researchers.

5.8.1 Boundary Conditions

For consistence and comparison purposes, the same two types of boundary conditions shown in Fig. 5.3(a) and (b) are applied to CHS T/Y-joints subjected to out-of-plane bending again. It is found that the boundary condition has slight effect on the moment rotation curves of CHS T/Y-joints with small β as shown in Fig. 5.31(a). However, for CHS T/Y-joints with larger β such as 0.8 as shown in Fig. 5.31(b), the boundary condition has a significant effect on the moment rotation curves. For CHS T/Y-joints with small β , local buckling is very easy to occur on the chord at the compression side of the brace, and thus overall rotation of the chord due to out-of-plane bending is small. However, for CHS T/Y with larger β such as 0.8, the effect of the overall rotation of the chord is significant when local buckling occurs at the chord on the compression side of the brace. By observing the deformations of a CHS T/Y-joint with pinned and fixed boundary conditions, it is found that the fixed boundary condition is more reasonable. Fig. 5.32 shows the deformations of a CHS T/Y-joint with pinned and fixed boundary conditions. It

can be seen that significant overall rotation at the top half of the chord is observed for the joint with the pinned boundary condition and it is unrealistic. Therefore, the fixed boundary condition is adopted for CHS T/Y-joints subjected to out-of-plane bending.

5.8.2 Deformation Characteristic

For uncracked CHS T/Y-joints subjected to out-of-plane bending, failure typically occurs due to local buckling and fracture of the chord walls on the tensile and the compression side of the brace. Fig. 5.33 shows the typical deformation of a CHS T/Y-joint subjected to out-of-plane bending. It can be seen that the deformation of the FE model is quite reasonable. Local bulging and indentation occurs at the chord on the tensile and compression sides of the brace. The maximum deformation always occurs at the saddle position on the compression side of the brace, and it involves both local indentation and overall rotation of the chord. The deformations of all the analyzed joints are quite similar.

5.8.3 Moment Rotation Curve

The trend of all moment rotation curves of CHS T/Y-joints subjected to out-of-plane bending is quite similar, behaving as continuously increasing. The slope of the moment rotation curve is moderate after excessive yielding occurs. This is similar to CHS T/Y-joints subjected to in-plane bending. The trend of all moment rotation curves can be observed from Figs. 5.31(a) and (b).

5.8.4 Plastic Collapse Moment

Plastic collapse moments of 22 uncracked CHS T/Y-joints subjected to out-of-plane bending are determined from the moment rotation curves. Both twice elastic compliance and Lu's limit are used to determine the plastic collapse moment. In this section, Lu's limit is determined as the maximum deformation of nodes reaching 3% of the chord diameter on the chord. Fig. 5.34 shows an example of the application of Lu's limit. When the maximum displacement of nodes indicated in red reaches 3% of the chord diameter, the out-of-plane bending is taken as the plastic collapse moment of the joint. If Lu's limit is not located exactly in a load increment, the linear interpolation method is used to determine the plastic collapse moment. For CHS Y-joints, one has to check the deformation of nodes around the saddle position of the chord as the maximum deformation may not occur exactly at the middle of the chord. Figs. 5.35 and 5.36 show the local

deformation used to determine Lu's limit. It can be seen that the local indentation not only involves vertical displacement U_X but also rotation U_Y .

Figs. 5.37(a), (b) and (c) show the application of twice elastic compliance and Lu's limit to determine the plastic collapse moments of three typical CHS T/Y-joints subjected to out-of-plane bending. It can be seen that plastic collapse moments determined using twice elastic compliance are reached first for joints with $\beta \leq 0.6$. It appears that for CHS T/Y-joints subjected to out-of-plane bending, plastic collapse moment determined from twice elastic compliance is also acceptable as the slope of the moment rotation curve is moderate.

5.8.5 Results and Discussions

Fig. 5.38 summaries the non-dimensional plastic collapse moments of all the analyzed uncracked CHS T/Y-joints subjected to out-of-plane bending. FE results are compared with plastic collapse moments calculated using API-RP-2A (2005), Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008). It can be seen that FE results in this study are consistently higher than values calculated by the three codes. The percentage difference between the values determined by using Lu's deformation limit and the average of the three codes is within the range of 25.5% to 73.5%. The percentage difference between the values determined by using twice elastic compliance and the average of the three codes is within the range of 20.9% to 50.1%. The percentage difference among the values calculated from the three codes of practice is within the range of -22.4% (the negative maximum difference over the maximum of the three values calculated using the three codes) to 28.8% (the maximum difference over the minimum of the three values calculated using the three codes). In Fig. 5.39, the FE results are further compared with experimental and numerical results reported by other researchers (Lee & Dexter, 1994). Data points labeled as JISC, YURA and TNO are experimental results. It can be seen that FE results in this study are mixed with the data band reported by other researchers. Therefore, it can be concluded that for uncracked CHS T/Y-joints subjected to out-of-plane bending, FE results of plastic collapse moments determined in this study are reliable.

5.9 CRACKED CHS T/Y-JOINTS SUBJECTED TO OUT-OF-PLANE BENDING

In this section, plastic collapse moments of cracked CHS T/Y-joints subjected to out-of-plane bending are studied. Firstly, plastic collapse moments of cracked CHS T/Y-joints subjected to out-of-plane bending are determined. After that, the strength reduction factor F_{AR} defined in Eq. (5.3) is calculated. Consequently, the effect of various parameters such as crack area, β , γ , and θ are investigated. Finally, a set of F_{AR} equations is proposed based on the FE results.

5.9.1 Strength Reduction Factor F_{AR}

Figs. 5.40(a), (b) and (c) show a set of moment rotations curves of cracked CHS T/Y-joints subjected to out-of-plane bending. It can be seen that for cracked joints with 5%, 10% and 15% crack area, the trend of the moment rotation curves are quite similar to the corresponding uncracked joints, behaving as continuously increasing. However, when the crack area exceeds 20%, the moment rotations curves decrease after peak values are observed for joints with $\beta \geq 0.6$. Moment rotation curves of cracked CHS T/Y-joints with $\beta = 0.8$ are terminated earlier in order to save computation cost.

5.9.2 Effect of θ

Figs. 5.41(a) and (b) show the effect of θ on F_{AR} . It can be seen that θ has a small effect on F_{AR} of cracked CHS T/Y-joints subjected to out-of-plane bending.

5.9.3 Effect of β

Figs. 5.42(a), (b) and (c) show the effect of β on F_{AR} . Values of F_{AR} plotted in dash line indicate that plastic collapse moments are determined using twice elastic compliance, whereas values of F_{AR} plotted in solid line indicate that plastic collapse moments are determined using Lu's limit. It can be seen that F_{AR} determined from twice elastic compliance and Lu's limit are quite similar when β is less than 0.6, increasing almost linearly. After β exceeds 0.6, F_{AR} decreases rapidly, particularly for joints containing large size crack.

5.9.4 Effect of γ

Figs. 5.43(a), (b) and (c) show the effect of γ on F_{AR} . It can be seen that the distributions of F_{AR} determined from twice elastic compliance and Lu's limit are similar. It appears that γ has a small effect on F_{AR} if the crack area is less than 15%. After the crack area exceeds 15%, the effect of γ on F_{AR} increases as β increases.

5.9.5 Effect of Crack Area

Figs. 5.44 and 5.45 show the effect of crack area on F_{AR} . It can be seen that crack area has a significant effect on F_{AR} . F_{AR} decreases as crack size increases. The relationship between the crack area and F_{AR} is approximately linear before the crack area exceeds 15%. After the crack area exceeds 15%, F_{AR} decreases rapidly. In order to make a direct comparison, F_{AR} curve plotted using BS7910 (2013) equation is also plotted in Figs. 5.44 and 5.45. It can be seen that the F_{AR} curve of BS7910 (2013) crosses the middle of two data bands of F_{AR} determined using twice elastic compliance and Lu's limit.

5.9.6 Proposed Equation of F_{AR}

A linear line is adopted to represent the lower bound of all F_{AR} s. The lower bound is expressed as

$$F_{AR} = (1 - 1.450 \frac{A_c}{t_0 l_w}) \quad (5.6)$$

From Figs. 5.46 and 5.47, it can be seen that all F_{AR} values lie above the proposed linear curve. Therefore, the proposed curve can be safely used to predict the plastic collapse moment of CHS T/Y-joints subjected to out-of-plane bending within the specified range.

Fig. 5.1 True stress-strain data used in finite element analysis

Fig. 5.2 Geometry and notations of a CHS T/Y-joint subjected to three basic loading

(a) The pinned boundary conditions

(b) The fixed boundary conditions

Fig. 5.3 Boundary conditions of FE models

Fig. 5.4 Effect of the boundary condition

(a) $\alpha=18$, $\beta=0.2$, $\gamma=25$

(b) $\alpha=18$, $\beta=0.6$, $\gamma=18$

Fig. 5.5 Deformation characteristic of CHS T/Y-joints (DSF=5, $P_a/P_c \approx 1$)

Fig. 5.6 Two representative load displacement curves of uncracked CHS T/Y-joints

Fig. 5.7 Lu's deformation limit ($\alpha=18$, $\beta=0.6$, $\gamma=18$, $DSF=5$, $P_a/P_c \approx 1$)

Fig. 5.8 Determination of Lu's limit ($\alpha=18$, $\beta=0.6$, $\gamma=25$, $\theta=60^\circ$)

(a)

(b)

Fig. 5.9 Non-dimensional plastic collapse loads determined using twice elastic compliance and Lu's limit

 (a) $\gamma=10$

(b) $\gamma=18$

(c) $\gamma=25$

Fig. 5.10 Non-dimensional plastic collapse load versus β

Fig. 5.11 Effect of chord length on the load displacement curve
($\beta=0.8$, $\gamma=10$, $\theta=90^\circ$)

(a) $\alpha=18$, $\beta=0.2$, $\gamma=25$, $\theta=90^\circ$

(b) $\alpha=18, \beta=0.2, \gamma=25, \theta=90^\circ$

Fig. 5.12 Typical load displacement curves of cracked CHS T/Y-joints subjected to axial tensile load

(a) $\alpha=18, \beta=0.4, \gamma=18, \theta=90^\circ$

(b) $\alpha=18$, $\beta=0.6$, $\gamma=18$, $\theta=90^\circ$

Fig. 5.13 Effect of θ on F_{AR}

(a) $\gamma=10$, $\theta=90^\circ$

(b) $\gamma=18, \theta=90^\circ$

(c) $\gamma=25, \theta=90^\circ$

Fig. 5.14 Effect of β on F_{AR}

(a) $\beta=0.4, \theta=90^\circ$

(b) $\beta=0.6, \theta=90^\circ$

(c) $\beta=0.8, \theta=90^\circ$

Fig. 5.15 Effect of γ on F_{AR}

Fig. 5.16 Effect of crack area on F_{AR} determined using twice elastic compliance

 Fig. 5.17 Effect of crack area on F_{AR} determined using Lu's limit

 Fig. 5.18 Proposed lower bound of F_{AR} determined using Lu's limit

(a)

(b)

Fig. 5.19 Effect of the boundary condition

Fig. 5.20 Typical failure mode of CHS T/Y-joints subjected to in-plane bending ($\alpha=18$, $\beta=0.4$, $\gamma=18$, $\theta=90^\circ$, $DSF=10$, $M_{ai}/M_{ci}\approx 1$)

Fig. 5.21 Determination of Lu's limit ($\alpha=18$, $\beta=0.6$, $\gamma=25$, $\theta=90^\circ$)

(a)

(b)

(c)

Fig. 5.22 Non-dimensional plastic collapse moments determined using twice elastic compliance and Lu's limit

 (a) $\gamma=10$

(b) $\gamma=18$ (c) $\gamma=25$ Fig. 5.23 Non-dimensional plastic collapse moment versus β

Fig. 5.24 Typical moment rotation curves of cracked CHS T/Y-joints subjected to in-plane bending ($\alpha=18$, $\beta=0.4$, $\gamma=18$, $\theta=90^\circ$)

(a) $\alpha=18$, $\beta=0.4$, $\gamma=18$

(b) $\alpha=18$, $\beta=0.8$, $\gamma=10$

Fig. 5.25 Effect of θ on F_{AR}

(a) $\gamma=10$, $\theta=90^\circ$

(b) $\gamma=18, \theta=90^\circ$

(c) $\gamma=25, \theta=90^\circ$

Fig. 5.26 Effect of β on F_{AR}

(a) $\beta=0.4$, $\theta=90^\circ$

(b) $\beta=0.6$, $\theta=90^\circ$

(c) $\beta=0.8, \theta=90^\circ$

Fig. 5.27 Effect of γ on F_{AR}

Fig. 5.28 Effect of crack area on F_{AR} determined using twice elastic compliance

Fig. 5.29 Effect of crack area on F_{AR} determined using Lu's limit

Fig. 5.30 Proposed lower bound of all F_{AR}

(a) $\alpha=18, \beta=0.2, \gamma=18, \theta=90^\circ$

(b) $\alpha=18, \beta=0.8, \gamma=18, \theta=90^\circ$

Fig. 5.31 Effect of boundary conditions on moment rotation curves

Fig. 5.32 Deformations of a CHS T/Y-joint applied the pinned and the fixed boundary condition ($\alpha=18$, $\beta=0.8$, $\gamma=25$, $\theta=90^\circ$, $DSF=5$, $M_{ao}/M_{co}\approx 1$)

Fig. 5.33 Typical failure mode of CHS T/Y-joints subjected to out-of-plane bending ($\alpha=18$, $\beta=0.4$, $\gamma=18$, $\theta=90^\circ$, $DSF=5$, $M_{ao}/M_{co}\approx 1$)

Fig. 5.34 Nodes used to determine Lu's limit of CHS T-joints
 $(\alpha=18, \beta=0.4, \gamma=18, \theta=90^\circ, \text{DSF}=5, M_{ao}/M_{co} \approx 1)$

Fig. 5.35 Determination of Lu's limit of CHS T-joints subjected to out-of-plane bending

Fig. 5.36 Amplified zone A in Fig.5.35

(a)

(b)

(c)

Fig. 5.37 Plastic collapse moments determined using twice elastic compliance and Lu's limit

(a) $\gamma=10$

(b) $\gamma=18$

(c) $\gamma=25$

Fig. 5.38 Non-dimensional plastic collapse moment versus β

Fig. 5.39 FE results compared with experimental and numerical data reported by other researchers

(a) $\alpha=18, \beta=0.4, \gamma=18, \theta=90^\circ$

(b) $\alpha=18, \beta=0.6, \gamma=18, \theta=90^\circ$

(c) $\alpha=18, \beta=0.8, \gamma=18, \theta=90^\circ$

Fig. 5.40 Typical moment rotation curves of cracked CHS T/Y-joints subjected to out-of-plane bending

(a) $\alpha=18, \beta=0.2, \gamma=18$

(b) $\alpha=18$, $\beta=0.6$, $\gamma=18$

Fig. 5.41 Effect of θ on F_{AR}

(a) $\gamma=10$, $\theta=90^\circ$

(b) $\gamma=18, \theta=90^\circ$ (c) $\gamma=25, \theta=90^\circ$ Fig. 5.42 Effect of β on F_{AR}

(a) $\beta=0.4$, $\theta=90^\circ$

(b) $\beta=0.6$, $\theta=90^\circ$

(c) $\beta=0.8, \theta=90^\circ$

Fig. 5.43 Effect of γ on F_{AR}

Fig. 5.44 Effect of crack area on F_{AR} determined using twice elastic compliance

 Fig. 5.45 Effect of crack area on F_{AR} determined using Lu's limit

 Fig. 5.46 Proposed lower bound of all F_{AR} determined using twice elastic compliance

Fig. 5.47 Proposed lower bound of all F_{AR} determined using Lu's limit

CHAPTER 6

PLASTIC COLLAPSE LOADS OF CRACKED MULTI-PLANAR CHS TT-JOINTS

6.1 INTRODUCTION

Previous research works focus mainly on the study of the compressive static strength of the uncracked multi-planar CHS TT-joints. Scola (Scola, 1990) carried out experimental test on 7 uncracked multi-planar CHS TT-joints subjected to compressive loads acting on two brace ends. The two braces and the chord were free to rotate as they were pinned supports. All the tested joints failed by chord plastification shortly after the peak load was reached. The peak load was taken as the ultimate strength of the joint. Paul (Paul, 1992) carried out similar experimental tests on 12 uncracked multi-planar CHS TT-joints. Plates were welded on the braces and chord ends using fillet welds. The end plates welded to the braces were bolted to two hinges, which were then bolted to a stiffened beam. Therefore, the ends of the two braces were free to rotate. On the other hand, the end plates welded to the chord ends were bolted to a frame preventing the chord ends from free rotation. Similar chord plastic failure modes reported by Scola (Scola, 1990) were observed.

Research work related to the plastic collapse load/moment of cracked multi-planar CHS TT-joints containing a surface crack is very limited. In this chapter, the plastic collapse load/moment of cracked multi-planar CHS TT-joints containing a surface crack located at the hot spot stress location of the chord is investigated. Four types of load shown in Figs. 6.1(a) – (d) are taken into account. The entire chapter is divided into four parts. In every part, plastic collapse load/moment of cracked multi-planar CHS TT-joints subjected to each type of load is studied.

6.2 BOUNDARY CONDITIONS AND MATERIAL PROPERTIES

Figs. 6.2(a), (b), (c) and (d) show the boundary conditions of multi-planar CHS TT-joints subjected to different loads. The boundary conditions for multi-planar CHS TT-joints are determined carefully with reference to the uni-planar CHS T/Y-joints described in Chapter 5, and the experimental tests carried out by Scola (Scola, 1990) and Paul (Paul, 1992). It

can be seen that only several nodes on the two chord ends are constrained to simulate a pinned boundary condition. For multi-planar CHS TT-joints subjected to axial T-T or T-C load, the free ends of the two braces are constrained to prevent any possible lateral movement, and only displacements at the axial direction of the two braces are allowed. This is different to CHS T/Y-joints in which the brace end is free to move laterally. For multi-planar CHS TT-joints subjected to in-plane and out-of-plane bending, the two brace ends are free to undergo lateral movement. It is exactly the same as the uni-planar CHS T/Y-joints. For multi-planar CHS TT-joints subjected to out-of-plane bending, the chord ends are pinned as the overall rotation of the chord is prevented.

For multi-planar CHS TT-joints subjected to axial T-T or T-C load, it is necessary to investigate the effect of the brace end constraint on the plastic collapse load. It is expected that the brace end constraint may increase the plastic collapse load of the joint. Therefore, the two braces should be sufficiently long to minimize the effect of the brace end constraint. Figs. 6.3(a) and (b) show the effect of the brace end constraint. It can be seen that the brace length has no effect on the load displacement curves for multi-planar CHS TT-joints subjected to axial T-T load without any constraint at the free ends of the two braces. For braces under compressive load without any constraint at the ends of the two braces, the load displacement curve is not realistic. Figs. 6.4(a) and (b) show two types of unrealistic deformation of multi-planar CHS TT-joints for such cases.

For braces under compressive load with lateral constraint, load displacement curves almost coincide with each other. It implies that the brace length has a minor influence on the plastic collapse load of multi-planar CHS TT-joints subjected to axial compressive load with lateral constraint at the ends of the two braces. For braces under tensile load with lateral constraint, it can be seen that the brace length may have significant effect on the load displacement curves ($\alpha=18$, $\beta=0.3$, $\gamma=25$, $\phi=60^\circ$). The load displacement curves tend to converge to the corresponding curve determined from braces under tensile load without any lateral constraint when the brace length is sufficiently long.

6.3 GEOMETRY SCOPE OF PARAMETRIC STUDY

Fig. 6.5 shows the geometrical parameters and several critical positions of a typical multi-planar CHS TT-joint. In the parametric study, the following ranges are specified:

$$0.3 \leq \beta \leq 0.5$$

$$10 \leq \gamma \leq 25$$

$$60^\circ \leq \varphi \leq 90^\circ$$

$$0 \leq A_{nc} \leq 25\%$$

$$0.2 \leq a/t_0 \leq 0.8$$

In practice, the maximum β is 0.6 for multi-planar CHS TT-joints ($\varphi = 90^\circ$) without any overlap. As there are many transition element layers in the mesh models of cracked multi-planar CHS TT-joints, and thus, the maximum β is taken as 0.5 in this study. The range of γ and A_{nc} is the same as those of CHS T/Y-joints described in Chapter 5. The crack depth ratio, $a/t_0 = 0.2$ is not considered in the FE analysis because it is found that the analyses take much time to converge. When A_{nc} reaches 5%, 15% and 25%, the corresponding crack depth ratio is 0.4, 0.6 and 0.8, following the rule that the crack area increases as the crack depth increases. The range of φ is given according to Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008). A total of 6 uncracked multi-planar CHS TT-joints subjected to different loads are investigated in this study. Considering the brace end constraint effect, the dimensions of the 6 uncracked multi-planar CHS TT-joints are given in Table 6.1. The braces are sufficiently long so that the brace end constraint effect can be ignored for multi-planar CHS TT-joints subjected to axial T-T or T-C load. For multi-planar CHS TT-joints subjected to in-plane or out-of-plane bending, the brace length is reduced to ensure that failure occurs on the chord as those of uni-planar CHS T/Y-joints described in Chapter 5.

Fatigue crack is prone to initiate from the hot spot stress region of any CHS joint. The hot spot stress location is dependent on the loading for different multi-planar CHS TT-joints, and it will be discussed individually in each relevant section.

6.4 UNCRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO AXIAL T-T LOAD

In this section, the plastic collapse load of uncracked multi-planar CHS TT-joints subjected to axial T-T load (Fig. 6.1(a)) is investigated. Firstly, the crack location of 6 multi-planar CHS TT-joints subjected to axial T-T load is investigated. Then, load displacement curves of 6 uncracked multi-planar CHS TT-joints subjected to axial T-T load are discussed. Consequently, plastic collapse loads of all the analyzed multi-planar

CHS TT-joints are determined. Finally, FE results are compared to values calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) codes of practice.

6.4.1 Crack Position

Figs. 6.6(a) - (f) show the von-Mises stress distribution at the chord-brace region of 6 multi-planar CHS TT-joints subjected to axial T-T load. In Chapter 5, the hot spot stress location of uni-planar CHS T/Y-joints is determined by using the in-house program developed by Huang (Huang, 2003) and CIDECT (Wardenier et al., 2008) code, and it is found that the maximum Von-misses stress at the weld toe is overlapping with the hot spot stress. Therefore, the Von-misses stress is used to determine the crack position for cracked multi-planar CHS TT-joints. From Figs. 6.6(a)-(f), it can be seen that the hot spot stress location is flexible along the weld toe. The crack positions of 6 multi-planar CHS TT-joints subjected to axial T-T load are tabulated in Table 6.2.

6.4.2 Deformation Characteristic

Figs. 6.7(a) - (f) show the deformation of 6 uncracked multi-planar CHS TT-joints subjected to axial T-T load. It can be seen that the deformation can be categorized into two groups depending on the angle ϕ . For cases TT2, TT4, TT5 and TT6 ($\phi=90^\circ$), a slight local indentation is generally observed at the region between two braces. For cases TT1 and TT3 ($\phi=60^\circ$), there is no such local indentation.

6.4.3 Load Displacement Curve

Fig. 6.8 shows load displacement curves of 6 uncracked multi-planar CHS TT-joints subjected to axial T-T load. The displacement mentioned here refers to the spatial movement of the brace end along the brace direction. All the load displacement curves behave as continuously increasing without showing a peak load. This is similar to the load displacement curves of CHS T/Y-joints subjected to axial tensile load. Load displacement curves shown in Fig. 6.8 can be divided into two groups according to the γ value. The first group includes cases TT1, TT2 and TT5 ($\gamma=10$), and the second group includes cases TT3, TT4 and TT6 ($\gamma=25$). It can be seen that the angle ϕ has significant influence on the load displacement curves of multi-planar CHS TT-joints. However, the effect of ϕ on the plastic collapse load of uncracked multi-planar CHS TT-joint is not incorporated in the

current versions of Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) codes of practice.

6.4.4 Plastic Collapse Load

For uncracked multi-planar CHS TT-joints subjected to axial T-T load, the deformation at the chord-brace region (Figs. 6.7(a)-(f)) is quite different compared to the uni-planar CHS T/Y-joints without showing apparent local indentation or bulging. Therefore, it is very difficult to calculate the Lu's deformation (Lu et al., 1994) at this region. One has to resort to the original definition of Lu's deformation limit. In Lu's publication (Lu et al., 1994), $3\%d_0$ was proposed based on the statistical analysis on the load displacement curves of different types of hollow section joints including plate to CHS and RHS column connection, I-beam to CHS and RHS column connection, axially loaded RHS X- and T-joints and moment loaded X-joints in CHS and RHS members. It was found that a peak load is generally obtained when a local indentation of the chord face at the intersection region reaches to $2.5\% - 4.0\%d_0$ (b_0). Based on this observation, Lu et al. (Lu et al., 1994) proposed that the applied load when the local deformation of the chord reaches $3\%d_0$ can be taken as the ultimate strength, which is identical to the plastic collapse load of this study. In order to determine the plastic collapse loads of uncracked multi-planar CHS TT-joints subjected to axial T-T load conveniently, the corresponding load displacement curves of the same uncracked multi-planar CHS TT-joints subjected to axial compressive-compressive (C-C) load are calculated in this study. By doing so, the calculation of Lu's $3\%d_0$ deformation limit for multi-planar CHS TT-joints subjected to axial T-T load is avoided completely.

Fig. 6.9(a) shows the load displacement curves of 3 uncracked multi-planar CHS TT-joints in group 1 ($\gamma=10$) subjected to axial T-T or C-C load. It can be seen that only load displacement curve of TT2 subjected to axial C-C load shows a peak load. The displacement corresponding to the peak load is taken as the deformation limit of all load displacement curves in Fig. 6.9(a) as the dimensions of the chords of TT1, TT2, and TT5 are the same.

Figs. 6.9(b) shows the load displacement curves of 3 uncracked multi-planar CHS TT-joints in group 2 ($\gamma=25$) subjected to axial T-T or C-C load. It can be seen that load displacement curves of TT3, TT4 and TT6 subjected to axial C-C load show peak loads.

The displacements corresponding to the peak loads are varied. As the chord dimensions of TT3, TT4 and TT6 are the same, the smallest displacement corresponding to 3 peak loads is taken as the deformation limit for TT3, TT4 and TT6 in this study. In order to distinguish from Lu's deformation limit ($3\%d_0$), deformation limit used in this section is simply denoted as 'deformation limit'.

Table 6.3 summarizes the non-dimensional plastic collapse loads of 6 uncracked multi-planar CHS TT-joints subjected to axial tensile-tensile load. The FE results are compared with the plastic collapse loads calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) as shown in Fig. 6.10. Similar to the uncracked uni-planar CHS T/Y-joints subjected to axial tensile load, chord bending has significant effect on the load displacement curve of the uncracked multi-planar CHS TT-joints. Therefore, the twice elastic compliance is also not suitable for determining the plastic collapse load of uncracked multi-planar CHS TT-joints subjected to axial tensile-tensile load. It can be seen that the plastic collapse loads determined by using Lu's deformation limit is much closer to the strength curves of two codes.

6.5 CRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO AXIAL T-T LOAD

In this section, plastic collapse load of cracked multi-planar CHS TT-joints subjected to axial T-T load is studied. Firstly, plastic collapse loads of 18 cracked multi-planar CHS TT-joints subjected to axial T-T load are determined. Secondly, the strength reduction factors F_{AR} are calculated using the following equation:

$$F_{AR} = \frac{\text{Plastic collapse load / moment of cracked multi-planar CHS TT-joint}}{\text{Plastic collapse load / moment of uncracked multi-planar CHS TT - joint}} \quad (6.1)$$

Finally, the lower bound of all F_{AR} values is proposed based on the FE results.

6.5.1 Load Displacement Curve

Figs. 6.11(a) - (f) show the load displacement curves of all cracked multi-planar CHS TT-joints investigated in this study. It is found that the surface crack has similar influence on the plastic collapse loads of multi-planar CHS TT-joints as compared to the uni-planar CHS T/Y-joints. All the load displacement curves behave as continuously increasing even

if when the surface crack area reaches 25%. It is also observed that load displacement curves of cracked multi-planar CHS TT-joints may terminate earlier as compared to the cracked uni-planar CHS T/Y-joints containing the same size surface crack. It implies that the tri-axial stress states at the crack tips of cracked multi-planar CHS TT-joints subjected to axial T-T load is much more complex and severe which may cause the convergence of the FE analysis difficult.

6.5.2 Effect of Crack Area

The plastic collapse load linearly decreases with the crack size increases. It appears that the surface crack may have more severe influence on the plastic collapse loads of cracked multi-planar CHS TT-joints subjected to axial T-T load comparing to the cracked uni-planar CHS T/Y-joints. This can be observed clearly from Fig. 6.12, and there are several points of F_{AR} ($A_{nc} = 25\%$) fall below the proposed lower bound of the cracked uni-planar CHS T/Y-joints subjected to axial tensile load.

6.5.3 Proposed Equation of F_{AR}

The lower bound of all F_{AR} values for cracked multi-planar CHS TT-joints subjected to axial T-T load is expressed as

$$F_{AR} = \left(1 - 0.518 \frac{A_c}{t_0 l_w} \right) \quad (6.2)$$

From Fig. 6.13, it can be seen that all F_{AR} data points lie above the proposed linear curve. Therefore, the proposed curve can safely be used to predict the plastic collapse moment of multi-planar CHS TT-joints subjected to axial T-T load within the specified range.

6.6 UNCRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO IN-PLANE BENDING

In this section, plastic collapse moment of uncracked multi-planar CHS TT-joints subjected to in-plane bending (Fig. 6.1(b)) is studied. As mentioned earlier, length of the brace is intentionally reduced for all the 6 multi-planar CHS TT-joints so as to ensure that failure occurs on the chord. Firstly, the crack location of multi-planar CHS TT-joints subjected to in-plane bending is investigated. Then, load displacement curves of 6 multi-planar CHS TT-joints subjected to in-plane bending are discussed. Consequently, in-plane

plastic collapse moments of all the analyzed multi-planar CHS TT-joints are determined. Finally, FE results are compared to values calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) codes of practice.

6.6.1 Crack Position

Figs. 6.14(a) - (f) show the von-Mises stress distribution at the chord-brace region of 6 multi-planar CHS TT-joints subjected to in-plane bending. It can be seen that the hot spot stress location is generally located between the crown and the inside saddle. The crack positions of 6 multi-planar CHS TT-joints subjected to in-plane bending are tabulated in Table 6.4.

6.6.2 Deformation Characteristics

It is observed that the failure mode of uncracked multi-planar CHS TT-joints subjected to in-plane bending is quite similar to the uncracked uni-planar CHS T-joints. Fig. 6.15 shows the typical deformation of the uncracked multi-planar CHS TT-joint subjected to in-plane bending. It can be seen that local bulging and indentation occurs at the chord on the tensile and compression sides of the brace respectively. Meanwhile, slight external ovalization at the two chord side-walls is observed. The amplitude of the local indentation is always larger than the local bulging at the same loading level.

6.6.3 Moment Rotation Curve

By examining all the uncracked multi-planar CHS TT-joints subjected to in-plane bending, it is found that the trend of all moment rotation curves is continuously increasing, and the slope of them is moderate after excessive gross yielding. The trend of moment rotation curves of uncracked multi-planar CHS TT-joints is similar to the uncracked uni-planar CHS T/Y-joints as observed from Fig. 6.16. It is also found that the angle ϕ between two braces has no effect on the moment rotation curves of uncracked multi-planar CHS TT-joints subjected to in-plane bending. This can be observed from moment rotation curves of TT1 ($\phi=60^\circ$), TT2 ($\phi=90^\circ$), TT3 ($\phi=60^\circ$) and TT4 ($\phi=90^\circ$).

6.6.4 Plastic Collapse Moment

Plastic collapse moments of all the 6 uncracked multi-planar CHS TT-joints are determined from the moment rotation curves. As the deformation at the chord-brace

intersection zone of uncracked multi-planar CHS TT-joints is quite similar to the uncracked uni-planar CHS T-joints, Lu's deformation limit can be used to determine the in-plane plastic collapse moment. Fig. 6.17 illustrates the determination of Lu's deformation limit of multi-planar CHS TT-joints subjected to in-plane bending in this study. The relative displacement of two points shown in Fig. 6.17 is defined as the Lu's deformation, and it is calculated at each load increment. When the relative displacement reaches $3\%d_0$, the corresponding load applied is taken as the plastic collapse load of the multi-planar CHS TT-joints subjected to in-plane bending. Linear interpolation method is used to determine the plastic collapse load when the Lu's deformation limit of $3\%d_0$ is reached in between the two load increments.

Figs. 6.18(a) - (f) illustrate the determination of the in-plane plastic collapse moments of 6 uncracked multi-planar CHS TT-joints using twice elastic compliance (TEC) and Lu's deformation limit. It can be seen that plastic collapse moments determined using twice elastic compliance is always reached earlier because the slope of all moment rotation curves is moderate after excessive gross yielding. Plastic collapse moments of TT1 and TT2 determined from Lu's deformation limit are not available as the chord is thick ($\gamma=10$) and local indentation is hard to develop.

Table 6.5 summarizes the non-dimensional in-plane plastic collapse moments of 6 uncracked multi-planar CHS TT-joints investigated in this section, and they are compared to the values calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) as shown in Fig. 6.19. It can be concluded that plastic collapse moments of uncracked multi-planar CHS TT-joints determined from twice elastic compliance and Lu's deformation limit are reliable.

6.7 CRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO IN-PLANE BENDING

In this section, the plastic collapse moment of cracked multi-planar CHS TT-joints subjected to in-plane bending is studied. Firstly, plastic collapse moments of 18 cracked multi-planar CHS TT-joints subjected to in-plane bending are determined. Then, the corresponding strength reduction factors F_{AR} are calculated. Finally, the lower bound of all F_{AR} values is suggested based on the FE results.

6.7.1 Moment Rotation Curve

It is found that the trend of moment rotation curves of all the cracked multi-planar CHS TT-joints subjected to in-plane bending is quite similar to the cracked uni-planar CHS T/Y-joints as observed from Figs. 20(a) - (f). It can be seen that for cracked multi-planar CHS TT-joints with 5% and 15% crack area, the trend of the moment rotation curves is similar to the uncracked joint, behaving as continuously increasing. However, when the crack area reaches 25%, the moment rotations curves may decrease after excessive gross yielding (TT6), and thus a peak value is observed.

6.7.2 Effect of Crack Area

Fig. 6.21 shows the effect of the crack area on F_{AR} . It can be seen that the crack area has significant effect on F_{AR} . F_{AR} can decrease to 0.677 when the crack area reaches to 25%. All data points of F_{AR} lie above the proposed lower bound for cracked uni-planar CHS T/Y-joints subjected to in-plane bending.

6.7.3 Proposed Equation of F_{AR}

The lower bound of all F_{AR} values for cracked multi-planar CHS TT-joints subjected to in-plane bending is expressed as

$$F_{AR} = \left(1 - 1.333 \frac{A_c}{t_0 l_w} \right) \quad (6.3)$$

From Fig. 6.22, it can be seen that all F_{AR} data points lie above the proposed linear curve. Therefore, the proposed curve can safely be used to predict the plastic collapse moment of cracked multi-planar CHS TT-joints subjected to in-plane bending within the specified range.

6.8 UNCRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO OUT-OF-PLANE BENDING

In this section, the plastic collapse moment of uncracked multi-planar CHS TT-joints subjected to out-of-plane bending (Fig. 6.1(c)) is studied. Firstly, the crack location of multi-planar CHS TT-joints subjected to out-of-plane bending is investigated. Then,

moment rotation curves of 6 uncracked multi-planar CHS TT-joints subjected to out-of-plane bending are discussed. Consequently, out-of-plane plastic collapse moments of all the analyzed multi-planar CHS TT-joints are determined. Finally, FE results are compared to values calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) codes of practice.

6.8.1 Crack Position

For multi-planar CHS TT-joints subjected to out-of-plane bending shown in Fig. 6.1(c), the surface crack is located at the inside saddle of the chord. Fig. 6.23 shows the typical von-Mises stress distribution at the chord-brace region of multi-planar CHS TT-joints subjected to out-of-plane bending. It can be seen that it is reasonable to locate the surface crack at the inside saddle of the chord.

6.8.2 Deformation Characteristic

For uncracked multi-planar CHS TT-joints subjected to out-of-plane bending, the failure mode is quite similar to that of uncracked uni-planar CHS T-joints subjected to out-of-plane bending. Failure typically occurs due to local buckling and fracture of the chord walls on the compression and the tensile side of the brace. Fig. 6.24 shows the typical deformation of a multi-planar CHS TT-joint subjected to out-of-plane bending. It is important to recognize that the overall rotation of the chord is removed for multi-planar CHS TT-joints subjected to out-of-plane bending. This is different to the uni-planar CHS T/Y-joints subjected to out-of-plane bending.

6.8.3 Moment Rotation Curve

The trend of all moment rotation curves of multi-planar CHS TT-joints subjected to out-of-plane bending is quite similar, behaving as continuously increasing. The slope of the moment rotation curve is moderate after excessive gross yielding. This is similar to the uni-planar CHS T/Y-joints subjected to out-of-plane bending. The angle ϕ has a significant effect on the moment rotation curves of multi-planar CHS TT-joints subjected to out-of-plane bending. The trend of all moment rotation curves can be observed from Fig. 25.

6.8.4 Plastic Collapse Moment

Plastic collapse moments of 6 uncracked multi-planar CHS TT-joints subjected to out-of-plane bending are determined from the moment rotation curves. As the deformation at the chord-brace intersection zone of uncracked multi-planar CHS TT-joints is quite similar to the uncracked uni-planar CHS T-joints, Lu's deformation limit can be used to determine the out-of-plane plastic collapse moment. The displacement of the outside saddle point is defined as the Lu's deformation. When it reaches $3\%d_0$, the corresponding out-of-plane moment is taken as the plastic collapse moment of the multi-planar CHS TT-joints. Linear interpolation method is used to determine the plastic collapse load when the Lu's deformation limit of $3\%d_0$ is reached in between the two load increments.

Figs. 6.26(a) - (f) illustrate the determination of the plastic collapse moments of 6 uncracked multi-planar CHS TT-joints subjected to out-of-plane bending using twice elastic compliance and Lu's deformation limit. The non-dimensional out-of-plane bending moments of 6 uncracked multi-planar CHS TT-joints are summarized in Table 6.6. The non-dimensional out-of-plane bending moments are compared to the values calculated using Eurocode 3 (2005) and CIDECT (Wardenier et al., 2008) as shown in Fig. 6.27. It can be concluded that the out-of-plane plastic collapse moments of uncracked multi-planar CHS TT-joints determined using the FE results are reliable.

6.9 CRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO OUT-OF-PLANE BENDING

In this section, the plastic collapse moment of cracked multi-planar CHS TT-joints subjected to out-of-plane bending is studied. Firstly, plastic collapse moments of 6 cracked multi-planar CHS TT-joints subjected to out-of-plane bending are determined. After that, the strength reduction factors F_{AR} defined in Eq. (6.1) are calculated. Finally, the lower bound of all F_{AR} values is suggested based on the FE results.

6.9.1 Moment Rotation Curve

Figs. 6.28(a) - (f) show moment rotations curves of 18 cracked multi-planar CHS TT-joints subjected to out-of-plane bending. It can be seen that for cracked joints with 5% and 15% crack area, the trend of the moment rotation curves are quite similar to the corresponding uncracked joints, behaving as continuously increasing. However, when the crack area

reaches to 25%, the moment rotations curve of TT6 decreases after a peak value is observed.

6.9.2 Effect of Crack Area

Fig. 29 shows the effect of crack area on F_{AR} . It can be seen that crack area has a significant effect on F_{AR} . F_{AR} decreases as crack size increases. All data points of F_{AR} lie above the proposed lower bound for cracked uni-planar CHS T/Y-joints subjected to out-of-plane bending.

6.9.3 Proposed Equation of F_{AR}

The lower bound of all F_{AR} values for cracked multi-planar CHS TT-joints subjected to out-of-plane bending is expressed as

$$F_{AR} = \left(1 - 1.220 \frac{A_c}{t_0 J_w} \right) \quad (6.4)$$

From Fig. 6.30, it can be seen that all data points of F_{AR} for multi-planar CHS TT-joints lie above the proposed lower bound. Therefore, the proposed curve can safely be used to predict the plastic collapse moment of multi-planar CHS TT-joints subjected to out-of-plane bending within the specified range.

6.10 UNCRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO AXIAL T-C LOAD

In this section, firstly, the crack position of multi-planar CHS TT-joints subjected to axial T-C load is introduced. After that, the deformation characteristic at the chord brace intersection zone of multi-planar CHS TT-joints is discussed. Finally, load displacement curves of 6 uncracked multi-planar CHS TT-joints subjected to axial T-C load (Fig. 6.1(d)) are presented.

6.10.1 Crack Position

For multi-planar CHS TT-joints subjected to axial T-C load shown in Fig. 6.1(d), fatigue crack is prone to initiate at the inside saddle position of the tensile brace. Fig. 6.31 shows typical von-Mises stress distribution at the chord-brace region of multi-planar CHS TT-joints subjected to axial T-C load. It can be seen that the maximum stress is located at the

saddle position. Therefore, it is reasonable to locate the surface crack at the inside saddle position of the brace under tension.

6.10.2 Deformation Characteristic

Figs. 6.32(a) - (f) show the deformation of 6 uncracked multi-planar CHS TT-joints subjected to axial T-C load. It can be seen that the deformation at the chord-brace zone is far more complex as compared to the CHS TT-joints subjected to axial T-T load because there is lack of any symmetrical plane. Generally, local indentation is observed around the chord region welded with the brace under compressive load. The chord region welded to the brace under tensile load tends to detach from the chord.

6.10.3 Load Displacement Curve

It is very difficult to apply Lu's deformation limit directly to determine the plastic collapse of uncracked multi-planar CHS TT-joints subjected to axial T-C load as the deformation at the chord-brace zone is very complex. Again, the original definition of Lu's deformation limit can be used to estimate the plastic collapse load of uncracked multi-planar CHS TT-joints subjected to axial T-C load. Figs. 6.33(a) - (f) show load displacement curves of 6 uncracked multi-planar CHS TT-joints subjected to axial T-C load. For each joint, two load displacement curves determined from the compressive and tensile braces are plotted together. The plastic collapse load determined from the tensile brace may be obtained from the deformation limit corresponding to the peak load of the load displacement curve determined from the compressive brace. In this section, it is not necessary to give the plastic collapse loads of 6 uncracked multi-planar CHS TT-joints. It is because the surface crack has a slight influence on the plastic collapse load.

6.11 CRACKED MULTI-PLANAR CHS TT-JOINTS SUBJECTED TO AXIAL T-C LOAD

In this section, plastic collapse load of cracked multi-planar CHS TT-joints subjected to axial T-C load is studied. Figs. 6.34(a) - (f) show the load displacement curves of 18 cracked multi-planar CHS TT-joints subjected to axial T-C load. Firstly, it can be seen that the surface crack has minor effect on multi-planar CHS TT-joints as 4 load displacement curves for each joint are almost coincide with each other. Secondly, it can be seen that load displacement curves of cracked multi-planar CHS TT-joint are slightly higher than the

corresponding uncracked joint and it appears to be physically impossible. In order to give the explanations, it is necessary to investigate the stress strain state at the crack front as well as at the region around it.

It is found that the brace under compressive load tends to close the surface crack. For case TT3 ($A_{nc}=25\%$), the two crack surfaces are in contact with each other around the crack front. In this situation, it is necessary to define the contact surface between two crack surfaces in the FE analysis. Otherwise, the FE analysis cannot converge. Fig. 6.35 shows the deformation at the crack front of case TT3 ($A_{nc}=25\%$). The crack closure caused by the compressive brace may explain why the surface crack has minor effect on the load displacement curves determined from the tensile brace.

It is also found that the stress distribution of the chord at the middle of the two braces changes due to the surface crack. Fig. 6.36 shows the elements enlarged at this region. It is found that the magnitude of the von-Mises stress at this region increases after a surface crack is present at the inside saddle of the tensile brace (Fig. 6.37). This may explain why the load displacement curve determined from the tensile brace of the cracked multi-planar CHS TT-joint is slightly higher than the curve for the uncracked joint.

6.12 RESULTS AND DISCUSSION

The main objective of this chapter is to investigate the plastic collapse load/moments of cracked multi-planar CHS TT-joints. In fact, it is the extension of Chapter 5, in which the plastic collapse load/moments of cracked uni-planar CHS T/Y-joints are investigated. The entire chapter is divided into four sections, in which the plastic collapse load/moments of cracked multi-planar CHS TT-joints subjected to axial tensile-tensile load; in-plane bending, out-of-plane bending and axial tensile-compressive load are investigated in sequence.

Firstly, the plastic collapse load of cracked multi-planar CHS TT-joints subjected to axial tensile-tensile load (Fig. 6.2(a)) is investigated. It is found that a semi-elliptical surface crack with the crack area A_{nc} up to 25% has a more severe effect on the cracked multi-planar CHS TT-joints than the cracked uni-planar CHS T/Y-joints. This can be observed from Fig. 6.12, in which some F_{AR} values of cracked multi-planar CHS TT-joints with $A_{nc}=25\%$ are below the lower bound of cracked uni-planar CHS T/Y-joints.

Secondly, the plastic collapse moment of cracked multi-planar CHS TT-joints subjected to in-plane bending (Fig. 6.2(c)) is investigated. It is found that a semi-elliptical surface crack has less effect on the cracked multi-planar CHS TT-joints than the cracked uni-planar CHS T/Y-joints. This can be observed from Fig. 6.21, in which all the F_{AR} values of cracked multi-planar CHS TT-joints are above the lower bound of cracked uni-planar CHS T/Y-joints.

Thirdly, the plastic collapse moment of cracked multi-planar CHS TT-joints subjected to out-of-plane bending (Fig. 6.2(d)) is investigated. Similar to the cracked multi-planar CHS TT-joints subjected to in-plane bending, a semi-elliptical surface crack is found to have less effect on the cracked multi-planar CHS TT-joints than the cracked uni-planar CHS T/Y-joints. This can be observed from Fig. 6.29, in which all the F_{AR} values of cracked multi-planar CHS TT-joints are above the lower bound of cracked uni-planar CHS T/Y-joints.

Finally, the plastic collapse load of cracked multi-planar CHS TT-joints subjected to axial tensile-compressive load (Fig. 6.2(b)) is investigated. Unlike the above three loading cases, it is found that a surface crack has a minor effect on the plastic collapse load determined from the brace under tension for the cracked multi-planar CHS TT-joints subjected to axial tensile-compressive load. This can be observed from Figs. 6.34(a)-(f).

Based on the above discussion, it can be seen that the lower bound equations for cracked uni-planar CHS T/Y-joints proposed in Chapter 5 cannot be directly used for cracked multi-planar CHS TT-joints. Therefore, three specific lower bound equations of the strength reduction factor are proposed for cracked multi-planar CHS TT-joints in this chapter.

Table 6.1 Dimensions of uncracked multi-planar CHS TT-joints

No.	α	β	γ	φ	d_0 (mm)	d_1, d_2 (mm)	l_0 (mm)	l_1, l_2 (mm)	
								Axial load	Bending moment
TT1	12	0.3	10	60°	200	60	1200	600	200
TT2	12	0.3	10	90°	200	60	1200	600	200
TT3	18	0.3	25	60°	500	150	4500	900	300
TT4	18	0.3	25	90°	500	150	4500	900	300
TT5	12	0.5	10	90°	200	100	1200	900	300
TT6	18	0.5	25	90°	500	250	4500	900	300

Table 6.2 Crack positions of multi-planar CHS TT-joints subjected to axial T-T load

	TT1	TT2	TT3	TT4	TT5	TT6
Crack position	Crown	Inside saddle	Outside saddle	Inside saddle	Between crown and inside saddle	Inside saddle

Table 6.3 Non-dimensional plastic collapse loads of uncracked multi-planar CHS TT-joints subjected to axial T-T load.

	TEC	Deformation limit	Eurocode 3 (2005)	CIDECT (2008)
TT1	7.56	7.60	6.70	6.90
TT2	9.08	9.30	6.70	6.90
TT3	13.2	9.90	7.30	7.50
TT4	16.2	13.20	7.30	7.50
TT5	9.81	10.00	10.40	11.50
TT6	17.8	15.80	11.30	12.50

Table 6.4 Crack positions of multi-planar CHS TT-joints subjected to in-plane bending

	TT1	TT2	TT3	TT4	TT5	TT6
Crack position	Crown	Crown	Between crown and inside saddle	Between crown and inside saddle	Between crown and inside saddle	Inside saddle

Table 6.5 Non-dimensional plastic collapse moments of uncracked multi-planar CHS TT-joints subjected to in-plane bending

	TEC	Lu's limit	Eurocode 3 (2005)	CIDECT (2008)
TT1	4.74	-	3.46	3.46
TT2	4.75	-	3.46	3.46
TT3	6.54	8.14	7.28	6.45
TT4	6.57	8.01	7.28	6.45
TT5	6.99	8.71	5.77	5.77
TT6	10.08	11.52	12.13	10.75

Table 6.6 Non-dimensional plastic collapse moments of uncracked multi-planar CHS TT-joints subjected to out-of-plane bending

	TEC	Lu's limit	Eurocode 3 (2005)	CIDECT (2008)
TT1	3.16	4.37	3.46	3.02
TT2	3.85	4.82	3.46	3.02
TT3	4.22	4.58	3.57	3.47
TT4	4.44	4.83	3.57	3.47
TT5	4.69	5.48	4.54	4.24
TT6	6.43	5.89	4.54	4.86

(a) Axial tensile-tensile (T-T) load

(b) In-plane bending

(c) Out-of-plane bending

(d) Axial tensile-compressive (T-C) load

Fig. 6.1 Load types of multi-planar CHS TT-joints

(a) Axial T-T load

(b) Axial T-C load

(c) In-plane bending

(d) Out-of-plane bending

Fig. 6.2 Boundary conditions for multi-planar CHS TT-joints subjected to different loads

(a) $\alpha=12$, $\beta=0.3$, $\gamma=10$, $\phi=60^\circ$

(b) $\alpha=18$, $\beta=0.3$, $\gamma=25$, $\phi=60^\circ$

Fig. 6.3 Brace end constraint effect

Fig.6.4 Deformation of multi-planar CHS TT-joints subjected to axial compressive-compressive (C-C) load without any lateral constraint at the ends of two braces

Fig. 6.5 Geometry and notations of a typical multi-planar CHS TT-joint

Fig. 6.6 Stress distribution of multi-planar CHS TT-joints subjected to axial T-T load

Fig. 6.7 Deformation characteristic of the chords of 6 uncracked multi-planar CHS TT-joints subjected to axial T-T load ($DSF=10$, $P_a/P_c \approx 1$)

Fig. 6.8 Load displacement curves of uncracked multi-planar CHS TT-joints subjected to axial T-T load

(a) TT1, TT2, TT5

(b) TT3, TT4, TT6

Fig. 6.9 Determination of the plastic collapse load of uncracked multi-planar CHS TT-joints using deformation limit concept

 Fig. 6.10 Non-dimensional plastic collapse loads versus β

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.11 Load displacement curves of cracked multi-planar CHS TT-joints subjected to axial tensile load

Fig. 6.12 Effect of crack area on F_{AR} of cracked multi-planar CHS TT-joints subjected to axial T-T load

Fig. 6.13 Proposed lower bound of all F_{AR} of cracked multi-planar CHS TT-joints subjected to axial T-T load

Fig. 6.14 Stress distribution of multi-planar CHS TT-joints subjected to in-plane bending

Fig. 6.15 Typical local deformation of multi-planar CHS TT-joints subjected to in-plane bending (TT4, DSF=5, $M_{ai}/M_{ci} \approx 1$)

Fig. 6.16 Moment rotation curves of 6 uncracked multi-planar CHS TT-joints subjected to in-plane bending

Fig. 6.17 Determination of Lu's deformation limit of multi-planar TT-joints subjected to in-plane bending (TT4, DSF=5)

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.18 Non-dimensional in-plane plastic collapse moments determined using twice elastic compliance and Lu's deformation limit

 Fig. 6.19 Non-dimensional in-plane plastic collapse moments versus β

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.20 Moment rotation curves of cracked multi-planar CHS TT-joints subjected to in-plane bending

Fig. 6.21 Effect of crack area on F_{AR} of cracked multi-planar CHS TT-joints subjected to in-plane bending

Fig. 6.22 Proposed lower bound of all F_{AR} of cracked multi-planar CHS TT-joints subjected to in-plane bending

Fig. 6.23 Stress distribution of multi-planar CHS TT-joints subjected to out-of-plane bending in this study (TT3)

Fig.6.24 Typical failure mode of Multi-planar CHS TT-joints subjected to out-of-plane bending (TT3, DSF=5, $M_{ao}/M_{co} \approx 1$)

Fig. 6.25 Moment rotation curves of uncracked multi-planar CHS TT-joints subjected to out-of-plane bending

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.26 Non-dimensional out-of-plane plastic collapse moments determined using twice elastic compliance and Lu's deformation limit

 Fig. 6.27 Non-dimensional out-of-plane plastic collapse moments versus β

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.28 Moment rotation curves of cracked multi-planar CHS TT-joints subjected to out-of-plane bending

Fig. 6.29 Effect of crack area on F_{AR} of cracked multi-planar CHS TT-joints subjected to out-of-plane bending

Fig. 6.30 Proposed lower bound of all F_{AR} of cracked multi-planar CHS TT-joints subjected to out-of-plane bending

Fig. 6.31 Stress distribution of multi-planar CHS TT-joints subjected to axial T-C load (TT4)

(a) TT1

(b) TT2

(c) TT3

(d) TT4

Fig. 6.32 Deformation characteristic of the chords of 6 uncracked multi-planar CHS TT-joints subjected to axial T-C load (DSF=5, $P_a/P_c \approx 1$)

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.33 Load displacement curves of uncracked multi-planar CHS TT-joints subjected to axial T-C load

(a) TT1

(b) TT2

(c) TT3

(d) TT4

(e) TT5

(f) TT6

Fig. 6.34 Load displacement curves of cracked multi-planar CHS TT-joints subjected to axial T-C load

Fig. 6.35 Crack closure due to the brace under compressive load (TT3, $A_{nc}=25\%$)

Fig. 6.36 Elements used to investigate load shedding at the chord-brace zone due to surface crack

(a) uncracked CHS TT-joint

(b) cracked CHS TT-joint

Fig. 6.37 von-Mises stress at the middle of the chord between two braces (TT5)

CHAPTER 7

FAILURE ASSESSMENT DIAGRAM

7.1 INTRODUCTION

The Option 1 failure assessment diagram (FAD) analysis of BS7910 (2013) is for a general purpose usage, and the FAD curve is deemed towards to the lower bound. However, as the Option 1 FAD curve was only verified against experimental results of relatively simple geometries such as cracked plain plates and cylinders, therefore, it is necessary to validate it before it can be applied to complex cracked structures such as cracked circular (CHS) and rectangular (RHS) hollow section joints. Lie et al. (Lie, Yang & Gho, 2009) carried out finite element (FE) analyses on the cracked uni-planar RHS T-, Y- and K-joints containing a surface crack subjected to axial load, and it was found that some segments of the constructed Option 3 FAD curves fell inside the Option 1 curve for some cracked RHS joints. It implies that the usage of Option 1 FAD curve is not always safe for cracked RHS joints subjected to axial load. Laham and Burdekin (Laham & Burdekin, 1997) carried out FE analyses on cracked CHS K-joints containing a through-thickness crack under balanced axial load, in-plane and out-of-plane bending, respectively. It was found that some of the constructed Option 3 FAD curves are completely different from the Option 1 curve. It is to be noted that FE mesh models generated at that time are very coarse and may not produce accurate plastic collapse loads.

The main objective of this chapter is to verify if the general usage Option 1 FAD curve is safe to predict the safety and integrity of a cracked uni-planar CHS T/Y-joint or multi-planar CHS TT-joint subjected to a single load only. In order to do so, the Option 3 FAD curves of 30 cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are compared directly with the Option 1 curve. Three basic loads are covered in this study. Finally, a convenient method to calculate the plastic collapse load/moments of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints is proposed.

7.2 OPTION 3 FAD CURVES

The Option 3 FAD curves of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are constructed using the previously obtained FE results of plastic collapse loads.

The plastic collapse load is determined from deformation limit rather than the twice elastic compliance, and it is used to calculate L_r in the FAD. This is because gross yielding at the chord-brace zone generally occurs when the deformation limit is reached, and yielding may be confined at a small region when the twice elastic compliance is reached. In addition, for CHS under axial loading, the plastic collapse load determined by using twice elastic compliance may not be reasonable.

The cutting-off line of the Option 1 FAD curve is expressed as

$$L_{r\max} = \frac{\sigma_f}{\sigma_y} \quad (7.1)$$

where σ_f is the average of σ_y and σ_u . The parameter L_r is defined as the limit load based on the yield (0.2% proof) stress. This is classically defined for a perfectly plastic material and can be estimated by a variety of methods. If a strain hardening material is used in the FE analyses, the loading can go beyond $L_r=1$ and indeed beyond $L_{r\max}$ of Eq. (7.1). However, failure is generally taken to be governed by plastic collapse at $L_r=L_{r\max}$ rather than by fracture. From the FE analyses carried out in this study, it is confirmed that the loading can go beyond $L_r=1$ if the material strain hardening effect is considered. It is also found that elements used to calculate the elastic-plastic J -integral may be distorted severely when the loading is larger than the plastic collapse load/moment considering the material strain hardening effect. In this case, the elastic-Plastic J -integral obtained may not be reliable when the loading is larger than the plastic collapse load/moment. In addition, failure is generally dominated by plastic collapse when the loading is close to $L_{r\max}$. Therefore, in this study, when constructing the Option 3 FAD curves using FE results, the constructed Option 3 FAD curves are intentionally cut off at $L_r=1$.

The applied load is increased using an interval of 10% up to the plastic collapse load. Therefore, 10 data points are used to construct the Option 3 FAD curve for each cracked CHS join. The horizontal axis L_r is divided equally using 10 segments. The elastic J -integral and elastic-plastic J -integral are calculated under each applied load for each joint. After that, the Option 3 FAD curves are constructed and compared with the Option 1 curve.

7.2.1 Calculation of J_e and J_{ep}

In BS7910 (2013), it is stated that ‘A failure assessment curve specific to a particular material, geometry and loading type may be determined using both elastic and elastic-plastic analyses of the flawed structure as a function of the loads giving rise to primary stresses, that is those which contribute to the evaluation of L_r .’ This is the Option 3 curve and it is given by

$$f(L_r) = \sqrt{J_e / J_{ep}} \quad L_r \leq L_{rmax} \quad (7.2)$$

$$f(L_r) = 0 \quad L_r > L_{rmax} \quad (7.3)$$

where J_e is the value from the J -integral from the elastic analysis at the load corresponding to the value L_r , and J_{ep} is the value from the J -integral from the elastic-plastic analysis at the load corresponding to the value L_r . This curve is not suitable for a general use. It is useful only for specific cases as an alternative approach to Options 1 and 2. In order to construct the Option 3 curve of cracked CHS joints, J_e and J_{ep} must be determined properly. The plastic collapse load/moments considering the material strain hardening effect have been determined in Chapters 5 and 6.

There is no existing J -integral solution for any cracked CHS joint containing a semi-elliptical surface crack. Therefore, mesh convergence test on the elastic and elastic-plastic J -integral have been carried out in Chapter 3. In this chapter, firstly, the elastic-plastic J -integral J_{ep} under different levels of loading up to greater than the plastic collapse load/moment are calculated using the ABAQUS software. Secondly, the corresponding elastic J -integral J_e under same level of loading are calculated. Finally, the term $\sqrt{J_e / J_{ep}}$ can be determined, and thus, the Option 3 FAD curve for a particular joint is constructed.

Fig. 7.1 shows an example of the J_{ep} plotted against the L_r at the crack deepest point of a cracked CHS T/Y-joint subjected to axial tensile load. It can be seen that L_r reaches up to 1.1, and it means the applied loading is up to 1.1 time of the plastic collapse load. Generally, at the small-scale yielding region ($L_r \leq 0.5$), path independence of the J -integral is valid. However, at large-scale yielding region ($L_r > 0.5$), path independence of the

obtained elastic-plastic J -integral is not valid. In this study, the highest J_{ep} at each applied loading level is used to construct the Option 3 FAD curve. This has been discussed in Section 2.4.4 of Chapter 2. For the case shown in Fig. 7.1, J_{ep} values from element contour 8 are used. Fig. 7.2 shows the J_e plotted against the L_r for the same cracked CHS T/Y-joint shown in Fig. 7.1. It can be seen that the elastic J -integral maintains path independence when the applied loading is up to 1.1 time of the plastic collapse load. The average of the J_e values under each loading is used to construct the Option 3 FAD curve in this study.

7.2.2 Cracked Uni-Planar CHS T/Y-Joints

A total of 12 cracked uni-planar CHS T/Y-joints are selected and investigated in this section. It is expected that the 12 models represent the typical cracked CHS T/Y-joints. Table 7.1 lists the dimensions and the calculated plastic collapse load/moments of these 12 cracked uni-planar CHS T/Y-joints. The plastic collapse load/moments of these 12 cracked uni-planar CHS T/Y-joints have been obtained in Chapter 5 and normalized with respect to the yield stress. Different models are grouped together to study the effect of a certain parameter on the Option 3 FAD curve as shown in Table 7.2.

The Option 3 FAD curves of 12 cracked uni-planar CHS T/Y-joints subjected to three basic loads are shown in Figs. 7.3, 7.4 and 7.5, respectively. It can be seen that some segments of the constructed Option 3 FAD curves fall inside the Option 1 curve. By analyzing all the constructed Option 3 FAD curves, several findings can be drawn and they are listed as follows:

- For cracked uni-planar CHS T/Y-joints with $\gamma=10$, almost all the segments of the Option 3 FAD curves fall inside the Option 1 curve. For cracked uni-planar CHS T/Y-joints with $\gamma=18$, the Option 3 FAD curve does not necessary fall inside the Option 1 curve. For cracked uni-planar CHS T/Y-joints with $\gamma=25$, the Option 3 FAD curve is much higher than the Option 1 curve at the small-scale yielding region ($L_r \leq 0.5$).
- It is also found that the Option 1 FAD curve is not the lower bound for cracked uni-planar CHS T/Y-joints containing a surface crack as some segments of the Option 3 FAD curves fall inside it. In order to use the Option 1 FAD curve safely, the plastic collapse load/moments must be determined correctly. For the Option 3 FAD curves shown in Figs. 7.3, 7.4 and 7.5, an efficient way to move all the data points outside of

the Option 1 curve is to reduce the plastic collapse load/moments (Yang, 2007). This will be further discussed later.

7.2.3 Cracked Multi-planar CHS TT-joints

A total of 18 cracked multi-planar CHS TT-joints containing a surface crack are investigated in this section. The plastic collapse loads obtained from Chapter 6, and the dimensions are tabulated in Table 6.3. As the plastic collapse moments of groups TT1 and TT2 subjected to in-plane bending determined from Lu's deformation limit are not available in Chapter 6, the Option 3 FAD curves of groups TT1 and TT2 subjected to in-plane bending are not attempted in this section.

The Option 3 FAD curves of all cracked multi-planar CHS TT-joints subjected to three basic loads are shown in Figs. 7.6, 7.7 and 7.8. Similar findings are observed and they are listed as follows:

- For cracked multi-planar CHS TT-joints with lower γ value such as 10, the Option 3 FAD curves similarly tend to fall inside the Option 1 curve. For cracked multi-planar CHS TT-joints with higher γ value such as 25, the Option 3 FAD curve does not necessary fall inside the Option 1 curve. This is similar to the cracked uni-planar CHS T/Y-joints containing a surface crack
- Similarly, it is observed that the Option 1 FAD curve is not the lower bound for cracked multi-planar CHS TT-joints containing a surface crack as some segments of the Option 3 FAD curves fall inside it.

7.3 MODIFIED CONSTRUCTED OPTION 3 FAD CURVES

As reported by Yang (Yang, 2006), an efficient way to move the constructed Option 3 FAD curves outside of the Option 1 curve is to reduce the plastic collapse load/moment used for calculating the parameter L_r , by introducing a penalty factor working on the plastic collapse load/moment of cracked CHS joints. In Figs. 7.3-7.8, the plastic collapse load/moments are determined from the FE analyses. This study provides a convenient way to determine a safe plastic collapse load/moment of a cracked uni-planar CHS T/Y-joint or multi-planar CHS TT-joint. The plastic collapse load/moment can be calculated by multiplying the strength reduction factors F_{AR} presented in Chapters 5 and 6 with the

characteristic strength of uncracked CHS joints recommended in other codes of practice such as Eurocode 3 (2005), API-RP-2A (2007) and CIDECT (Wardenier et al., 2008). As the plastic collapse load/moment of uncracked CHS joints calculated using these codes of practice is generally much smaller than that predicted using FE analysis, their ratio can be defined as the penalty factor which is expressed as

$$F_p = \frac{\text{Plastic collapse load / moment calculated using the FE analysis}}{\text{Plastic collapse load / moment calculated using the strength reduction factor}} \quad (7.4)$$

The penalty factors for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are tabulated in Tables 7.4 and 7.5, respectively. In order to obtain a safe prediction, the lowest characteristic strength obtained from Eurocode 3 (2005), API-RP-2A (2007) and CIDECT (Wardenier et al., 2008), is used to calculate the penalty factor.

The modified Option 3 FAD curves of all cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are shown in Figs. 7.9-7.14. By observing all the modified Option 3 FAD curves, the following findings can be drawn:

- All the constructed Option 3 FAD curves for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to in-plane bending are completely lie outside of the general Option 1 curve, by multiplying the corresponding penalty factors to the L_r values in Figs. 7.3-7.8.
- For cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to axial load or out-of-plane bending, there are still some segments of the Option 3 FAD curves fall inside the general usage Option 1 curve after introducing the penalty factors. However, it can be seen that all the segments falling inside are quite close to the general usage Option 1 curve under the same loading level. It is observed that all the penalty factors are acceptable.
- It is important to recognize that the penalty factor is needed when the plastic collapse load/moments are obtained from FE analyses using the twice elastic compliance and Lu's deformation limit. If the plastic collapse load/moments are determined by multiplying the strength reduction factors proposed in this study to the plastic collapse load/moments of uncracked joints in the codes, then penalty factor is not needed.

7.4 DISCUSSIONS

As mentioned above, there are still some segments of the Option 3 FAD curves fall inside the general usage Option 1 curve after introducing the penalty factors presented in Eq. (7.2). A simplified method is to increase the corresponding penalty factors so as to move further the constructed Option 3 FAD curves outside of the Option 1 curve. However, it is not the economical way even though it is very straightforward and efficient. This is because certain amount of conservatism exists not only in the plastic collapse load/moment but also the fracture toughness of the material as the position of the assessment point is determined by K_r and L_r . Even if some segments of the constructed Option 3 FAD curve slightly fall inside the Option 1 curve, it does not mean that the cracked CHS joint will fail when the Option 1 curve is used in the assessment. It merely means that it is necessary to examine it more carefully when a higher level assessment is required. Therefore, the plastic collapse load/moment of a cracked uni-planar CHS T/Y-joint or multi-planar CHS TT-joint can be determined by multiplying the strength reduction factor with the corresponding characteristic strength of the uncracked joint. The general usage Option 1 FAD analysis can provide a safe prediction with a high reliability in assessing the safety and integrity of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints within the ranges used of this study.

Table 7.1 Dimensions and the plastic collapse load/moments of cracked uni-planar CHS
T/Y-joints

No.	l_0 (mm)	d_0 (mm)	l_1 (mm)	d_1 (mm)	θ (°)	a (mm)	$2c$ (mm)	A_{nc} (%)	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)
T1	3240	360	600 (200)	72	90	6	88	15	336.6	13.9	10.5
T2	3240	360	600 (400)	144	90	6	162	15	471.2	39.3	24.9
T3	3240	360	600 (400)	216	90	8	180	15	580.6	81.3	49.0
T4	3240	360	600 (400)	288	90	8	238	15	631.0	138.8	84.3
T5	1200	200	400 (400)	80	90	6	97	15	430.6	18.2	13.4
T6	4500	500	800 (400)	200	90	8	164	15	515.9	58.3	34.6
Y7	3240	360	600 (400)	144	60	6	174	15	533.6	42.0	28.7
T8	3240	360	600 (400)	144	90	2	64	2	490.0	45.0	28.0
T9	3240	360	600 (400)	144	90	4	96	5.9	483.0	43.0	27.4
T10	3240	360	600 (400)	144	90	6	96	8.9	480.0	42.0	25.8
T11	1200	200	400 (400)	80	90	2	48	2.5	441.1	20.0	15.3
T12	1200	200	400 (400)	80	90	4	72	7.4	437.0	19.5	13.7

Table 7.2 Model groups for different research purpose

Research purpose	Model number
Effect of β	T1, T2, T3, T4
Effect of γ	T2, T5, T6
Effect of θ	T2, Y7
Effect of crack depth	T8, T9, T10 and T5, T11, T12

Fig. 7.3 Dimensions and the plastic collapse load/moments of cracked multi-planar CHS
TT-joints

No.	l_0 (mm)	d_0 (mm)	l_1, l_2 (mm)	d_1, d_2 (mm)	φ (°)	a (mm)	$2c$ (mm)	A_{nc} (%)	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)
TT1-1	1200	200	600 (200)	60	60	4	38	5	266.3	-	8.8
TT1-2	1200	200	600 (200)	60	60	6	77	15	256.3	-	7.7
TT1-3	1200	200	600 (200)	60	60	8	96	25	238.6	-	6.7
TT2-1	1200	200	600 (200)	60	90	4	38	5	328.7	-	10.0
TT2-2	1200	200	600 (200)	60	90	6	77	15	318.1	-	8.9
TT2-3	1200	200	600 (200)	60	90	8	96	25	301.4	-	7.7
TT3-1	4500	500	900 (300)	150	60	4	84	5	351.1	42.7	23.4
TT3-2	4500	500	900 (300)	150	60	6	167	15	344.7	37.2	21.3
TT3-3	4500	500	900 (300)	150	60	8	209	25	333.7	29.1	19.5
TT4-1	4500	500	900 (300)	150	90	4	84	5	468.2	41.8	24.7
TT4-2	4500	500	900 (300)	150	90	6	167	15	447.3	36.1	22.3
TT4-3	4500	500	900 (300)	150	90	8	209	25	430.3	28.4	20.6
TT5-1	1200	200	900 (300)	100	90	4	59	5	353.6	30.7	18.8
TT5-2	1200	200	900 (300)	100	90	6	119	15	343.3	27.5	16.9
TT5-3	1200	200	900 (300)	100	90	8	149	25	312.0	21.6	14.2
TT6-1	4500	500	900 (300)	250	90	4	136	5	560.5	101.2	51.3
TT6-2	4500	500	900 (300)	250	90	6	271	15	546.7	91.2	47.9
TT6-3	4500	500	900 (300)	250	90	8	339	25	526.1	75.3	44.9

Table 7.4 Penalty factors of cracked uni-planar CHS T/Y-joints working on the plastic collapse load/moment

No.	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)	F_p	F_p	F_p
	FE	FE	FE	EC 3	API- RP-2A	API- RP-2A	$\frac{P_{c-FE}}{P_{c-EC 3}}$	$\frac{M_{ci-FE}}{M_{ci-API}}$	$\frac{M_{co-FE}}{M_{co-API}}$
T1	336.6	13.9	10.5	201.9	5.18	5.24	1.67	2.68	2.00
T2	471.2	39.3	24.9	304.1	23.8	12.9	1.55	1.65	1.92
T3	580.6	81.3	49.0	474.4	58.1	27.9	1.22	1.40	1.76
T4	631.0	138.8	84.3	712.8	109.4	56.3	0.98	1.27	1.50
T5	430.6	18.2	13.4	270.4	9.0	6.9	1.59	2.02	1.94
T6	515.9	58.3	34.6	324.7	42.3	18.7	1.59	1.38	1.84
Y7	533.6	42.0	28.7	351.1	27.5	15.0	1.52	1.53	1.91
T8	490.0	45.0	28.0	318.7	29.1	16.1	1.54	1.54	1.74
T9	483.0	43.0	27.4	314.3	27.5	15.2	1.54	1.56	1.80
T10	480.0	42.0	25.8	311.0	26.3	14.5	1.54	1.60	1.78
T11	441.1	20.0	15.3	282.9	10.9	8.5	1.56	1.82	1.80
T12	437.0	19.5	13.7	277.9	10.2	7.9	1.57	1.91	1.74

Table 7.5 Penalty factors of cracked multi-planar CHS TT-joints working on the plastic collapse load/moment

No.	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)	P_c (kN)	M_{ci} (kN*m)	M_{co} (kN*m)	F_p	F_p	F_p
	FE	FE	FE	EC 3	API- RP-2A	API- RP-2A	$\frac{P_{c-FE}}{P_{c-EC 3}}$	$\frac{M_{ci-FE}}{M_{ci-API}}$	$\frac{M_{co-FE}}{M_{co-API}}$
TT1-1	266.3	-	8.8	223.5	-	5.6	1.19	-	1.58
TT1-2	256.3	-	7.7	211.6	-	4.8	1.21	-	1.59
TT1-3	238.6	-	6.7	199.7	-	4.1	1.19	-	1.63
TT2-1	328.7	-	10.0	223.5	-	5.6	1.47	-	1.80
TT2-2	318.1	-	8.9	211.6	-	4.8	1.50	-	1.84
TT2-3	301.4	-	7.7	199.7	-	4.1	1.51	-	1.87
TT3-1	351.1	42.7	23.4	251.4	20.6	14.3	1.40	2.07	1.64
TT3-2	344.7	37.2	21.3	238.0	17.7	12.4	1.45	2.10	1.72
TT3-3	333.7	29.1	19.5	224.6	14.7	10.6	1.49	1.97	1.85
TT4-1	468.2	41.8	24.7	251.4	20.6	14.3	1.86	2.03	1.73
TT4-2	447.3	36.1	22.3	238.0	17.6	12.4	1.88	2.04	1.80
TT4-3	430.3	28.4	20.6	224.6	14.7	10.6	1.92	1.93	1.95
TT5-1	353.6	30.7	18.8	348.0	17.3	11.9	1.02	1.77	1.58
TT5-2	343.3	27.5	16.9	329.5	14.8	10.4	1.04	1.85	1.63
TT5-3	312.0	21.6	14.2	311.0	12.4	8.8	1.00	1.75	1.61
TT6-1	560.5	101.2	51.3	391.4	63.5	32.0	1.43	1.59	1.60
TT6-2	546.7	91.2	47.9	370.6	54.4	27.8	1.47	1.68	1.72
TT6-3	526.1	75.3	44.9	349.8	45.3	23.7	1.50	1.66	1.90

Fig. 7.1 Elastic-plastic J -integral J_{ep} versus L_r
 $(\alpha=18, \beta=0.4, \gamma=18, \theta=90^\circ, a/t_0=0.6, A_{nc}=15\%)$

Fig. 7.2 Elastic J -integral J_e versus L_r
 $(\alpha=18, \beta=0.4, \gamma=18, \theta=90^\circ, a/t_0=0.6, A_{nc}=15\%)$

Fig. 7.3 The Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to axial load

Fig. 7.4 The Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to in-plane bending

Fig. 7.5 The Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to out-of-plane bending

(a) $\gamma=10$

(b) $\gamma=25$

Fig. 7.6 The Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to axial load

Fig. 7.7 The Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to in-plane bending

(a) $\gamma=10$ (b) $\gamma=25$

Fig. 7.8 The Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to out-of-plane bending

Fig. 7.9 The modified Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to axial load

Fig. 7.10 The modified Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to in-plane bending

Fig. 7.11 The modified Option 3 FAD curves of cracked uni-planar CHS T/Y-joints subjected to out-of-plane bending

Fig. 7.12 The modified Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to axial T-T load

Fig. 7.13 The modified Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to in-plane bending

Fig. 7.14 The modified Option 3 FAD curves of cracked multi-planar CHS TT-joints subjected to out-of-plane bending

CHAPTER 8

CONCLUSIONS AND RECOMMENDATIONS

8.1 CONCLUSIONS

The jacket offshore platforms are still being used in shallow waters around the world. Many of them have been in service for many years. Hence, fatigue cracks are frequently detected at the hot spot location of the chord-brace intersection of the circular hollow section (CHS) joints. These damaged offshore platforms are still in service due to various reasons. For instance, a fatigue crack may initiate during in-between of two routine maintenances. In this case, the fatigue crack can only be detected in the second routine maintenance. In addition, a very small fatigue crack has a minor influence on the entire structure. Hence, it is not economical to carry out repair works immediately after the routine maintenance. In BS7910 (2013) and API 579-1 (2007), there is an approach which can be used to assess the safety and integrity of any a cracked CHS joint. This approach is based on the failure assessment diagram (FAD) method. This method can take into account the plastic collapse and fracture behaviors together when it is used to assess the safety and integrity of the cracked CHS joints. It is very straightforward for engineers to use this FAD method which depends on the position of the assessment point defined as (K_r, L_r) . However, it is found that the existing provisions on the usage of FAD method to assess cracked CHS joints are quite general. There are no explicit equations provided in BS7910 (2013) and API 579-1 (2007). For instance, only equations for estimating the plastic collapse loads of cracked uni-planar CHS T/Y-, K- and X-joints subjected to axial load are provided. For multi-planar CHS joints subjected to axial load, there is no such equation. Similarly, there are no available equations for any a cracked CHS joint subjected to in-plane and out-of-plane bending.

The stress intensity factor, K_I is a very important fracture parameter needed in the FAD analysis. An indirect method proposed by Bowness and Lee (Bowness & Lee, 2002) is incorporated in BS7910 (2013) to estimate the stress intensity factors of cracked CHS joints containing a surface crack. Generally, the indirect method overestimates the stress intensity factors so that it can produce a very conservative prediction. This has been

discussed in Chapter 2. The indirect method is limited to several types of CHS joints such as uni-planar CHS T/Y-, K- and X-joints.

The objective of this study is to provide a safe and convenient method of calculating the plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subject to three basic loads. Emphasis is put on the in-plane and out-of-plane bending as the plastic collapse moments (in-plane and out-of-plane) of cracked CHS joints have never been reported.

Another objective of this study is to validate the existing Option 1 FAD analysis. Option 1 FAD curve is for a general usage, and it is deemed towards the lower bound. It has been validated against experimental and numerical results of cracked plain plate. However, such a validation for cracked CHS joints has not been reported in the literature.

From the present research works, several conclusions are noted and they are summarized as follows:

1. A robust finite element (FE) mesh generator for generating the mesh models of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints is developed in this study. Extensive FE analyses are carried out to calculate the fracture parameters used in the FAD analysis; it includes the stress intensity factor, elastic J -integral, elastic-plastic J -integral and plastic collapse load/moment.
2. Different mesh designs of the crack tips are adopted in the elastic and elastic-plastic analysis respectively. In this study, elastic analysis focuses on the stress intensity factor and the elastic J -integral, whereas elastic-plastic analysis concerns calculating the elastic-plastic J -integral and the plastic collapse load/moment. In the elastic analysis, elements enclosing the crack front share one node at the crack tips, and these collapsed nodes are generated to produce the well-known quarter-node singularity element. In the elastic-plastic analysis, elements enclosing the crack front use different nodes at the crack tips, and a finite radius key hole is designed at the crack front to ensure convergence even at large plastic deformation.
3. In order to capture an accurate stress distribution, sufficient elements should be generated along the weld toe at the chord-brace region of the cracked CHS joints. A minimum of 40 elements along the weld toe is recommended in this study. Element size at

the remaining part ahead of the surface crack has a significant effect on the stress intensity factor and J -integral. Therefore, element size should be of the same size as elements along the crack front. For calculating the stress intensity factor and elastic J -integral, 4 element contours enclosing the crack front are sufficient to produce accurate prediction. Results from the first contour are generally not accurate and should be discarded.

4. For calculating the elastic-plastic J -integral, it is found that mesh requirement at the crack front is important. Firstly, the integral domain (dimensions of the crack tube) should be sufficiently large. This is because at large yielding region ($L_r > 0.5$), J -integral results from the first several inner element contours tend to be invalid due to severely distortion of the elements. J -integral at the large yielding region ($L_r > 0.5$) tends to be path dependent. Secondly, 6 element contours are recommended to check the path independence of the J -integral at the large yielding region. If all the 6 contours of elements are severely distorted, then it is necessary to increase the size of the integral domain and repeat the calculation. For the cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints investigated in this study, the integral domain size is generally in between $0.06t_0$ - $0.12t_0$ (0.6 - 1.2mm). At the large yielding region ($L_r > 0.5$), the highest J -integral from the most outer contour is taken as the J_{ep} used in the FAD analysis.

5. For calculating the plastic collapse load/moment of cracked CHS joints, it is found that a relatively coarse mesh can be used at the crack region where 1 element contour enclosing the crack front is found to be sufficient. This has been verified against the load displacement curves of cracked uni-planar T/Y-joints and multi-planar TT-joints.

6. Stress distributions of the two chord-brace zones of an uncracked multi-planar CHS TT-joint are measured. Experimental results show that the stress distribution of one chord-brace zone changes when the constraint is applied on the other brace. If loading is applied at one brace only (the other brace is free from any loading and constraint), then the stress distribution at the chord-brace (loading brace) zone is similar to that of a uni-planar CHS T-joint.

7. From the fatigue test, it is confirmed that a crack initiates at the hot spot stress location of a multi-planar CHS TT-joint. An alternating current potential drop technique (ACPD) is used to monitor and record the crack initiation and propagation. It is found that the S-N

curve approach can safely be used to predict the fatigue life of the multi-planar CHS TT-joint tested in this study.

8. A reasonable and accurate plastic collapse load/moment of uncracked CHS joints can be obtained by using the Lu's deformation limit (Lu et al., 1994). This is because Lu's deformation limit refers to the local deformation of the chord at the chord-brace intersection region, and it is directly related to the main objective of this study. The twice elastic compliance is more convenient to be used for determining the plastic collapse load/moment of uncracked CHS joints, but care should always be exercised. This is because the twice elastic compliance has inherent shortcomings. For uncracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to axial load, twice elastic compliance cannot remove the bending effect of the chord. It implies that for two similar uncracked CHS joints (only the chord length is different), different plastic collapse load values may be obtained. Similarly, for uncracked CHS joints subjected to in-plane and out-of-plane bending, twice elastic compliance cannot help to determine accurate plastic collapse moments of the joint if failure occurs due to brace bending. Therefore, it is recommended to use Lu's deformation limit (Lu et al., 1994) to determine the plastic collapse load/moments of cracked CHS joints.

9. It is found that the geometry parameters such as β , γ , τ , θ and ϕ have slight influence on the plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints. Hence, the crack area has a significant influence on the plastic collapse load/moment of cracked CHS joints. The plastic collapse load/moment decrease almost linearly as the crack area increases.

10. It is shown that the strength reduction factor given in BS7910 (2013) is very conservative for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to axial load. Based on extensive parametric study, 6 sets of strength reduction factors, F_{AR} for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints are proposed in this study, and they are tabulated in Table 8.1. The strength reduction factors are proposed based on the lower bound data. In addition, all the strength reduction factor equations proposed in this study are only valid for the corresponding cracked joints containing a semi-elliptical surface crack located at the weld toe of the chord.

11. For cracked multi-planar CHS TT-joints subjected to balanced axial load (one brace under tension whereas the other brace under compression), the surface crack has a slight effect on the plastic collapse load determined from the tensile brace loading due to the crack closure.

12. The general usage Option 1 FAD curve is found generally not the lower bound for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints subjected to three basic loads. However, the Option 1 curve can still be used to produce a safe prediction if the plastic collapse load/moment is determined properly. This study provides a safe and convenient way to determine the plastic collapse load/moment of cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints in the FAD analyses. The plastic collapse load/moment can be determined by multiplying the strength reduction factor proposed in this study to the characteristic strength of the uncracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints. For the axial load case, the plastic collapse load determined using Eurocode 3 (2005) is recommended. For the in-plane and out-of-plane bending cases, the plastic collapse moments determined using API-RP-2A (2007) are recommended. The penalty factor is needed when the plastic collapse load/moments are obtained from FE analyses using the twice elastic compliance and Lu's deformation limit in this study. If the plastic collapse load/moments are determined by multiplying the strength reduction factors proposed in this study to the plastic collapse load/moments of uncracked joints in the three codes, then penalty factor is not needed.

8.2 RECOMMENDATIONS FOR FUTURE RESEARCH WORKS

1. In practice, multiple cracks at the chord-brace zone are also frequently detected. The effect of the interaction of multiple cracks on the plastic collapse load/moment and the fracture behavior of each crack can be studied in the future.

2. The current research works can be extended to other types of CHS joints such as cracked uni-planar CHS K- and X-joints, and multi-planar CHS XX-, KK- and KT-joints and so on. Research works on these cracked CHS joints are limited, particularly for in-plane and out-of-plane bending conditions.

3. In practice, CHS joints are generally subjected to combined load (combinations of axial load, in-plane and out-of-plane bending) though the axial load may be the dominant one.

When assessing the safety and integrity of a cracked CHS joint subjected to combined load using failure assessment diagram method, L_r is expressed as

$$L_r = \left| \frac{P_a}{P_c} \right| + \left| \frac{M_{ai}}{M_{ci}} \right|^2 + \left| \frac{M_{ao}}{M_{co}} \right| \quad (8.1)$$

The second term of the right part of Eq. (8.1) is originally provided for uncracked CHS joints subjected to combined load. It should be lower than 1 when designing an uncracked CHS joint subjected to combined load. However, for cracked CHS joints, Eq. (8.1) should be verified against experimental or numerical results because the loading path may have a significant effect on the fracture behavior of the loaded CHS joints.

4. More experimental testing on multi-planar CHS joints is needed to further verify the FE analysis.
5. Similar work is also needed for rectangular hollow section (RHS) joints.

Table 8.1 Proposed strength reduction factors for cracked uni-planar CHS T/Y-joints and multi-planar CHS TT-joints

Joint type	Load type	F_{AR}	Validity range
Uni-planar CHS T/Y- joint	Axial load	$F_{AR} = 1 - 0.350 \frac{A_c}{t_0 l_w}$	$0.2 \leq \beta \leq 0.8$ $10 \leq \gamma \leq 25$
	In-plane bending	$F_{AR} = 1 - 1.368 \frac{A_c}{t_0 l_w}$	$60^\circ \leq \theta \leq 90^\circ$ $0 \leq A_{nc} \leq 25\%$
	Out-of-plane bending	$F_{AR} = 1 - 1.450 \frac{A_c}{t_0 l_w}$	$0.2 \leq a/t_0 \leq 0.8$
Multi-planar CHS TT- joint	Axial load	$F_{AR} = 1 - 0.518 \frac{A_c}{t_0 l_w}$	$0.3 \leq \beta \leq 0.5$ $10 \leq \gamma \leq 25$
	In-plane bending	$F_{AR} = 1 - 1.333 \frac{A_c}{t_0 l_w}$	$60^\circ \leq \varphi \leq 90^\circ$ $0 \leq A_{nc} \leq 25\%$
	Out-of-plane bending	$F_{AR} = 1 - 1.220 \frac{A_c}{t_0 l_w}$	$0.2 \leq a/t_0 \leq 0.8$

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