Ultimate strength and fracture behaviour of cracked welded square hollow section T-joints

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ULTIMATE STRENGTH AND FRACTURE BEHAVIOUR OF CRACKED WELDED SQUARE HOLLOW SECTION T-JOINTS

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2006

Ultimate Strength and Fracture Behaviour of Cracked Welded Square Hollow Section T-Joints

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TABLE OF CONTENTS

ACKNOWLEDGEMENTS	i
TABLE OF CONTENTS	ii
SUMMARY	vii
LIST OF TABLES	ix
LIST OF FIGURES	x
NOTATIONS	xx
CHAPTER 1 INTRODUCTION	1
1.1 Background	1
1.2 Scope and Objectives	4
CHAPTER 2 LITERATURE REVIEW	8
2.1 Square and Rectangular Hollow Section Joints	8
2.2 General Design Philosophy of SHS and RHS Trusses	8
2.3 Failure Criteria of SHS and RHS Tubular Joints	9
2.4 Failure Modes of RHS Joints	9
2.5 Analytical Models to Predict Ultimate Strength of RHS Joints	10
2.5.1 Yield Line Model	10
2.5.2 Punching Shear Model	12
2.5.3 Effective Width Model	13
2.5.4 Chord Wall Bearing or Local Buckling	13
2.5.5 Shear Yield Model	14
2.6 Research on Strength of Cracked Tubular Joints	14
2.7 Fracture Mechanics Approach for Assessments of Welded Joints	17
2.7.1 Stress Intensity Factor Approach	18

2.7.2	Crack Tip Opening Displacement (CTOD) Approach	18
2.7.3	<i>J</i> -Contour Integral Approach	19
2.8	Fracture Assessment Procedures	20
2.8.1	CEGB R6 Revision 2 Procedure	21
2.8.2	FAD According to R6 Revision 3	22
2.8.3	New Updated R6 Revision 4 Procedure	24
2.9	BS7910 Methodology for Fracture Assessment	26
2.10	Residual Stresses	27
2.11	Failure Assessment for Tubular Joints	28
2.12	Mixed Mode Loading	28
 CHAPTER 3 GEOMETRICAL MODELLING OF RHS AND SHS JOINTS WITH CRACKS		39
3.1	Introduction	39
3.2	Geometry of RHS and SHS Joints	39
3.3	Geometry of Welds	39
3.4	Geometry of Fatigue Cracks in RHS and SHS Joints	41
3.4.1	Fatigue Crack Initiation Point	41
3.4.2	Fatigue Crack Shape	42
 CHAPTER 4 NUMERICAL MODELLING AND AUTOMATIC MESH GENERATION OF RHS JOINTS WITH CRACKS		52
4.1	Introduction	52
4.2	Modelling of Cracks under Weld Toe	52
4.3	Element Selection	54
4.3.1	Elastic Crack-Tip Fields	55
4.3.2	Plastic Crack-tip Fields	58
4.4	Mesh Generation Procedure	59

4.4.1	Mesh Generation of Uncracked Joints	60
4.4.2	Localization and Extraction of Crack Zone	60
4.4.3	Mesh Generation of Crack Zone for Surface Crack	60
4.4.4	Mesh Generation of Crack Zone for Through-thickness Crack	63
4.4.5	Local Mesh Refinement and Completion of Cracked Joint Mesh	63
 CHAPTER 5 PLASTIC COLLAPSE LOAD OF CRACKED RHS T-JOINTS		 79
5.1	Introduction	79
5.2	Ultimate Strength of Uncracked RHS T-joints under Axial Loads	79
5.3	Plastic Collapse Load of RHS T-joints with Surface Cracks	80
5.4	Plastic Collapse Load of RHS T-joints with Through Thickness Cracks	82
5.5	Estimation of L_r Parameter	84
 CHAPTER 6 EXPERIMENTAL TESTS OF SQUARE HOLLOW SECTION (SHS) T-JOINTS		 88
6.1	Introduction	88
6.2	Test Rig and Loading System	89
6.3	Specimen Details	90
6.4	Weld and Fatigue Crack Details	90
6.5	Monitoring of Crack Growth	90
6.6	Measurement of Crack Mouth Opening Displacement (CMOD)	92
6.7	Measurements of Brace End Axial Load and Displacement	93
6.8	Static Strength Test	93
6.9	Test Results and Discussion	93
6.9.1	Brace End Axial Load-Displacement Curves	93
6.9.2	CMOD and CTOD	95

6.9.3	Crack Growth Analyses	95
6.10	Material Properties	96
6.10.1	Stress-strain Curve	96
6.10.2	Charpy V-notch Tests	97
6.10.3	Fracture Toughness Tests	98
6.11	Failure Assessment	99
 CHAPTER 7 NUMERICAL ANALYSES OF TUBULAR SQUARE HOLLOW SECTION (SHS) T-JOINTS		126
7.1	Introduction	126
7.2	Numerical Modelling	127
7.2.1	Loading and Boundary Conditions	128
7.2.2	Material Properties	128
7.2.3	Material and Geometric Non-linearity	128
7.2.4	Element Selection and Mesh Density	129
7.3	Validation of Numerical Modelling	133
7.4	Calculation of J -integral	134
7.5	Construction of Failure Assessment Diagrams (FADs)	138
7.6	Determination of Plastic Collapse Loads	140
7.7	Numerical Analyses	143
7.7.1	General Plastic Behaviour of SHS T-joint	144
7.7.2	The Plastic Collapse Strength of SHS T-joint	144
7.7.3	Influence and Definition of Contact Crack Surface	145
7.7.4	Mixed-mode Fracture Effects	147
7.7.5	Influence of Crack Inclined Angle	150
7.7.6	Influence of Residual Stresses	151
7.7.7	FADs of SHS T-joints under Brace End Axial Loads	152

CHAPTER 8 CONCLUSIONS AND RECOMMENDATIONS	204
8.1 Conclusions	204
8.2 Recommendations for Further Research Works	208
REFERENCES	210
PUBLICATIONS	232
APPENDIX A MATERIAL CERTIFICATION	233
APPENDIX B THE CHARPY V-NOTCH TESTS RESULTS	234
APPENDIX C THE CTOD TESTS RESULTS	236
APPENDIX D THE ULTRASONIC INSPECTION TEST REPORT	245

SUMMARY

The prediction of the ultimate capacity of tubular joints is based on the use of design equations for uncracked joints. However, flaws, such as fatigue cracks, welding defects and corrosion damage, can arise during the manufacture and the use of the welded structures. As a result, the joint capacity is reduced by these flaws, and therefore, there is a need to develop procedures for estimating the static residual strength of cracked tubular joints in practice.

The primary objectives of the present study are: 1) to develop a reliable numerical model to simulate the ultimate and fracture behaviour of square and rectangular hollow section (SHS and RHS) joints containing cracks, 2) to investigate the failure of SHS T-joints containing surface cracks or through-thickness cracks under brace end axial loads, 3) to derive the plastic collapse loads of cracked SHS T-joints under brace end axial loads, and 4) to validate the usage of standard BS7910 (1999) Level 3 failure assessment diagram for damaged SHS T-joints.

To serve these objectives, a set of three full-scale destructive tests of cracked SHS T-joints is performed, and the corresponding finite element model is constructed using the actual crack profiles measured from the tests. The choice of elements, the refinement of meshes, and the effect of crack tip elements are investigated so as to obtain accurate numerical results. Based on this model, an automatic mesh generation program is proposed in this study. This model is then used to refine the parametric study of the effect of the variables, such as the crack shape, the crack size and the weld. Based on the yield line theory, the plastic collapse loads of cracked SHS T-joints under brace end axial loads are derived and validated experimentally and numerically. It is found that the yield line theory gives a reliable prediction of the plastic collapse strength for the cracked SHS T-joints with medium β ratio. Furthermore, in accordance with the J -integral approach, the failure assessment diagrams (FADs) of the SHS T-joints with different geometries are constructed subsequently, and they are compared with the standard BS7910

(1999) Level 3A curve. It is confirmed that the standard BS7910 (1999) Level 3A FAD is valid and safe for determining limits of safe loading of cracked SHS T-joints under brace end axial loads provided that the plastic collapse load is calculated using the conservative yield line formulae neglecting the influence of the welds and the chord wall thickness.

LIST OF TABLES

Table No.	Description	Page
Table 4.1	3-D solid quadratic elements	78
Table 6.1	Dimensions of the Specimens	123
Table 6.2	Mechanical Properties of the Material Provided by Corus	123
Table 6.3	Mechanical Properties of the Weld (AWS, 2000)	123
Table 6.4	The Weld and Fatigue Crack Details	123
Table 6.5	Coupon Tests Results	124
Table 6.6	The Charpy Impact Energies	124
Table 6.7	The CTOD Fracture Toughness	124
Table 6.8	The Results of K_r ($K_r = \sqrt{\frac{\delta_{CTOD}}{\delta_{IC}}}$) for Specimens T1 and T2	125
Table 6.9	The Crack Tip Opening Displacement (CTOD) Results for Specimen T3	125
Table 6.10	The Results of K_r ($K_r = \sqrt{\frac{\delta_{CTOD}}{\delta_C}}$) for Specimen T3	125
Table 6.11	The Results of L_r ($L_r = \frac{P_0}{P_u}$)	125
Table 7.1	Elastic-plastic J -integral under different types of crack tip for $\beta = 0.57$	198
Table 7.2	Elastic J -integral results for $a/T_c=0.66$ and $P=920$ kN	198
Table 7.3	Elastic-plastic J -integral results for $a/T_c=0.66$ and $P=575$ kN	198
Table 7.4	Elastic-plastic J -integral results for $a/T_c=0.66$ and $P=920$ kN	199
Table 7.5	The T-joints dimensions of the cases investigated in this study	199
Table 7.6	The surface crack details of SHS T-joints	200
Table 7.7	The through-thickness crack details of SHS T-joints	201
Table 7.8	The plastic collapse load of SHS T-joints with surface crack compared with the ultimate strength of uncracked joints	202
Table 7.9	The plastic collapse load of SHS T-joints with through-thickness crack	203

LIST OF FIGURES

Figure No.	Description	Page
Figure 1.1	A truss of HSS helped raising the Civil War submarine Hunley from the ocean floor (“HSS Literature”, 2004)	6
Figure 1.2	Lattice girder of square hollow sections in a roof structure (Rondal, et al., 1996)	6
Figure 1.3	Cycle path bridge (Packer et al., 1992)	7
Figure 1.4	Failure assessment diagram (FAD)	7
Figure 2.1	Profile of RHS joint	30
Figure 2.2	Uniplanar and multiplanar joint classification	30
Figure 2.3	Basic loading modes	31
Figure 2.4	A typical load-displacement curve	31
Figure 2.5	Failure modes of RHS joints (Wardenier, 1982)	32
Figure 2.6	Applied loadings (Wardenier, 1982)	33
Figure 2.7	Yield line patterns (Wardenier, 1982)	33
Figure 2.8	Punching shear model (Wardenier, 1982)	34
Figure 2.9	Chord side wall bearing model (Wardenier, 1982)	34
Figure 2.10	Chord side wall stability model (Wardenier, 1982)	35
Figure 2.11	Shear area A_Q of the chord member	35
Figure 2.12	Crack tip opening displacement under EPFM conditions	36
Figure 2.13	A central crack in an infinite flat plate under tension	36
Figure 2.14	Contour path for determination of the J -integral	37
Figure 2.15	FAD philosophy of flaw assessment	37
Figure 2.16	R6 Revision 3 assessment curves	38
Figure 2.17	FADs of BS7910 (1999)	38
Figure 3.1	Dimensional notations used by CIDECT (1992) and IIW (1989)	44
Figure 3.2	Cross-section of RHS and SHS joints	44
Figure 3.3	Prequalified joint details for complete joint penetration groove welds in tubular T-, Y-, and K-joints (AWS, 2000)	45

Figure 3.4	Welds of RHS and SHS joints (AWS, 2000)	45
Figure 3.5(a)	Weld toe profile of T-joint ($F \geq t_1 / 2$)	46
Figure 3.5(b)	Weld toe profile of Y and K-joints	46
Figure 3.6(a)	Dimensions of the weld ($\psi \leq 90^\circ$)	47
Figure 3.6(b)	Dimensions of the weld ($\psi > 90^\circ$)	47
Figure 3.7	The corner of chord cross-section	48
Figure 3.8	Principal stress distribution around the weld toe of T-joint under axial load	48
Figure 3.9	Principal stress distribution around the weld toe of T-joint under IPB	49
Figure 3.10	Principal stress distribution around the weld toe of T-joint under OPB	49
Figure 3.11	Crack growth development of basic fracture modes and three-dimensional superimposed Mode I+II+III loading (Leblond, 1993)	50
Figure 3.12	Fatigue crack curvature under the weld toe (Chiew et al., 2002)	50
Figure 3.13	Cross-section of fatigue crack under the weld toe	50
Figure 3.14	Crack front extension	51
Figure 3.15	Crack curvature under weld toe on a smooth curved crack surface	51
Figure 3.16	Crack under the weld toe	51
Figure 4.1	Crack surface under the weld toe	65
Figure 4.2	Local coordinate system at point W0 along the weld toe	65
Figure 4.3	Mapping from 2-D to 3-D crack front	66
Figure 4.4	Local polar u-v coordinate system	66
Figure 4.5	The weld toe on a local u-v coordinate system	67
Figure 4.6	The first quadrant of u-v local coordinate system	67
Figure 4.7	Through-thickness crack in the $u'-v'$ plane.	68
Figure 4.8	The degenerated element used around the crack front	68
Figure 4.9	The plane where $\zeta=1$	68

Figure 4.10	Elastic singularity element	69
Figure 4.11	Stress-strain relationship of power-law strain hardening material (Kanninen and Popelar, 1985)	69
Figure 4.12	Plastic singularity element	70
Figure 4.13	Deformed shape of plastic singularity elements	70
Figure 4.14	Mesh design of the weld zone	71
Figure 4.15	Mesh density around the intersection perimeter	71
Figure 4.16	The completed mesh of the weld zone	71
Figure 4.17	The complete mesh of the entire joint	72
Figure 4.18	The mesh after extracting the crack zone	72
Figure 4.19	The mesh design around the crack front	73
Figure 4.20	The u - v - w and x - y - z local coordinate system	73
Figure 4.21	The mesh along the crack front	73
Figure 4.22	The mesh of the crack zone	74
Figure 4.23	The crack front curve	74
Figure 4.24	The mesh of the crack zone of a surface crack	74
Figure 4.25	The mesh of the crack zone and the cross-section of a through-thickness crack	74
Figure 4.26	The refined mesh of the crack zone	75
Figure 4.27	The mesh of the crack zone after refining twice	75
Figure 4.28	The mesh around the crack front after doubling the mesh density	75
Figure 4.29	T-joint containing two surface cracks	76
Figure 4.30	T-joint containing two surface cracks and one through-thickness crack.	76
Figure 4.31	The completed Y-joint mesh	77
Figure 4.32	The completed K-joint mesh	77
Figure 4.33	The completed KK-joint mesh	77
Figure 5.1	Yield line pattern of an uncracked RHS T-joint under axial load	85
Figure 5.2	Yield line pattern of a RHS T-joint containing surface crack under axial load	86

Figure 5.3	Yield line pattern of a RHS T-joint containing through-thickness crack under axial load	87
Figure 6.1	Fatigue experiment by Chiew et al. (2002, 2003)	101
Figure 6.2	The actuators for applying the fatigue load	101
Figure 6.3	The clay model from the crack surface (Chiew et al., 2003)	102
Figure 6.4	The cross section of the clay model used to measure the curved crack (Chiew et al., 2003)	102
Figure 6.5	Test rig	103
Figure 6.6	Setup of the specimen	103
Figure 6.7	The test controller and recording system	104
Figure 6.8	The weld details (AWS, 2000)	104
Figure 6.9	The crack shape measured from the first crack of specimen T1	104
Figure 6.10	The crack shape measured from the second crack of specimen T1	105
Figure 6.11	The crack shape measured from the specimen T2	105
Figure 6.12	The crack shape measured from the specimen T3	105
Figure 6.13	The ACPD theory and notation	105
Figure 6.14	A plane view showing the cracks, ACPD probes and LVDTs location of specimen T1	106
Figure 6.15	A plane view showing the crack, ACPD probes and LVDTs location of specimen T2	106
Figure 6.16	A plane view showing the crack, ACPD probes and LVDTs location of specimen T3	107
Figure 6.18	The ACPD probes spot-welded around the weld toe	107
Figure 6.18	The U-10 and computer system set-up used to record the crack propagation	108
Figure 6.19	Set-up of the ACPD probes and LVDTs along the fatigue crack	108
Figure 6.20	The dimensions of the LVDTs before the test	109
Figure 6.21	The dimensions of the LVDTs during the test	109
Figure 6.22	The recording system set-up used during the test process	110

Figure 6.23	Load-displacement curves at the brace end	110
Figure 6.24	The final failure of specimen T1	111
Figure 6.25	The final failure of specimen T2	111
Figure 6.26	The final failure of specimen T3	112
Figure 6.27	Load-CMOD curves of specimen T1	112
Figure 6.28	Load-CMOD curves of specimen T2	113
Figure 6.29	Load-CMOD curves of specimen T3	113
Figure 6.30	The assumed crack deformation at the crack tip	114
Figure 6.31	Extrapolation of the crack mouth opening displacements	114
Figure 6.32	ACPD readings of specimen T1	115
Figure 6.33	ACPD readings of specimen T2	115
Figure 6.34	ACPD readings of specimen T3	115
Figure 6.35	The first crack surface of specimen T1	116
Figure 6.36	The second crack surface of specimen T1	116
Figure 6.37	The Crack surface of specimen T2	116
Figure 6.38	The Crack surface of specimen T3	116
Figure 6.39	A typical crack surface of the damaged T-joint	117
Figure 6.40	The cross-section of the crack surface at the deepest point	117
Figure 6.41	Locations of specimens used for the coupon tests	117
Figure 6.42	Dimensions of the coupons	117
Figure 6.43	Set-up of the coupon tests	118
Figure 6.44	Stress-strain curves of the material	118
Figure 6.45	The directions of the charpy V-notch specimens	119
Figure 6.46	The charpy V-notch specimens after the tests	119
Figure 6.47	The broken surface of the charpy V-notch specimens	120
Figure 6.48	The specimen orientation for CTOD fracture toughness test	120
Figure 6.49	The set-up of the 3PB specimen	120
Figure 6.50	The fracture surface of the 3PB specimens	121
Figure 6.51	FAD plotted using maximum crack tip driving force and minimum fracture toughness	121
Figure 6.52	FAD plotted using the crack tip driving force at the location where the crack tearing initiated and fracture toughness in	122

	the crack propagation direction	
Figure 7.1	Loading and boundary conditions	154
Figure 7.2	Stress-strain curve of the BS4360-50D structural steel	154
Figure 7.3	Original mesh of SHS T-joint	155
Figure 7.4	Doubled mesh of SHS T-joint	155
Figure 7.5	Local refined mesh of SHS T-joint	156
Figure 7.6	Comparison of SIFs calculated using different mesh schemes	156
Figure 7.7	Load-displacement curves calculated using different meshes	157
Figure 7.8	Crack tip modes	157
Figure 7.9	Surface crack tip deformation of the SHS T-joint	158
Figure 7.10	Load-displacement curves calculated using different crack tip modes	159
Figure 7.11	Through-thickness crack tip deformation in the non-linear analysis	159
Figure 7.12	Through-thickness crack tip deformation in the linear elastic analysis	160
Figure 7.13	Load-displacement curve of specimen T1	160
Figure 7.14	Load-displacement curve of specimen T2	161
Figure 7.15	Load-displacement curve of specimen T3	161
Figure 7.16	CMOD-Load curves of specimen T1	162
Figure 7.17	CMOD-Load curves of specimen T2	162
Figure 7.18	CMOD-Load curves of specimen T3	163
Figure 7.19	The through-thickness crack	163
Figure 7.20	Virtual crack extension	163
Figure 7.21	Realization of a 2-D virtual crack extension in a finite element mesh (Bakker, 1983)	164
Figure 7.22	Crack front perturbation by virtual crack extension	165
Figure 7.23	Construction of failure assessment diagram (FAD)	165
Figure 7.24	Material model and structural response of elastic-perfectly plastic material	166
Figure 7.25	Material model and structural response of elastic-plastic material	166

Figure 7.26	Twice elastic compliance criterion	166
Figure 7.27	Stress distribution around the intersection between the chord and brace for surface crack, $P/P_u=0.2$	167
Figure 7.28	Stress distribution around the intersection between the chord and brace for surface crack, $P/P_u=1.0$	167
Figure 7.29	Stress distribution of the chord face and the yield line mode for uncracked SHS T-joint under the plastic collapse load	168
Figure 7.30	Stress distribution of the chord face for SHS T-joint with surface crack under the plastic collapse load	168
Figure 7.31	Stress distribution around the intersection between the chord and brace for through-thickness crack, $P/P_u=0.2$	169
Figure 7.32	Stress distribution around the intersection between the chord and brace for through-thickness crack, $P/P_u=1.0$	169
Figure 7.33	Stress distribution of the chord face for uncracked SHS T-joint under the plastic collapse load	170
Figure 7.34	Stress distribution of the chord face for SHS T-joint with through-thickness crack under the plastic collapse load	170
Figure 7.35	Load-displacement curves for series ST1	171
Figure 7.36	Load-displacement curves for series ST2	171
Figure 7.37	Load-displacement curves for series ST3	172
Figure 7.38	Load-displacement curves for series ST4	172
Figure 7.39	Load-displacement curves for series TT1	173
Figure 7.40	Load-displacement curves for series TT2	173
Figure 7.41	Load-displacement curves for series TT3	174
Figure 7.42	Load-displacement curves for series TT4	174
Figure 7.43	The Mode-I stress intensity factor (K_I) distribution along the surface crack front for shallow crack	175
Figure 7.44	The Mode-I stress intensity factor (K_I) distribution along the surface crack front for deep crack	175
Figure 7.45	The stress distribution around the surface crack opening displacements	176
Figure 7.46	Crack tip1 stress intensity factor (K_I) distributions through	177

	the chord wall	
Figure 7.47	Crack tip2 stress intensity factor (K_I) distributions through the chord wall	177
Figure 7.48	The stress distribution at the crack tip of through-thickness crack without contact crack surface definition	178
Figure 7.49	The stress distribution at the crack tip of through-thickness crack with contact crack surface definition	178
Figure 7.50	Crack tip1 stress intensity factor (K_{II}) distributions through the chord wall thickness	179
Figure 7.51	Crack tip2 stress intensity factor (K_{II}) distributions through the chord wall thickness	179
Figure 7.52	Crack tip1 stress intensity factor (K_{III}) distributions through the chord wall thickness	180
Figure 7.53	Crack tip2 stress intensity factor (K_{III}) distributions through the chord wall thickness	180
Figure 7.54	Crack tip1 J -integral value distributions through the chord wall thickness	181
Figure 7.55	Crack tip2 J -integral value distributions through the chord wall thickness	181
Figure 7.56	Load-displacement curves for a typical through-thickness cracked T-joint	182
Figure 7.57	The SIF results along the surface crack front under brace end axial loading for a shallow crack with $a/t_0=0.4$	182
Figure 7.58	The SIF results along the surface crack front under brace end axial loading for a deep crack with $a/t_0=0.85$	183
Figure 7.59	Stress components and cylinder co-ordinates at the crack front	183
Figure 7.60	The 3-D surface crack propagation direction	184
Figure 7.61	An actual fracture crack direction compared with the numerical prediction	184
Figure 7.62	The crack tip1 SIF results across the chord wall thickness for through-thickness crack under brace end axial loading	185

Figure 7.63	The crack tip ² SIF results across the chord wall thickness for through-thickness crack under brace end axial loading	185
Figure 7.64	Load-displacement curve of a typical T-joint under different crack inclined angles	186
Figure 7.65	Elastic J -integral values of a typical T-joint under different crack inclined angles	186
Figure 7.66	Elastic-plastic J -integral values of a typical T-joint under different crack inclined angles	187
Figure 7.67	The failure assessment curves constructed using different crack inclined angles compared with the standard BS7910 (1999) Level 3A assessment curve	187
Figure 7.68	The assumed residual stress distribution in the numerical analyses	188
Figure 7.69	The J -integrals of the specimen T2 with and without residual stresses	188
Figure 7.70	The failure assessment curves for series ST1 using P_u as the plastic collapse load	189
Figure 7.71	The failure assessment curves for series ST1 using P'_u as the plastic collapse load	189
Figure 7.72	The failure assessment curves for series ST2 using P_u as the plastic collapse load	190
Figure 7.73	The failure assessment curves for series ST2 using P'_u as the plastic collapse load	190
Figure 7.74	The failure assessment curves for series ST3 using P_u as the plastic collapse load	191
Figure 7.75	The failure assessment curves for series ST3 using P'_u as the plastic collapse load	191
Figure 7.76	The failure assessment curves for series ST4 using P_u as the plastic collapse load	192
Figure 7.77	The failure assessment curves for series ST4 using P'_u as the plastic collapse load	192
Figure 7.78	The failure assessment curves for series TT1 using P_u as the	193

	plastic collapse load	
Figure 7.79	The failure assessment curves for series TT1 using P'_u as the plastic collapse load	193
Figure 7.80	The failure assessment curves for series TT2 using P_u as the plastic collapse load	194
Figure 7.81	The failure assessment curves for series TT2 using P'_u as the plastic collapse load	194
Figure 7.82	The failure assessment curves for series TT3 using P_u as the plastic collapse load	195
Figure 7.83	The failure assessment curves for series TT3 using P'_u as the plastic collapse load	195
Figure 7.84	The failure assessment curves for series TT4 using P_u as the plastic collapse load	196
Figure 7.85	The failure assessment curves for series TT4 using P'_u as the plastic collapse load	196
Figure 7.86	The failure assessment curves constructed using different crack inclined angles using P'_u as the plastic collapse load	197

NOTATIONS

The notations used in the main text are briefly defined in the alphabetical order in the following list. A symbol whose usage is confined to only a specific analysis is omitted here, and it is defined at the appropriate point in the text.

a	= maximum crack depth
b_0	= chord width
b_1	= brace width
d	= crack depth along weld toe
d_c	= vertical crack mouth opening displacement
d_{ic}	= inclined crack depth
g	= gap distance of gaped K-joint
h_0	= chord height
h_1	= brace height
h_w	= weld height
J	= J -integral
J_e	= elastic J -integral
J_{ep}	= elastic-plastic J -integral
J_{mat}	= J -integral fracture toughness
K	= 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_{eff}	= effective stress intensity factor
K_{IC}	= plane strain stress intensity factor fracture toughness
K_{mat}	= stress intensity factor fracture toughness
K_r	= fracture ratio using stress intensity factor
l_1, l_2, l_3	= direction cosines of the local coordinate axis with X-axis direction

l_c	= horizontal crack mouth opening displacement
l_{c1}	= crack length along the weld toe parallel to the chord side wall
l_{c2}	= crack length along the weld toe perpendicular to the chord side wall
l_{cr}	= crack length along the weld toe
L_r	= ratio of applied load to plastic collapse load
m	= constraint factor
m_1, m_2, m_3	= direction cosines of the local coordinate axis with Y-axis direction
n	= number of the cracks
n_1, n_2, n_3	= direction cosines of the local coordinate axis with Z-axis direction
P	= applied load
P_u	= plastic collapse load considering the influence of welds
P'_u	= plastic collapse load neglecting the influence of welds
t_0	= chord wall thickness
t_1	= brace wall thickness
t_w	= weld thickness
T	= chord thickness
α_{cr}	= polar angle of the point at the weld toe
α_{cr1}	= polar angle of crack tip1
α_{cr2}	= polar angle of crack tip2
β	= brace to chord width ratio
2γ	= chord width to chord wall thickness ratio
η	= brace height to chord width ratio
δ_c	= critical crack tip opening displacement
δ_{CMOD}	= crack mouth opening displacement (CMOD)
δ, δ_{CTOD}	= crack tip opening displacement (CTOD)
δ_{IC}	= plane strain crack tip opening displacement fracture toughness
δ_{mat}	= crack tip opening displacement fracture toughness
δ_r	= fracture ratio using CTOD parameters

ε	= principal strain
φ	= tangent direction angle of the weld toe
ψ	= dihedral angle of chord and brace
ν	= Poisson's ratio
θ	= inclined angle of the crack
σ	= principal stress
σ_y	= yield stress
τ	= brace wall thickness to chord wall thickness ratio

CHAPTER 1

INTRODUCTION

1.1 Background

The applications of structural hollow sections (HSS) nearly cover all fields of engineering. Sometimes, hollow sections are used because of the beauty of their shape in expressing lightness, and in other cases their geometrical properties determine their usage. For example, hollow sections are used in buildings for the columns and lattice girders or space frames for the roofs, in bridges for the truss or arch, in offshore structures for the supporting structure, the jacket or tower, in electrical transmission towers for the members of space frame, and in the agricultural field, mechanical engineering jibs and cranes, and others (Wardenier, 2002).

There are two main types of hollow sections, one is circular hollow section (CHS), and another is square and rectangular hollow section (SHS and RHS). CHS has a more efficient shape and lower drag coefficients resulting in lower forces due to wave loading. Hence, most offshore structures are fabricated using CHS, and research in the field of CHS joints is very extensive (Berge and Tubby, 1994; Stacey et al., 1996). SHS and RHS are the youngest members in the family of steel sections, and they have grown up to be a respectable member of the steel society under the endeavour of researchers (Wardenier, 2002). Their industrial production started as late as in 1959 in England. They possess practically all the favourable structural qualities of CHS in various degrees, such as high radii of gyration about both axes, low surface, lack of ledges and edges, closed aerodynamic shape, and aesthetic appearance. However, the distinct advantage of applying RHS in lieu of CHS emerges in the field of fabrication, where the facility of end preparation with flat cuts for RHS is significantly easier and more economical than that with profile cuts for CHS. They are therefore well suited for application on topside structure of offshore platforms as well as for onshore uses (Berge et al., 1995), such as trusses (Fig. 1.1), space frames (Fig. 1.2) and bridges (Fig. 1.3). For an optimal cost

effective design, it is essential that the connections are made without any stiffening plates.

The researches on the ultimate strength and fatigue for RHS joints have been carried out for the past decades extensively (Packer et al., 1992; Zhao et al., 2001). The equations for the static strength of uncracked joints are presented as design guidances in CIDECT (Packer et al., 1992) and some other codes, such as API RP2A (1993) and AWS (2000). However, during the service life of a welded structure, defects may be introduced by environment fatigue loading or by accidental damage, e.g. impact and earthquake. When the defects are found, the structures have to be assessed and checked whether it still safe or unsafe.

Fracture mechanics is an important tool in performing a critical assessment of defects in steel welded structures. The main objective of this assessment method is to establish the maximum tolerable defect size which would not compromise the service requirements of the cracked structure. At the design stage, the ability to evaluate the tolerance of a structure to possible defects may be used to optimize the design with respect to properties of the material, geometric shape, and ease of inspection. During operation, an assessment may be used to reassess a structure that has been found to contain defects, thus possibly determining the reliability of the structure and the usage of the structure with reduced life span. The assessment can help in making rational repair/no-repair decision and improving the inspection strategy. The potential economic consequence of this critical decision for structures can be very great (Harrison, 1982). There has, of course, been considerable effort in this field for the past decades, resulting in several theories describing the behaviour of such damaged structures.

The most widespread and successful assessment approach is the failure assessment diagram (FAD) approach. Based on this approach, API RP579 (2000), BSI Document BS7910 (1999) and the R6 (2001) give guidances for assessing the acceptability of defects in welded structures. This approach simplifies the treatment of a structure containing defects by bypassing the post yield fracture mechanics theories. It assumes that failure occurs by one of the two mechanisms. The first is

the fracture failure under conditions where linear elastic fracture mechanics (LEFM) can describe the behaviour adequately. The second is the ultimate plastic collapse of the structure taking account of the presence of the crack. It is realized that there is a transition region between these two criteria. Therefore, an interpolation curve is constructed to connect the two assessment lines as shown in Fig. 1.4 (Harrison et al., 1981). The distinct advantage of this approach is that each criterion is sufficiently well understood that it can be used to predict the full-scale behaviour of the cracked joints (Dowling and Townley, 1975). Although these codes and standards provide guidance in the form of detailed procedures for fatigue and fracture assessments, these are intended for general applications and do not necessarily give optimal solutions for all types of structures, such as tubular joints.

In offshore engineering, the cracked CHS joints have been studied extensively (Kristiansen and Turner, 1993; Cheaitani and Burdekin, 1994; Stacey et al., 1996; Burdekin, 2001) for the past decades, and these results have been used as a basis for defect assessment of cracked tubular joints using BS7910 (1999). But the published information for cracked square and rectangular hollow section joints is very rare. Because of the geometrical difference between the two types of tubular joints, the fatigue behaviour of the SHS joints is quite different from that of CHS joints (Chiew et al., 2003). The fatigue crack generally initiates from one of the brace corners in the chord face where the hot spot stress is located. After the crack has propagated through the chord thickness, the geometry of the joint is not symmetric, and the stress distributions around the two crack tips are different, and hence the fatigue crack propagation rates of the two crack tips are different too, resulting in an unsymmetrical fatigue crack propagation. Furthermore, the crack shape is inclined and slightly curved (Chiew et. al., 2002). Because there are four corners at the brace to chord intersection, several large fatigue cracks may be produced at the same time, and hence multiple cracks should be considered in the analysis. Therefore, the ultimate and fracture behaviour of SHS joints containing such cracks should be investigated, and the use of the standard FAD for assessment of cracked SHS joints should be validated experimentally.

1.2 Scope and Objectives

In this research study, the main areas of interest include geometrical analyses, modelling and mesh generation of welded RHS joints with and without cracks, experimental tests and numerical study of the ultimate and fracture behaviour of cracked SHS T-joints under brace end axial loads, and the assessment of structural integrity of this type of tubular joints. Based on detailed geometrical analyses, a numerical model of the cracked RHS joints is given, and an automatic finite element mesh generation approach is proposed accordingly. The meshes of RHS T, Y, K and KK-joints are then generated using the proposed approach automatically. A set of three full-scale experimental tests of SHS T-joint with different geometric parameters are performed for validating the accuracy of the numerical models. The proposed models will be used mainly for the analysis of cracked SHS T-joints under brace end axial loads.

In accordance with the yield line theory which forms the basis of the formulae used to calculate the ultimate strength of uncracked RHS T-joints (Wardenier, 1982), the formulae for the plastic collapse load of cracked SHS T-joints under brace end axial loads is derived, and this formulae is validated numerically by parametric studies. Hereafter, the two assessment parameters, the plastic collapse load and the crack driving force of cracked SHS T-joints which are loaded under brace end axial forces will be obtained by non-linear FE analyses. Based on the J -integral approach, the assessment curves of cracked SHS T-joints will be generated and compared with the standard BS7910 (1999) Level 3A FAD assessment curve.

The objectives of this research are as follows:

- To analyse the geometrical model of cracked SHS joints including welds and cracks.
- To develop a consistent 3-D welded joint model that complies with the AWS (2000) welding specifications.
- To develop a consistent crack model for surface and through-thickness cracks.
- To design and develop a special-purpose automatic mesh generator for welded RHS T, Y, K and KK-joints with and without cracks.

- To perform the full-scale experimental tests to validate the proposed numerical models.
- To deduce the plastic collapse load formulae of cracked SHS T-joints under brace end axial loads using the yield line theory and numerical parametric studies.
- To construct the FAD of SHS T-joints containing cracks using the J -integral approach, and compared with the standard BS7910 (1999) Level 3A FAD.
- To extend the used of standard BS7910 (1999) Level 3A FAD to the cracked SHS T-joints under brace end axial loads by giving some recommendations about the use of the plastic collapse load.

The main contributions of this research are as follows:

- The full-scale experimental tests of SHS T-joints under brace end axial loads.
- The fatigue crack model of the SHS T-joints.
- The automatic mesh generation procedure for RHS joints.
- The parametric study of the fracture and ultimate behaviour of SHS T-joints under brace end axial loads.
- The validation for the use of standard BS7910 (1999) failure assessment diagram (FAD) for structure integrity assessment of SHS T-joints containing defects.

The main originalities of this research are as follows:

- The method to generate the numerical model of RHS joints containing cracks.
- The special test method to study the fracture and ultimate strength of cracked SHS T-joints under brace end axial loads.
- The formulae to calculate the plastic collapse load of the SHS T-joints under brace end axial loads.
- The recommendation to assess the integrity of SHS T-joints containing defects using the FAD method.

Fig. 1.1 A truss of HSS helped raising the Civil War submarine Hunley from the ocean floor (“HSS Literature,” 2004)

Fig. 1.2 Lattice girder of square hollow sections in a roof structure
(Rondal et al., 1996)

Fig. 1.3 Cycle path bridge (Packer et al., 1992)

Fig. 1.4 Failure assessment diagram (FAD) (Harrison et al., 1981)

CHAPTER 2

LITERATURE REVIEW

2.1 Square and Rectangular Hollow Section Joints

Space frames made up of tubular members are generally connected by welding them at the joints or “nodes”. Within a joint or a “node”, the main member is defined as the chord, while the other members which intersect with the chord, are called the braces (Fig. 2.1). Research on the static strength of these nodal joints is very important because they are the weakest part of the welded structure. In accordance with CIDECT (Zhao et al., 2000) guides, the uniplanar joints are classified as T(Y), X and K-joints, while the multiplanar joints are referred to as TT, XX and KK-joints (Fig. 2.2). To describe the geometry of SHS and RHS joints, the joints are commonly defined by some standard variables (Zhao et al., 2000), namely, $\beta = b_1 / b_0$, ratio of brace width to chord width; $\eta = h_1 / b_0$, ratio of brace depth to chord width; $\tau = t_1 / t_0$, ratio of brace wall thickness to chord wall thickness; and $2\gamma = b_0 / t_0$, ratio of chord width to chord wall thickness.

2.2 General Design Philosophy of SHS and RHS Trusses

The elastic analysis of RHS truss is frequently performed by assuming that all members are pinned connections (Packer and Henderson, 1997). It is recommended that the small moments due to the eccentricity of the brace to chord connection can be neglected as far as the nodal eccentricities between the centrelines of intersecting members are kept within the limits stipulated in the design guides (Packer et al., 1984).

The weld connecting the brace members to the chord member can be of a fillet weld, a butt weld, or a combination of the two. The weld should be designed to give a resistance that is not less than the bracing member capacity. It is generally more economical to use fillet weld than butt weld. However, the upper limit on throat of leg size for fillet weld constrains the usage of this type weld within the thin wall members. Because of the difficulty of welding at the heel of a bracing member at

low θ values, a lower limit for the applicability of the connection design rules given herein has been set at $\theta=30^0$ (Packer et al., 1992). As an alternative, the butt weld is used in the thicker wall thickness or lower dihedral angles.

2.3 Failure Criteria of SHS and RHS Tubular Joints

Tubular joints are subjected to loads on both the brace and chord. Based on different combinations of loads on the brace and chord, basic load conditions are classified into axial compression and tension (AX), in-plane bending (IPB) and out-of-plane bending (OPB), as shown in Fig. 2.3. In most cases, the failure of joint is controlled by brace loading.

A typical load-displacement curve for a T-joint is shown in Fig. 2.4. For the joint under compression, the failure load is the ultimate load which is the peak load in the Load-Displacement curve. For the joint under tension, three possible failure criteria can be observed, namely, ultimate load, load at a certain deformation limit, and load at the observation of the first crack for uncracked joints or load at the beginning of crack propagation for cracked joints. The widely accepted criterion of failure is the ultimate load. In some cases, researchers have defined the failure criterion differently. Yura et al. (1980) proposed that the minimum loads associated with the three possible failure criteria should be used to define the failure of the joint.

2.4 Failure Modes of RHS Joints

Past experimental works on RHS welded truss connections has shown that the failure modes of a RHS joints depend on the joint type, the geometric variables and the loading conditions (Wardenier, 1982). The possible failure modes of RHS joints are described in Fig. 2.5 where Mode A is a plastic failure of the chord face, Mode B is a ductile shear failure of the chord wall, Mode C is a failure at the weld at the base of the tension brace, Mode D is a local buckling of compressive areas in the bracing, Mode E is a local buckling of the chord walls, Mode F is a shear failure of the chord between the adjacent braces, and Mode G is a local buckling of the chord face.

For compression loaded RHS or SHS T-joints, the failure mode is associated with plastic deformation of the chord cross-section (Mode A) or local buckling (Modes D, E, G). Local buckling of the chord occurs only for high γ values, particularly with chord loading. For tension loaded T-joints where the brace width to chord width ratio β is less than 0.85, the chord yields around the brace and chord intersection, and the cross-section distorts at the top chord surface (Mode A). As the load increases, a crack may be initiated, and this eventually leads to rupture of the brace from the chord (Mode B). For higher values of β close to 1.0, local buckling of the chord side wall (Mode E) is observed in practice. For the cracked T-joints, the brittle fracture or ductile tearing may occur before the chord failure. Hence, the fracture failure of the cracked joints must be considered at the same time.

2.5 Analytical Models to Predict Ultimate Strength of RHS Joints

In the research of ultimate strength of RHS joints, the prime aim is to derive simple design rules to be followed when designing RHS joints. The early research on welded RHS joints took place principally at Sheffield University by Eastwood and Wood (1970). They described the problem as complicated and involving various unknowns which must be investigated to determine the effect of these parameters on how the load is transmitted from the bracing members to the chord member, what deflections are expected in the face of the chord member, and what is the best connections arrangement to strengthen the RHS joints. Analytical models are used to describe the joint behaviour, and gives useful information regarding the influencing parameters. This information, combined with experimental evidence, leads to the expressions for the joint strength.

2.5.1 Yield Line Model

Based on the test results, several empirical ultimate strength formulae were proposed by Eastwood and Wood (1970). To provide information regarding the ultimate strength of the RHS joints accurately, the theoretical approach became necessary. Johansen's (1962) yield line theory provided a reasonable tool which was adopted by Jubb and Redwood (1966) for RHS T-joints loaded locally by an axial force or an in-plane-bending moment through the brace as shown in Fig. 2.6. The proposed yield line patterns for the axial load and in-plane-bending moment

cases are shown in Fig 2.7. Based on the lower-bound solutions and considering the alternative yield line patterns shown in Fig. 2.7, the ultimate strength of RHS T-joints was given as

a) for the case of axial load only

$$\frac{N_y}{8m_p} = \frac{\pi}{2} - \alpha + \tan \alpha + \frac{\beta}{1-\beta} \cos \alpha + \frac{\eta}{1-\beta} \quad (2.1)$$

b) for the case of bending moment only

$$\frac{M}{2b_0m_p} = 1 + 2\eta \left[\frac{\pi}{2} - \alpha + \tan \alpha + \frac{\beta}{1-\beta} \cos \alpha + \frac{\eta}{1-\beta} \right] \quad (2.2)$$

where m_p is the full plastic moment per unit width of plate, and α is obtained from

$$\sin \alpha = -\left(\frac{1-\beta}{2\beta} \right) + \sqrt{\left(\frac{1-\beta}{2\beta} \right)^2 + 1} \quad (2.3)$$

They compared their theoretical results with the experimental measurements carried out by Duff (1963) and Redwood (1965), and found out that the estimated collapse loads showed some agreement with the measured loads within a certain deflection range for connections with relatively smaller braces, while as the brace became larger the estimated collapse load became conservative.

Wardenier (1982) applied the yield line method again for RHS T-joints. It was found that the simplified yield pattern shown in Fig 2.6(a) gave only 1 to 7% higher estimated strength than the most complicated pattern with yield fans, as shown in Fig. 2.6(c). Wardenier (1982) concluded that for joints with a low β ratio, the joint strength can be conservatively estimated on the basis of the simple yield line analysis. Therefore, for T, Y and X-joints with a low β ratio, Equation (2.1) becomes

$$N_y = \frac{\sigma_{e0} \cdot t_0^2}{1-\beta} \left[\frac{2\eta}{\sin \theta} + 4\sqrt{1-\beta} \right] \frac{1}{\sin \theta} \quad (2.4)$$

where N_y is the axial load in brace for yield strength of the joint, and σ_{e0} is the yield stress of chord member.

Wardenier (1982) also presented the following equation to calculate the yield load capacity for a symmetrical K-joint using the simplified push-pull joint mechanism as

$$N_y = \frac{\sigma_{e0} \cdot t_0^2}{1 - \beta} \left[\frac{2\eta}{\sin \theta} + \frac{g}{b_0} + \frac{b_0}{2g} (1 - \beta) + 2\sqrt{1 - \beta} \right] \frac{1}{\sin \theta} \quad (2.5)$$

where g is the gap size.

Mouty (1978) adopted the yield line theory for Warren truss gapped joints. He found that the yield line approach underestimates the collapse load of the joint especially for high β values. Packer et al. (1980) presented a generalized method covered all gapped joint configurations. They included the effect of the chord end axial force on the yield line pattern. In addition, the assumption that the brace at the connection to the chord face remain rigid was examined by then and found to be valid.

As the width ratio β approaches 1.0, problems arise if the strength of HSS joints is estimated using the yield line approach, because the yield line equations predict infinitely strong joints. In reality the mode of failure will change and an alternative analytical model has to be considered.

2.5.2 Punching Shear Model

When the brace is pulled away from the chord connecting face, failure can be induced by cracking, and eventually rupturing the chord face. Based on the punching shear model shown in Fig. 2.8 for X and Y-joints, and on the assumption that the shear stress is uniform around the periphery of the brace, Wardenier (1982) expressed the punching shear resistance as

$$N_u = \frac{\sigma_{e0}}{\sqrt{3}} \cdot t_0 \cdot l_{\text{eff}} \cdot \frac{1}{\sin \theta} \quad (2.6)$$

with an assumed yield punching shear of $\frac{\sigma_{e0}}{\sqrt{3}}$ and effective punching shear area of

$t_0 \cdot l_{\text{eff}}$, where

$$l_{\text{eff}} = 2 \left(\frac{h_1}{\sin \theta} + 2b_e \right). \quad (2.7)$$

Due to the non-uniform stiffness along the intersection perimeter, the entire perimeter can not always be considered to be fully effective. However, when the b_0/t_0 ratio of the chord is low, the perimeter is fully effective. For joints with higher b_0/t_0 ratio, the effectiveness also depends on the width ratio β . If a yield line pattern occurs at the top chord face before punching shear failure, the stress is distributed more uniformly and consequently the effective punching shear area becomes larger. Therefore, the value of b_e has to be determined experimentally (Wardenier, 1982).

2.5.3 Effective Width Model

In joints with brace of relatively low thickness, the brace itself can be critical in comparison with the chord (Wardenier, 1982; Packer et al., 1984). The joint strength for the effective width failure (Mode C) can be expressed in a similar way to that of punching shear, which in this case is related to the brace dimensions and material properties as

$$N_u = \sigma_{ei} \cdot t_i \cdot l_{\text{eff}_b} \quad (2.8)$$

where σ_{ei} is the yield stress of brace i , l_{eff_b} is the effective perimeter length of the brace, and $l_{\text{eff}_b} = (2h_i - 4t_i + 2b_{ei})$. b_{ei} is the effective width of the brace i .

2.5.4 Chord Wall Bearing or Local Buckling

In T, Y, and X-joints with a high β ratio, the joint can fail by yielding or instability of the side walls of the chord as shown in Fig. 2.9. Local buckling criteria were developed by Packer (1978), but it can only be used with the aid of a computer. The bearing capacity of the two chord side walls is given by (Wardenier, 1982) as

$$N_y = \frac{2\sigma_{e0}t_0}{\sin \theta} \left(\frac{h_1}{\sin \theta} + 5t_0 \right). \quad (2.9)$$

Based on the elastic plate buckling equation (Timoshenko, 1961) for an infinite long plate with simple supported edges and subjected to opposing point loads (Fig. 2.10), Wardenier (1982) presented one additional analytical method for the

calculation of the buckling strength. Local buckling can occur in the compression brace member, particularly in joints with thin walled brace, due to the moments introduced by the non-uniform stiffness distribution at the intersection or the deflection of the top face of the chord member.

2.5.5 Shear Yield Model

For gap joints with β ratios of 0.7 to 1.0, a shear failure always occurs (Wardenier and Stark, 1978), this mode of failure is also observed in joints with a rectangular chord section having $h_0 < b_0$. For T and Y-joints this will occur on either side of the brace, or for gap joints between the braces as shown in Fig. 2.11.

Analytical lower bound expressions have been proposed by the IIW (Packer, 1984) using the basic interaction formulae for plastic design. The basic yield shear load in a cross section is given as

$$Q_p = \frac{\sigma_{e0}}{\sqrt{3}} \cdot A_Q \quad (2.10)$$

where A_Q is the chord area that includes the sidewalls and part of the flanges. In various publications (Wardenier and Stark, 1978; Wardenier and Davies, 1981; Wardenier, 1982) proposed the following equation which has been confirmed by several test results, and it is expressed as

$$A_Q = 2t_0(h_0 + 2t_0) \quad (2.11)$$

The CIDECT (Packer et al., 1992) design guides for RHS joints gives the expression of A_Q as

$$A_Q = (2h_0 + \alpha b_0) \cdot t_0 \quad (2.12)$$

where $\alpha = \sqrt{1 / \left(1 + \frac{4g^2}{2t_0^2}\right)}$. This simply means that the vertical component of the axial load in the brace must not exceed the shear strength of the chord member.

2.6 Research on Strength of Cracked Tubular Joints

In the normal design philosophy, the applied loads are compared and check against the yield stress of the material and the ultimate strength of the welded joints.

As long as the latter exceeds the applied loads, the component or structure is regarded as safe. The implicit background assumption is that the component or structure is defect-free. Equations for the static strength of uncracked tubular joints are presented in the standard codes, such as HSE Guidance (1995), CIDECT Guidance (Packer et al. 1992) and API RP 2A (2000). However, during the service life of structures, defects may be introduced by environmental fatigue loading or by accidental damage, e.g. impacts and earthquakes. The static strength of a cracked tubular joint would be expected to be lower than that of the corresponding intact perfect joint. The acceptance of the defects affecting the integrity of such structures is an important research field for many years now.

Following major research programmes into the fatigue performance of tubular joints in the 1970s, some limited investigations were also carried out into the strength of cracked joints by testing the residual strength of the fatigue specimens, notably by Kurobane et al. (1970) and by Gibstein and Moe (1986). The tests by Gibstein and Moe (1986) were mainly on T-joints under axial tension, and are notable for using a combination of materials and test temperature to produce failure by brittle fracture. Tests designed to produce failure by brittle fracture were also carried out by Machida et al. (1987).

A series of research programmes into assessment of defects in tubular joints was performed in UK. (Burdekin and Frodin, 1987; Cheaitani and Burdekin, 1994; Laham and Burdekin, 1995; Burdekin and Yang, 1997; Hadley et al., 1998). These programmes were the first serious effort to combine finite element analysis, fracture mechanics procedures and model scale testing to provide guidances on the assessment of the effect of cracks on the ultimate strength of defective tubular joints. The experimental tests on T-joints (Burdekin and Frodin, 1987) showed failure by ductile tearing and it provided the first demonstration of the significant contribution of Mode-III loading in T-joints with through thickness cracks under tension loading. The experimental test on 45° K-joints (Cheaitani and Burdekin, 1994) demonstrated that for this configuration, the effect of the presence of the crack at the tension brace weld toe was to undermine the support provided by the tension brace against buckling failure of the chord under the compression brace. Once this buckling

failure started to occur, the severity of conditions at the crack tip increased substantially and tearing started to develop.

Combined with the finite element analyses, the above investigations showed a progressive reduction in the ultimate strength of the cracked joints relative to the base line of an uncracked joint of the same geometry and loading configuration. They (Burdekin, 2001) recognized that there was likely to be a significant effect of absolute size on the possible incidence of fracture failure as opposed to plastic collapse and were concerned to provide simple recommendations which could be used as an input for fracture mechanics assessments. They therefore suggested that a safe assessment (lower bound) of the plastic collapse strength at a given value of crack length could be made by applying a reduction factor to the HSE characteristic strength formulae (HSE, 1995) for the uncracked geometry but that fracture must be considered separately. This reduction factor (RF) for plastic collapse was recommended as follows:

$$RF = \left[1 - \frac{\text{cracked area}}{(\text{intersection length} \times t_0)} \right] \left(\frac{1}{Q_\beta} \right) \quad (2.13)$$

where Q_β is geometry factor. This has been adopted by BS7910 (1999) to assess the tubular joints in offshore structures.

Based on this plastic collapse load, a recommended procedure for defect assessment in offshore structures was published by Burdekin and Cowling (1998). A number of experimental and numerical programmes have been carried out to extend and validate these initial findings (Cheaitani, 1992; Skallerud et al., 1994; Hyde et al., 1994; AL Laham and Burdekin, 1995; Ting and Moffat, 1995; Klasen and Wastberg, 1997; Hadley et al., 1998; Talei-Faz et al., 1999). Work to investigate the significance of mixed mode loading was carried out by Burdekin and Yang (1997) leading to the recommendation of using fracture mechanics treatments for taking account of mixed mode loadings in tubular joints. All of the above works described earlier are concerned with the behaviour of brace to chord connections with cracks present in the chord thickness around part of the weld toe perimeter.

A summary of all the experimental tests and finite element analyses available on the ultimate strength of cracked tubular brace to chord joints is given (Burdekin, 2001). Reviews of the work in this area show that all the numerical and experimental research works have been concentrated on CHS joints. Very little information about RHS joints with cracks has been published so far. Burdekin and AL Laham (1993) investigated the RHS fillet welded K-joints with gap experimentally and numerically. It was found that the reduction in strength was closer to the punching shear area reduction for the higher β ratios but tended to be closer to the chord wall area reduction for the lower β ratios. However, it was shown experimentally that there was an almost negligible effect of the surface crack at room temperature on the ultimate strength of the joints, and they indicated that more experimental work is required in order to check the influence of surface cracks on the ultimate strength for the whole range of geometries at room and low temperature.

2.7 Fracture Mechanics Approach for Assessments of Welded Joints

If a real or assumed crack or crack-like flaw affects the load carrying capacity of cracked RHS welded joints, fracture mechanics has to be applied accordingly. Then the comparison between the applied force and the material strength has to be carried out on the basis of crack tip parameters. As a result, the fracture behaviour of a component can be predicted in terms of a critical applied load or a critical crack size.

Three parameters have dominated the application of fracture mechanics to metal structures in the second half of the twentieth century. These are the stress intensity factor (K), developed by Irwin (1957), the crack tip opening displacement (CTOD), proposed by Wells (1961) and Cottrell (1961), and a particular line contour integral (J), published by Rice (1968). Of these, CTOD has become the most widely used fracture mechanics parameter in practice. The popularity of CTOD may be explained by its physical simplicity and the early development of relatively simple and practical CTOD relationships for the avoidance of brittle and

ductile fracture in even the most complex welded metal structures (Burdekin and Dawes, 1971; Dawes, 1974; Harrison et al., 1979).

Standardised solutions for the crack tip parameters are available for test specimens which are used for measuring the material's resistance to fracture. As long as the deformation behaviour of the structural component is still linear elastic, then the relevant applied parameter in the component is K . Comprehensive compendia of K factor solutions exist in the handbooks and as computer programs. If the component behaves in an elastic-plastic manner, the situation is much more complex because the crack tip loading is additionally influenced by the deformation pattern of the material as given by its stress-strain curve. This makes the compilation of such handbook solutions an expensive task. Generally, finite element analyses have to be carried out (Zerbst et al., 2000).

2.7.1 Stress Intensity Factor Approach

The stress intensity factor K (Irwin, 1957) describes the amplitude of the stress singularity near the tip of a crack in a component subjected to predominantly linear elastic stresses. For tubular joints, the applied stress intensity factor depends on the geometry of the joints and load condition. Generally, it can be expressed as (Chong Rhee et al., 1991; Chiew et al., 2003)

$$K_e = f_g f_s f_i f_\theta \sigma_n \sqrt{\pi a} \quad (2.14)$$

where, f_g is the joint geometry factor, f_s is the crack size factor, f_i is the joint and crack coupling factor, f_θ is the influence factor of the dihedral angle between the brace and the chord, σ_n is the nominal stress. Unstable crack extension starts when the applied K is equal to the fracture toughness

$$K_e = K_{\text{mat}} \quad (2.15)$$

2.7.2 Crack Tip Opening Displacement (CTOD) Approach

In most metallic structures, crack extension is preceded by sufficient plastic deformation ahead of the crack tip so as to invalidate a strict LEFM description of fracture toughness. However, the plastic deformation causes crack tip blunting and crack tip opening, as indicated schematically in Fig. 2.12. This observation led

Wells (1961) and Cottrell (1961) to propose that the CTOD could be used to quantify crack extension. Thus, CTOD is used in elastic-plastic fracture mechanics (EPFM) as a complementary parameter to K in linear-elastic fracture mechanics (LEFM), and can be used for a considerably wider range of crack sizes and stresses.

The earliest analytical solutions for the applied CTOD is an elastic-perfectly plastic material with a through thickness crack as depicted in Fig. 2.13, the applied CTOD is given by Burdekin and Stone (1966) as

$$\delta_I = \left(\frac{8\sigma_y a}{\pi E} \right) \left[\ln \sec \left(\frac{\pi \sigma}{2\sigma_y} \right) \right] \quad (2.16)$$

where E is Young's modulus and σ_y is the yield stress. Thus the critical combination of stress and crack size for crack extension under EPFM conditions can be calculated from

$$\delta_I = \delta_{mat} \quad (2.17)$$

Subsequently, many experimental, analytical, and numerical solutions for different geometries and work hardening materials have been given (Ewalds and Wanhill, 1984; Berge et. al., 1988; Anderson and Dodds, 1991; Fujikubo et. al., 1991a, 1991b, 1991c).

2.7.3 J -Contour Integral Approach

Because it is generally too difficult to obtain rigorous analytical solutions for cracked components in actual design, analysts have modelled the material behaviour by assuming that they consist of linear and non-linear parts. This led to line and surface integrals being used to quantify the amplitude of a stress and strain singularity ahead of a crack tip. One of these to gain importance in engineering fracture mechanics is the result of path independent line contour integral called J -integral (Rice, 1968). Referring to Fig. 2.14, the J -integral can be expressed by

$$J_I = \int_{\Gamma} \left[w dy - T_i \frac{\partial u_i}{\partial x} ds \right] \quad (2.18)$$

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.

Rice (1968) also showed that for small scale yielding conditions,

$$J_I = m\sigma_y\delta_I \quad (2.19)$$

where m is a function of geometry, the elastic-plastic characteristics, and the stress-strain relationship of the material concerned. If the fracture toughness J_{mat} is used, the crack will extend when

$$J_I = J_{mat} \quad (2.20)$$

2.8 Fracture Assessment Procedures

Analytical flaw assessment methods have been developed for more than 30 years. In 1972, fracture assessments based on LEFM were first incorporated into the American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel Code (ASME, 1972). This early and very conservative fracture assessment procedure has a significant influence on the development of EPFM procedures for both nuclear and non-nuclear applications. In 1981, the General Electric Company and the Electric Power Research Institute in the USA published an EPFM fracture assessment procedure for purely ductile fracture (Kumar et al., 1981). Corresponding works in the UK led to the publication of the BSI document PD6493 (1980), which gives fracture assessments for welded structures in terms of both LEFM and EPFM.

In an attempt to produce a realistic engineering assessment method, the R6 procedure was introduced in UK in 1976 by the Central Electricity Generating Board (CEGB) (Harrison et al., 1977), and was published in 1986 (Milne et al., 1986). The failure assessment diagram (FAD) for flaw assessment was first used in this procedure. This approach was originally developed from the two-criteria assessment proposed by Dowling and Townley (1975). It enables the integrity of cracked components to be assessed through two separate calculations based on the two extremes of fracture behaviour, linear elastic and fully plastic. A design curve is used to interpolate between the two failure criteria. The relative position of the assessment point on the diagram, derived from the two separate calculations, determines the integrity of the structure. If the assessment point falls inside the failure curve, the structure is deemed safe; if the assessment point is on or outside

the curve, then failure is predicted to occur. An increased load or larger crack size will move the assessment point along the loading path toward the failure line (Fig. 2.15). The reliability of the method depends on how accurately the intersection zone is described by the failure curve which depends on the structural geometry, type of loading and crack size.

Failure is characterized by two criteria, namely, crack tip failure where failure occurs when the applied load equals the LEFM failure load, i.e.

$$K_r = \frac{K_I}{K_{mat}} = \frac{\text{load}}{\text{LEFM failure load}} = 1 \quad (2.21)$$

and failure by plastic collapse of the ligament given by

$$S_r = \frac{\text{load}}{\text{ligament area} * \bar{\sigma}} = \frac{\text{load}}{\text{plastic collapse load}} = 1 \quad (2.22)$$

where $\bar{\sigma}$ is the material flow stress.

2.8.1 CEGB R6 Revision 2 Procedure

The first version of R6 (Revision 2) obtained the elastic-plastic failure line via the Dugdale strip yield plastic zone concept for an infinitely wide tension plate (Dugdale, 1960). In terms of the J -integral, the elastic-plastic crack tip loading corresponds to

$$\frac{J}{J_e} = \frac{8}{\pi^2} \left(\frac{\sigma}{\sigma_y} \right)^2 \ln \left[\sec \left(\frac{\pi}{2} \frac{\sigma}{\sigma_y} \right) \right] \quad (2.23)$$

for small scale yielding conditions, and Rice (1968) gave the linear elastic J -integral, J_e , as

$$J_e = \frac{K^2}{E}. \quad (2.24)$$

At the critical failure point, the elastic-plastic J is replaced by the fracture resistance of the material, J_{mat} , which is related to K_{mat} by an equation similar to Equation (2.24). The applied load is normalized by the plastic collapse load, replacing σ/σ_y in Equation (2.23). This led to a geometry independent failure line, which formed the first FAD of CEGB (Harrison et al., 1977) of

$$K_r = S_r \left[\frac{8}{\pi^2} \ln \sec\left(\frac{\pi}{2} S_r\right) \right]^{-1/2} \quad (2.25)$$

In the 1980s, a number of modifications of the CEBG FAD were proposed (Rich et al., 1985; Will et al., 1987). A disadvantage of the original approach was that it was tailored to elastic-ideally plastic deformation behaviour due to its origin from the Dugdale model (Zerbst et al., 2000). Based on a power law description of the stress-strain curve, Kaiser (1984) developed separated FADs for simple bending and tension cases.

2.8.2 FAD According to R6 Revision 3

The R6 procedure, Revision 3 (Milne et al., 1988a; 1988b), is probably the most widely applied analytical flaw assessment method worldwide. It forms the basis of industrial codes and guidelines such as the British standard BS7910 (1999) for assessing flaws in weldments, the API RP579 (2000) fitness for service procedure of the American petro-chemical industry, the Swedish SAQ method (Bergman et al., 1991) and the French RSE-M codes (1997) for assessing flaws in nuclear power components and others.

This approach was based on the reference stress method that was developed by Ainsworth (1984) from expressions for the J -integral taken from the EPRI handbook (Kumar et al., 1981). The failure assessment curve was given by Ainsworth (1984) as

$$K_r = \left[\frac{E \varepsilon_{\text{ref}}}{\sigma_{\text{ref}}} + \frac{L_r^2}{2(1 + L_r)^2} \right]^{-1/2} \quad (2.26)$$

where ε_{ref} is taken from the material's true stress-true strain curve at respective values of σ_{ref} . The reference stress was defined as

$$\sigma_{\text{ref}} = L_r \sigma_y \quad (2.27)$$

where the quantity L_r is based on the yield stress, rather than the flowing stress based quantity S_r of Equation (2.25). The second term in Equation (2.26) can be interpreted as a plasticity correction in the intermediate small scale yielding region where the bulk behaviour of the component is still elastic but the ligament ahead of

the crack already experiences substantial local plasticity. In order to improve the curve fitting in real condition, the denominator $(1 + L_r)^2$ was replaced by the semi-empirical term $(E \varepsilon_{\text{ref}} / \sigma_{\text{ref}})$ (Miller and Ainsworth, 1989) producing the equation of

$$K_r = \left(\frac{E \varepsilon_{\text{ref}}}{L_r \sigma_y} + \frac{L_r^3 \sigma_y}{2 E \varepsilon_{\text{ref}}} \right)^{-1/2} \quad (2.28)$$

which is termed the material specific Option 2 of R6 Revision 3. The application of this equation requires the knowledge of complete stress-strain curve. In particular the region around the yield point has to be available in a detailed manner. However, there are many cases where this information is not available to the users. Therefore, Equation (2.28) was applied to a number of materials to generate a material independent lower bound curve of

$$K_r = (1 - 0.14 L_r^2) [0.3 + 0.7 \exp(-0.65 L_r^6)] \quad (2.29)$$

which is the more conservative Option 1 curve of R6 Revision 3.

Chell (1979) re-examined and interpreted the failure curve in terms of an equivalent J -integral, given by

$$K_r = \sqrt{\frac{J_e}{J}} \quad (2.30)$$

This enabled the failure line to be validated for structures where the J -solution was known and to be applied where toughness was known in terms of J_{IC} rather than K_{IC} . The Revision 3 Option 3 curve (Milne et al., 1988a; 1988b) is based directly on the equivalence of the assessment curve of a J -integral analysis, and this assessment line can be described by the following equation

$$K_r = (J_e / J)^{1/2} \quad \text{for } L_r \leq L_{r\text{max}} \quad (2.31a)$$

$$K_r = 0 \quad \text{for } L_r > L_{r\text{max}} \quad (2.31b)$$

where J_e and J are values corresponding to the same load, and K_r is plotted as a function of L_r . J and J_e may be obtained from available J -solutions, experiments or a full elastic-plastic analysis. This method clearly has the potential for great accuracy.

2.8.3 New Updated R6 Revision 4 Procedure

The R6 (2001) method was updated recently by the British Energy in UK to the fourth Revision which includes an update with respect to the developed European flaw assessment method SINTAP (1999). In the R6 Revision 4, slight changes were made to the R6 Revision 3 Option 1 FAD to ensure consistency with Option 2 at low L_r , and additional FADs were introduced using approximate fits to stress-strain curves in conjunction with R6 Option 2.

The Revision 4 Option 1 curve (Equation 2.29) has been replaced by the SINTAP (1999) basic curve

$$K_r = f_1(L_r) = (1 + 0.5L_r^2)^{-1/2} [0.3 + 0.7 \exp(-0.6L_r^6)]. \quad (2.32)$$

This ensures full consistency with Option 2 at low L_r , and this curve is within about $\pm 3\%$ of Revision 3 Option 1 for $L_r < 1.6$.

The so-called Approximate Option 2 curves have been added in Revision 4 as compared with Revision 3. These come from SINTAP (1999), and they do not require detailed stress-strain data and can be defined with no more data than Option 1. The Approximate Option 2 curves have been included for materials both with and without a discontinuous yield point. These curves are described as follows:

- 1) Approximate Option 2 curve, for materials with continuous yielding

For this behaviour, the failure assessment curve is given by

$$K_r = f_2^{cy}(L_r) = (1 + 0.5L_r^2)^{-1/2} [0.3 + 0.7 \exp(-\mu L_r^6)] \quad L_r \leq 1 \quad (2.33)$$

$$K_r = f_2^{cy}(L_r) = f_2^{cy}(1) L_r^{(N-1)/2N} \quad 1 < L_r < L_r^{\max} \quad (2.34)$$

where $f_2^{cy}(1)$ is obtained from Equation (2.33) for $L_r = 1$ and

$$\mu = \text{Min}[0.001E / \sigma_y; 0.6] \quad (2.35)$$

$$N = 0.3[1 - \sigma_y / \sigma_u] \quad (2.36)$$

where σ_u is the ultimate tensile strength. This corresponds to an estimated power law fit to the plastic portion of the stress-strain curve with the value of the power-law exponent, N , obtained from correlations with tensile data for a range of steels.

2) Approximate Option 2 curve, for materials with discontinuous yielding

Materials which show discontinuous yielding may be considered to exhibit a stress-strain curve consisting of three regions (R6, 2001), namely,

- elastic behaviour up to the (lower) yield stress, σ_y ;
- an increase in strain, $\Delta\epsilon$, at the stress σ_y without any increase in stress;
- further plastic straining with increasing stress up to the ultimate stress, σ_u .

For this behaviour, the failure assessment curve, $f_2^{\text{dy}}(L_r)$, is obtained from Option 2 function of Equation (2.28) as

$$K_r = f_2^{\text{dy}}(L_r) = (1 + 0.5L_r^2)^{-1/2} \quad L_r \leq 1 \quad (2.37)$$

$$K_r = f_2^{\text{dy}}(1) = \left[\lambda + \frac{1}{2\lambda} \right]^{-1/2} \quad L_r = 1 \quad (2.38)$$

where $\lambda = 1 + E\Delta\epsilon / \sigma_y$.

In the absence of detailed stress-strain data enabling Equation (2.37) and the Option 2 curve to be evaluated for $L_r > 1$, then $\Delta\epsilon$ may be estimated from

$$\Delta\epsilon = 0.0375 [1 - \sigma_y / 1000] \quad (2.39)$$

for σ_y in MPa. Application of Equation (2.39) is restricted to materials for which $\sigma_y < 946\text{MPa}$, for which the estimate of $\Delta\epsilon$ exceeds 0.2%. The curve for $L_r > 1$ may be estimated from

$$K_r = f_2^{\text{dy}}(L_r) = f_2^{\text{dy}}(1) L_r^{(N-1)/2N} \quad 1 < L_r < L_r^{\text{max}} \quad (2.40)$$

with N estimated from Equation (2.36).

The Option 3 curve, which is obtained using J -integral analysis, is the same as that in Revision 3. But based on the world-wide review over the past 14 years, comprehensive advice on derivation of the assessment curve is provided in Revision 4, such as the use of finite element methods to obtain elastic J -integral, J_e , and elastic-plastic J -integral, J . Based on the SINTAP (1999) procedure, a procedure for presenting results as a crack driving force is also provided in the Option 3 curve.

2.9 BS7910 Methodology for Fracture Assessment

The PD6493 (1991) provides guidance on assessing the significance of defects in fusion welded structures on a fitness-for-purpose basis. The guidance covers both fracture and fatigue assessments. The fracture treatment is based on the failure assessment diagram (FAD) approach which originated in the CEGB R6 procedures, and it was subsequently validated extensively using pressure vessel data (Milne et al., 1988a; 1988b). Challenger et al. (1995) published further validation studies covering a wide range of welded structures. The BSI published document (BS7910, 1999) has been consistent with the continuing developments and improvements in fracture mechanics assessment methods, and refines the approaches of the PD6493 (1991) (Wiesner et al., 2000). The document gives guidances on three levels of assessment.

Level 1 is an initial preliminary assessment to check whether particular flaws present a fracture problem, or whether the flaws can be considered safe without having to use the more complex assessment procedures. In Level 1 as shown in Fig. 2.14 where estimates of the fracture toughness K_{IC} are available, a preliminary estimation of the acceptability of a known flaw can be carried out by calculating K_r and S_r . If K_r is less than $(1/\sqrt{2})$ and S_r is less than 0.8, then the flaw is acceptable.

Level 2 is the normal assessment route for general application, which is based on the CEGB R6 Revision 3. The Level 2 method is refined to Level 2A and Level 2B; the choice between the two depends on the type of stress-strain data available for the material. Level 2B, in fact, is identical to the CEGB R6 Revision 3 Option 2 which is used if the relevant full stress-strain curve is available. In the cases where the stress-strain curve is not available, Level 2A, which is identical to the CEGB R6 Revision 3 Option 1, is used. Level 2 assessments are based on a single value of toughness. This may be a value associated with limited ductile tearing, but, for a full analysis of ductile tearing, a Level 3 approach is required.

Level 3 is appropriate for ductile materials that exhibit stable tearing, e.g. austenitic steels and ferritic steels on the upper shelf. There are three assessment

methods: Level 3A, 3B and 3C. Each method uses a different assessment line and applies a ductile tearing analysis. The use of Level 3A and 3B depends on the type of stress-strain data available as for Level 2. A novelty of Level 3 options is the R6 Revision 3 Option 3 method, which becomes Level 3C in BS7910 (1999). The FAD and crack driving force may be derived from elastic-plastic finite element analysis to give more accurate predictions of structural behaviour.

Several FE codes are now available which can deal with the evaluation of J -integral, such as the computer program ABAQUS (2002). The features in ABAQUS (2002) facilitate the construction of failure assessment diagrams for welded connections with cracks, and this will be discussed in more details in Chapter 7.

2.10 Residual Stresses

The residual stresses in a component or structure are stresses caused by incompatible internal permanent strains. Welding is one of the most significant causes of residual stresses and typically produces large tensile stresses whose maximum value is approximately equal to the yield strength of the materials being joined, balanced by lower compressive residual stresses elsewhere in the component.

Allowing for residual stresses in the assessment of service performance varies according to the failure mechanism. It is not usually necessary to take account of residual stresses in calculations of the static strength of ductile materials (TWI, 2000). Residual stresses have a major effect on fracture in the brittle and transitional regimes, and hence the stress intensity, K , or energy release rate, J , due to residual stresses must be calculated and included in the fracture assessment. In the standard failure assessment procedures, the residual stresses can be present as a secondary stress. In the calculation of the fracture ratio, K_r , a plasticity correction factor, ρ , is used to allow for interaction of the primary and secondary stress contributions, such that:

$$K_r = \frac{K_I}{K_{mat}} + \rho \quad (2.41)$$

where ρ is as defined in the standard codes, for example in BS7910 (1999) annex R.

2.11 Failure Assessment for Tubular Joints

Validation of the FAD approach for assessment of offshore tubular joints was carried out using analytical and experimental studies recently. The analytical studies, based on finite element analysis, allowed data on crack driving force parameters and plastic collapse to be generated accordingly. Such data were then used to prove that standard FAD(s), e.g. those of PD6493 (1991) were generally applicable to tubular joints (Cheaitani and Burdekin, 1994; Burdekin and Yang, 1997; Stacey et al., 1996; Hadley et al., 1998).

The experimental studies allowed data on plastic collapse to be generated from small-scale tests including comparisons of FAD-based assessments with large-scale fracture tests such as those of Department of Energy (1989), Machida et al. (1992), Cheaitani and Billington (1996), and HSE (Talie-Faz et al., 2000). Although these studies showed that FAD approach provided an adequate framework for the assessment of tubular joints, further work is still required to improve the analysis procedure and widen its range of applications. In particular, there is a need to validate the procedure for material on the upper shelf of the ductile-brittle transition curve where failure occurs by ductile tearing. There is also a need to address the influence of mixed mode behaviour and to consider tubular joints with a high β ratio under various types of brace loadings (Burdekin and Yang, 1997).

2.12 Mixed Mode Loading

Because of the complexity of the tubular joints, generally the crack under the weld toe is under mixed mode loading. Basic studies have been carried out to develop procedure for estimating the crack tip driving force under mixed mode loading by Yang (1996). A simple method has been developed to separate total J -integral into Mode-I, Mode-II and Mode-III J -integrals based on the crack tip opening displacements. The general relationship between J and CTOD is given by

$$J = m\sigma_y\delta \quad (2.42)$$

where m is a constraint factor. It was suggested that this approach can be used for any individual model,

$$J_I = m_I\sigma_y\delta_I \quad (2.43)$$

$$J_{II} = m_{II} \tau_y \delta_{II} \quad (2.44)$$

$$J_{III} = m_{III} \tau_y \delta_{III} \quad (2.45)$$

The total J value under mixed mode loading is given by

$$J = J_I + J_{II} + J_{III} \quad (2.46)$$

The value of m_I increases with increasing plasticity and can be considered to be divided into 3 groups according to load levels and plastic deformation as follows:

$$\begin{aligned} m_I < 1 & \quad \text{elastic load} \\ 1 \leq m_I \leq 2 & \quad \text{elastic and plastic load} \\ m_I = 2 & \quad \text{collapse load} \end{aligned}$$

Typical $m_I = 1.89$ for large scale yield in the plastic zone is recommended by Yang (1996), and it was found that the value of m_{II} , m_{III} appears to remain a fixed percentage of m_I at 0.43.

The mixed mode fracture toughness was studied by Yang (1996) and a test method has been developed for Mode-I/III loading based on modified compact tension specimen with initial cracks inclined at an angle to the loading direction. According to the experimental results, it was suggested that the fracture toughness for the high mixed mode loading case is about 25% lower than that for the pure Mode-I case for the particular steel tested. In the studies of the assessment of tubular joints by Yang (1996), it was found that Mode-III loading for through thickness crack is very important and the contribution for the fracture is remarkable. Therefore, it was suggested that the most appropriate method to estimate fracture behaviour of through thickness cracks is for the driving force at the crack tip to be estimated including Mode-I and Mode-III components and compared with the lowest of either Mode-I and Mode-III fracture toughness. But for surface crack, no significant effects of mixed mode loading need to be taken into account as the remaining ligament prevents any substantial Mode-III loading and the Mode-II loading does not dominate.

Fig. 2.1 Profile of RHS joint

Fig. 2.2 Uniplanar and multiplanar joint classifications

Fig. 2.3 Basic loading modes

Fig. 2.4 A typical load-displacement curve

Fig. 2.5 Failure modes of RHS joints (Wardenier, 1982)

Fig. 2.6 Applied loadings (Wardenier, 1982)

Fig. 2.7 Yield line patterns (Wardenier, 1982)

Fig. 2.8 Punching shear model (Wardenier, 1982)

Fig. 2.9 Chord side wall bearing model (Wardenier, 1982)

Fig. 2.10 Chord side wall stability model (Wardenier, 1982)

Fig. 2.11 Shear area A_Q of the chord member

Fig. 2.12 Crack tip opening displacement under EPFM conditions

Fig. 2.13 A central crack in an infinite flat plate under tension

Fig. 2.14 Contour path for determination of the J -integral

Fig. 2.15 FAD philosophy of flaw assessment

CHAPTER 3

GEOMETRICAL MODELLING OF RHS AND SHS JOINTS WITH CRACKS

3.1 Introduction

In recent years, commercial finite element software packages such as ABAQUS (2002), ANSYS (2001) and MARC (1998) have been used extensively in the analysis and computation for fracture problems. It has been shown that the numerical results obtained from these software packages have sufficient accuracy provided that the geometry of the structure can be modelled accurately. Therefore, producing accurate and precise geometrical modelling of tubular joints, especially the welds and cracks, is vital in the study of damaged welded joints.

3.2 Geometry of RHS and SHS Joints

For modelling the RHS and SHS joints, the basic dimensions of these joints defined in accordance to CIDECT (Zhao et al., 2000) and IIW (1989) specifications are shown in Fig. 3.1. A typical cross-section of a RHS connection is shown in Fig. 3.2. In accordance with the BS EN10210-2 (1997) for hot finished sections and BS EN10219-1 (1997) for cold formed sections, the outer radius of the corner R are $1.5t_1$ and $2.0t_1$ respectively, while the inner radius are $1.0t_1$. The arc angles of the corner for hot finished and cold formed sections are approximately 60° and 90° respectively. It can be seen that there is a kink at the intersection of the corner with the side of the hot finished cross-section.

3.3 Geometry of Welds

In this study, AWS (2000) specifications are used to define the geometry of welds. For the penetration butt or groove welds, AWS (2000) has given guidelines on the minimum weld thickness t_{AWS} , and the profile of the welds as shown in Fig. 3.3. In practice, the design of the weld thickness t_w is always greater than or equal to t_{AWS} , that is

$$t_w \geq t_{AWS} \quad (3.1)$$

Hence, the ratio λ is introduced to define t_w as

$$t_w = \lambda \cdot t_{AWS} \quad (\lambda \geq 1.0) \quad (3.2)$$

For comparison with experiment results, λ should be calculated according to the t_w measured from the specimens. In the numerical mode, the default λ is taken to be 1.0.

To describe the location of the full penetration welds conveniently, the fillet welds are divided into several parts (AWS, 2000) as shown in Fig. 3.4, namely,

- side – the straight parts along the chord wall where the dihedral angle is a right angle
- toe – the straight part perpendicular to the chord wall where the dihedral angle is an obtuse angle
- heel – the straight part perpendicular to the chord wall where the dihedral angle is an acute angle
- corner – the curve parts along the brace corners

The cross-section of a hot finished rectangular hollow section has kinks at the corners of the brace. But according to the actual welding procedure and the measurement of the welded specimen, the kink has not been observed along the weld toe. Therefore, the following assumptions are adopted in this study, i.e.

- 1) at the corner, the weld toe is tangent to the toe and heel as shown in Figs. 3.5(a) and (b),
- 2) the welds height h_w is the same along the perimeter,
- 3) at the toe and heel, as shown in Fig. 3.6(a) and (b) respectively, the length L are equal to

$$\begin{aligned}
 L &= \frac{t_1}{\sin(\psi)} \text{ and less than } 1.75 t_1 \text{ for } \psi > 135^\circ, \\
 L &= \frac{(t_1 + F)}{\sin(\psi)} \text{ for } 90^\circ \leq \psi \leq 135^\circ, \text{ and } F = \frac{t_1}{2} \cdot \frac{(135 - \psi)}{45}, \\
 L &= \frac{t_1}{2} + \frac{t_1}{\sin(\psi)} \text{ for } \psi \leq 90^\circ
 \end{aligned} \quad (3.3)$$

According to the BS EN10210-2 (1997) standards, the radius of the corner is given by

$$R = 1.5t_1 \quad (3.4)$$

From assumption (1), the radius of weld toe for T-joints can be deduced as

$$R_w = R[\cos(\frac{\pi}{4} - \frac{\alpha}{2}) - \sin(\frac{\pi}{4} - \frac{\alpha}{2})] + F \quad (3.5)$$

For Y and K-joints, the corner becomes a quarter-ellipse, the longer axial radius is

$$R'_w = R[\cos(\frac{\pi}{4} - \frac{\alpha}{2}) - \sin(\frac{\pi}{4} - \frac{\alpha}{2})] / \sin(\psi) + F \quad (3.6)$$

3.4 Geometry of Fatigue Cracks in RHS and SHS Joints

The fatigue crack model in the CHS joints has been studied extensively by many researchers (Wu, 1985; Sigurdsson and Torhaug; 1993; Bowness and Lee, 1995; Dover et al., 1995; Cao et al., 1998 and Lie et al., 2001). In this type of joints, the fatigue crack under the weld toe is generally modelled as a semi-ellipse in shape and perpendicular to the chord surface (Rhee and Salama, 1987; Pook, 1993; and Lie et. al., 2003). Because the geometry of the SHS and RHS joints is quite different from the CHS joints, especially the existence of the brace corner, the fatigue crack shows different curvature (Chiew et. al., 2003), and there is no benchmark of crack curvature for SHS joints available in the literature. Based on the experimental data carried out recently (Chiew et al., 2003), the geometry of these fatigue cracks under the weld toe in the SHS joints will be discussed in the following sections.

3.4.1 Fatigue Crack Initiation Point

The fatigue crack always initiates from the hot spot location in which maximum geometrical stress occurs in the connection (Berge et al., 1995; Van Wingerde et al., 1997). In the case of welded connections between hollow sections, the hot spot location is at the weld toe.

Due to the characteristic geometry of RHS and SHS joints described by IIW (1995), the stiffness distribution at the flat sidewalls is quite different from that of

cylindrical shells due to brace corners, and there are chord corners near the brace side walls (please refer to Fig. 3.7). Therefore, the stress distributions near the connection are not uniform. In the RHS and SHS joints, the stress tends to be highest near the corners of the brace, either at the brace or chord weld toe. From the earlier research works (Puthli et al., 1988; Soh and Soh, 1990; Van Wingerde, 1992; Chiew et al., 2002), it was confirmed that in the case of SHS T-joints, the hot spot stress always occurs at the four corners of the brace-to-chord intersection. In accordance with the experimental data (Chiew et al., 2002), the typical stress distributions of a T-joint under axial, in-plane-bending (IPB) and out-of-plane-bending (OPB) loads are shown in Figs. 3.8 to 3.10. It is clear that the hot spot stress is always located at the corner region. Hence, the fatigue crack always initiates from this corner of the weld toe.

3.4.2 Fatigue Crack Shape

Due to complex geometries and load conditions in the actual structures, cracks are often subjected to a superposition of normal, in-plane shear and out-of-plane shear loading, or mixed-mode loading (Lange, 1993; Pook, 1998), which can not be described sufficiently by a 2D model. Because of the influence of Mode-II and Mode-III, the crack propagation direction will be changed as shown in Fig. 3.11 (Leblond, 1993), and the fatigue crack shape is not a simple flat surface. Mixed I-III, I-II modes have been the subject of a number of investigations, e.g. Cisilino and Aliabadi (1997) and Frangi (2002), showing that the crack propagation direction is strongly dependent on the relative influence of the modes. Based on the analysis of stress and displacement fields in the vicinity of the crack front, numerous research works have been performed to predict the fatigue crack growth in different components (Swedlow, 1976; Maiti and Smith, 1983; Qian and Fatemi, 1996; Mi, 1996; Wasiluk and Golos, 2000).

Fatigue crack path under the weld toe of tubular joints was also studied in recent years to estimate accurate stress intensity factors (SIFs) along the crack front (Bowness and Lee, 1995; Pook, 2002; Chiew et al., 2003). The fatigue crack growth analysis was performed using linear elastic fracture mechanics (LEFM) method. In accordance with the numerical simulation and the experimental test for SHS T-

joints (Chiew et al., 2003), the fatigue crack curvature under weld toe was studied to produce a reasonable crack model. It is observed that the crack is inclined to the chord face, and it is slightly curved as shown in Figs. 3.12 and 3.13. The secant line, as shown in Fig. 3.13, is used to model the crack surface. The inclined angle varies from 30° to 60° , and an inclined angle of 45° was suggested by Chiew et al. (2003). Because the stress distribution is not symmetrical to the brace corner, the stress intensity factors (SIFs) close to the two crack tips are different. Therefore, the crack propagation rate is different too. As a result, the crack is not semi-elliptical in shape, as shown in Fig. 3.12. In this research works, a two halves semi-ellipse, as shown in Fig. 3.14, is used to model the crack curvature under the weld toe. Mapping this assumed crack curvature onto the curved crack surface and keeping the deepest point at the brace corner, the crack model can be obtained as shown in Fig. 3.15. On the surface, there is an ordered family of smooth curves representing successive position of the crack front, as shown by the solid lines in Fig. 3.15. Corresponding crack growth directions are represented by family of orthogonal trajectories. These are also smooth curves, as shown by the dashed lines on the Fig. 3.15. In differential geometry terms, these two families are an orthogonal net (Kreysig, 1991). From Fig. 3.16, it can be seen that the crack inclined angle θ varies as the crack depth increases.

Because the crack shape depends on many factors such as geometry of joints, load conditions, materials of the weld and chord, strain hardening and residual stresses, further studies should be carried out in the future, but the detailed investigation of the behaviour and mechanics of crack propagation are not the main objective of this research study.

Fig. 3.1 Dimensional notations used by CIDECT (1992) and IIW (1989)

Fig. 3.2 Cross-section of RHS and SHS joints

Fig. 3.3 Prequalified joint details for complete joint penetration groove welds in tubular T-, Y-, and K-joints (AWS, 2000)

Fig. 3.4 Welds of RHS and SHS joints (AWS, 2000)

Fig. 3.5(a) Weld toe profile of T-joint ($F \geq t_1 / 2$)

Fig. 3.5(b) Weld toe profile of Y and K-joints

Fig. 3.6(a) Dimensions of the weld ($\psi \leq 90^\circ$)

Fig. 3.6(b) Dimensions of the weld ($\psi > 90^\circ$)

Fig. 3.7 The corner of chord cross-section

(The stresses inside the weld toe is compressive stresses, outside the weld toe is tensile stresses)

Fig. 3.8 Principal stress distribution around the weld toe of T-joint under axial load (Tension)

(The stresses inside the weld toe is compressive stresses, outside the weld toe is tensile stresses)

Fig. 3.9 Principal stress distribution around the weld toe of T-joint under IPB

(The stresses inside the weld toe is compressive stresses, outside the weld toe is tensile stresses)

Fig. 3.10 Principal stress distribution around the weld toe of T-joint under OPB

Fig. 3.11 Crack growth development of basic fracture modes and three-dimensional superimposed Mode I+II+III loading (Leblond, 1993)

Fig. 3.12 Fatigue crack curvature under the weld toe (Chiew et al., 2002)

Fig. 3.13 Cross-section of fatigue crack under the weld toe

Fig. 3.14 Crack front extension

Fig. 3.15 Crack curvature under weld toe on a smooth curved crack surface

Fig. 3.16 Crack under the weld toe

CHAPTER 4

NUMERICAL MODELLING AND AUTOMATIC MESH GENERATION OF RHS JOINTS WITH CRACKS

4.1 Introduction

For the past decades, finite element (FE) method plays an important role in the analysis of different types of welded cracked tubular joints (Burdekin, 2001). The numerical modelling and mesh generation of CHS joints with cracks have been carried out by many researchers (Cao et al., 1997, 1998; Lee, 1999; Lee et al., 1999; Lie et al., 2000, 2001). But these works focus on the CHS joints only, and there are very few published literatures on modelling of RHS joints with cracks. Based on the detailed geometrical analysis of RHS joints with cracks discussed in Chapter 3, the welded joints will be modelled, and an automatic mesh generation procedure will be developed accordingly.

4.2 Modelling of Cracks under Weld Toe

There has already been extensive research being carried out on the prediction of stress intensity factor (SIF) of cracked tubular joints in the past decades (Du and Hancock, 1989; Chong Rhee et al., 1991; Pook, 1993; Bowness and Lee, 1995; Chiew et al., 2003, 2004). From the past literature (Yang, 1996), it has been found that an accurate crack modelling is essential in obtaining a reasonable numerical result.

The experimental observation (Chiew et al., 2003) shows that the fatigue crack is propagating on a 3-D curved surface formed inside the thickness of the chord. This invisible 3-D surface is known as the cracked surface where the crack front lies on, as shown in Fig 4.1. As discussion in Chapter 3, a straight secant line is used to model the slightly curved cross-section of the crack surface. In the local u - v coordinate system, the surface can be defined as shown in Fig. 4.2. The point W_0 (X_{W_0} , Y_{W_0} , Z_{W_0}) is located along the weld toe, and the point D can be determined according to the assumptions that it is located on the crack surface, $|\mathbf{W}_0\mathbf{D}_0| = d$,

where d is the vertical depth of crack, the angle between $\mathbf{W}_0\mathbf{D}$ and $\mathbf{W}_0\mathbf{D}_0$ is θ , and the point D is located inside the chord thickness. The coordinates of point D can be expressed as

$$\begin{aligned} X_D &= h_0/2 - d \\ Y_D &= Y_{W_0} - L_0 \cos \varphi \\ Z_D &= Z_{W_0} + L_0 \sin \varphi \end{aligned} \quad (4.1)$$

where h_0 is the chord height, L_0 equals to $d \tan \theta$, φ is the tangent direction angle of weld toe, and θ is the inclined crack angle. Hence, if the profile of weld toe is defined in accordance to AWS (2000) specifications, the crack surface can be expressed by the vector function as

$$\mathbf{r}_D = X_D \mathbf{i} + Y_D \mathbf{j} + Z_D \mathbf{k} \quad (4.2)$$

After the crack surface has been defined, the crack front defined in the $u'-v'$ plane is mapped onto this surface, and the 3-D crack front is obtained as shown in Fig. 4.3. In Fig. 4.3, the point o is the crack tip 1, the u' -axis relates to the crack propagation length, l_{cr} , and the v' relates to the crack depth d . Hence, u' and v' can be defined as the arc length of the crack along the weld toe l_{cr} and the crack depth d respectively.

For convenience, a polar coordinate system, as shown in Fig. 4.4, is used to determine the location of any point along the weld toe profile. The arc length of the crack can be given by the integral

$$l_{cr} = \int_{\alpha_{cr1}}^{\alpha_{cr}} \left| \frac{d\mathbf{r}}{d\alpha} \right| d\alpha = \int_{\alpha_{cr1}}^{\alpha_{cr}} \sqrt{\left(\frac{du}{d\alpha} \right)^2 + \left(\frac{dv}{d\alpha} \right)^2} d\alpha \quad (4.3)$$

where α_{cr} is the polar angle of the point on the weld toe with u axis.

If the two positions of the crack tips are given, the angle α_{cr1} can be determined. Substituting the arc length $l_{cr} = u'$ into equation (4.3), the angle α_{cr} can be deduced by using the binary search method. Then the coordinates of point W_0 in local $u-v$ coordinate system can be obtained accordingly as a piecewise function. The whole weld toe is divided into 8 sections by the intersection points of the corner curves with the straight lines along the brace side wall as shown in Fig. 4.5.

Because the relationship in the four quadrants is similar, the piecewise function in the first quadrant shown in Fig. 4.6 is illustrated as

$$\begin{aligned} u_{w0} &= B/2, \quad v_{w0} = B/2 \cdot \tan \alpha_{cr} & \alpha_{cr} &\leq \alpha_1 \\ (u_{w0} - u_1)^2 + (v_{w0} - v_1)^2 \cdot \sin^2 \psi &= R^2, \quad v_{w0} = u_{w0} \cdot \tan \alpha_{cr} & \alpha_1 < \alpha_{cr} < \alpha_2 \\ u_{w0} &= H/2 \cdot \cot \alpha_{cr}, \quad v_{w0} = H/2 & \alpha_2 &\leq \alpha_{cr} \end{aligned} \quad (4.4)$$

where B and H are the width and height of the weld toe respectively, u_1 and v_1 are the center coordinates of the corner, α_{cr} is the polar angle of line OW_0 with u axis, α_i are the polar angles of the intersection points in corners measured in clockwise direction, $i = 1 \sim 8$, and ψ is the dihedral angle of the brace with the chord.

Transforming the results to global coordinate system, the following relationship is obtained

$$Y_{W_0} = v_{W_0}, \quad Z_{W_0} = u_{W_0} \quad \text{and} \quad d = v' \quad (4.5)$$

Substituting equation (4.5) into equation (4.1), the coordinates of point D , which is located at the crack front, are determined as the crack front is mapped onto the three-dimensional crack surface. Hence, the crack front is simulated finally.

For a through-thickness crack, it is idealized as a rectangular crack of length l (TWI, 1999), as shown in Fig. 4.7. If the two crack tips are known, the through-thickness crack can be determined from equation (4.1) directly.

4.3 Element Selection

In the past decades, many types of element are used to model the tubular joints, such as shell elements (Lee and Wilmschurst, 1995), shell elements combined with solid elements or line spring elements to embed the crack into the joint (Berge and Haswell, 1994, Skallerud, 1995; Herion et al., 1996), and solid elements (Cao et al., 1998). Herion et al. (1996) compared the results from models with different types of elements shows that the best results are provided by models using three-dimensional quadratic solid elements for all tubes and welds. Cao et al. (1998) had indicated that three-dimensional quadratic solid elements should be used throughout cracked tubular joints with detailed modelling of cracks. Dyer (1996) carried out an

extensive study for the effect of mesh density on ultimate strength and fracture driving forces for tubular joints, and concluded that a fine mesh, with at least three layers of elements across the chord wall thickness and a similar mesh density in the brace, is required for a joint because of the large amount of local bending in the region around a crack, and the stress concentration effect at the weld toe. In this study, it is found that the quality of the elements around the crack front influences the numerical results greatly, especially in the non-linear analysis. This is due to badly distorted elements, and as a result the convergent problem would terminate the FE analysis prematurely. To obtain high quality elements around the crack front, Lie et al. (2001) used several types of elements as shown in Table 4.1 to decrease the element mesh density from very fine mesh around the crack front to coarse mesh at region far away from it gradually. The present paper adopts the same technique for modelling the mesh of cracked RHS and SHS joints.

4.3.1 Elastic Crack-tip Fields

In linear elastic fracture mechanics (LEFM), it is well known that the inverse square root singularity exists in the stress and strain field near the crack tip (Irwin, 1957). There are two widely used finite element methods to treat this singularity. The first method uses a specially refined mesh in the singularity region (Babuska and Miller, 1984; Demkowicz et al., 1983). The second method uses special singular finite element shape functions. Various singular elements had been proposed (Fix et al., 1973; Blackburn, 1973; Lin and Tong, 1980; Hughes and Akin, 1980) in the past decades. Barsoum (1976) proposed the 20-noded isoparametric element for three-dimensional crack tip analyses. The singularity in these elements is achieved by placing the mid-side node near the crack tip at the quarter point (Steinmueller, 1974). Such element has been proved to satisfy the essential convergence criteria (Zienkiewicz, 1971) and pass the patch test (Irons and Razzaque, 1972). The numerical accuracy of the results in a reasonably dense mesh of a practical problem is very high (Barsoum, 1976; Lapidus and Pinder, 1982).

In accordance with the relationship between the strain and displacement in the finite element analysis of

$$[\varepsilon] = [B] \begin{Bmatrix} u \\ v \\ w \end{Bmatrix} \quad (4.6)$$

$$[B] = \begin{bmatrix} \frac{\partial N_i}{\partial x} & 0 & 0 \\ 0 & \frac{\partial N_i}{\partial y} & 0 \\ 0 & 0 & \frac{\partial N_i}{\partial z} \\ \frac{\partial N_i}{\partial y} & \frac{\partial N_i}{\partial x} & 0 \\ 0 & \frac{\partial N_i}{\partial z} & \frac{\partial N_i}{\partial y} \\ \frac{\partial N_i}{\partial z} & 0 & \frac{\partial N_i}{\partial x} \end{bmatrix} \quad (4.7)$$

where u , v , w are the displacements, N_i is the shape function corresponding to the node i . Transforming to the normalized cube space (ξ, η, ζ) ($-1 \leq \xi \leq 1$, $-1 \leq \eta \leq 1$, $-1 \leq \zeta \leq 1$), it yields

$$\begin{Bmatrix} \frac{\partial N_i}{\partial x} \\ \frac{\partial N_i}{\partial y} \\ \frac{\partial N_i}{\partial z} \end{Bmatrix} = [J]^{-1} \begin{Bmatrix} \frac{\partial N_i}{\partial \xi} \\ \frac{\partial N_i}{\partial \eta} \\ \frac{\partial N_i}{\partial \zeta} \end{Bmatrix} \quad (4.8)$$

where $[J]$ is the Jacobian matrix and is given by

$$[J] = \begin{bmatrix} \frac{\partial x}{\partial \xi} & \frac{\partial y}{\partial \xi} & \frac{\partial z}{\partial \xi} \\ \frac{\partial x}{\partial \eta} & \frac{\partial y}{\partial \eta} & \frac{\partial z}{\partial \eta} \\ \frac{\partial x}{\partial \zeta} & \frac{\partial y}{\partial \zeta} & \frac{\partial z}{\partial \zeta} \end{bmatrix} \quad (4.9)$$

Hence the strain $\{\varepsilon\}$ can be expressed as

$$\{\varepsilon\} = [J]^{-1} [B'(\xi, \eta, \zeta)] \begin{Bmatrix} u \\ v \\ w \end{Bmatrix} \quad (4.10)$$

The form of $N_i(\xi, \eta, \zeta)$ in all isoparametric elements are polynomials, and hence $\partial N_i / \partial \xi$, $\partial N_i / \partial \eta$ and $\partial N_i / \partial \zeta$ are non-singular, therefore the singularity could be achieved by requiring that the inverse Jacobian matrix $[J]^{-1}$ be singular at the crack tip.

Considering the $\zeta=1$ plane in the prism element (Zienkiewicz, 1989), the shape function at the corner points is

$$N_1 = (2L_1 - 1)L_1, \quad N_2 = (2L_2 - 1)L_2, \quad N_3 = (2L_3 - 1)L_3 \quad (4.11)$$

and at the middle points is

$$N_4 = 4L_1L_2, \quad N_5 = 4L_2L_3, \quad N_6 = 4L_3L_1, \quad (4.12)$$

where L_1, L_2, L_3 are the area factor given by

$$L_1 = \frac{\text{area of } \Delta P23}{\text{area of } \Delta 123}, \quad L_2 = \frac{\text{area of } \Delta P13}{\text{area of } \Delta 123}, \quad \text{and } L_3 = \frac{\text{area of } \Delta P12}{\text{area of } \Delta 123} \quad (4.13)$$

Investigating the singularity along the x-axis in Fig. 4.9 where $\eta=0$, the area factors become

$$L_1 = \frac{1}{2}(1 - \xi), \quad L_2 = L_3 = \frac{1}{4}(1 + \xi) \quad (4.14)$$

and, therefore the shape functions can be expressed as

$$N_1 = -\xi \cdot \frac{1}{2}(1 - \xi), \quad N_2 = N_3 = -\frac{1}{8}(1 - \xi)(1 + \xi) \quad (4.15)$$

$$N_4 = N_6 = \frac{1}{2}(1 - \xi^2), \quad N_5 = \frac{1}{4}(1 + \xi)^2 \quad (4.16)$$

If the point 4 and 6 are moved to the quarter point, then

$$x_1 = 0, \quad x_4 = x_6 = \frac{L}{4}, \quad x_2 = x_3 = x_5 = L \quad (4.17)$$

and, hence

$$x = \sum_{i=1}^6 N_i x_i = \frac{1}{4}(1 + \xi)^2 \cdot L \quad (4.18)$$

Therefore,

$$\xi = 2\sqrt{\frac{x}{L}} - 1 \quad (4.19)$$

In the Jacobian the term $\partial x / \partial \xi$ is given by

$$\frac{\partial x}{\partial \xi} = \frac{L}{2}(1 + \xi) = \sqrt{\frac{x}{L}} \quad (4.20)$$

which makes the inverse Jacobian matrix $\mathbf{J}^{-1}$ an inverse square root singularity at $x=0$. Then, the strain field obtained an inverse square root singularity at $x=0$ which satisfies the nature of the strain field close to the crack tip in the linear elastic fracture mechanics (LEFM). In this research, the three-dimensional prism with four mid-side nodes at the quarter points is used, as shown in Fig. 4.10, the nodes at the crack tip are tied, and the mid-side nodes are moved to the 1/4 points.

4.3.2 Plastic Crack-tip Fields

In the elastic-plastic fracture mechanics (EPFM) analyses, the inverse square root singularity does not exist any longer when a plastic zone forms at the crack tip. When the strain hardening is characterized by a power law, the dominant singularity is referred to as the Hutchinson, Rice and Rosengren (HRR) singularity after the investigations of Hutchinson (1968) and Rice and Rosengren (1968). The uniaxial stress-strain curve is modelled as

$$\frac{\varepsilon}{\varepsilon_y} = \frac{\sigma}{\sigma_y} + \alpha \left(\frac{\sigma}{\sigma_y} \right)^n \quad (4.21)$$

where σ_y and ε_y are the yield stress and strain respectively, $n > 1$ is the strain hardening exponent or index, and α is a dimensionless material constant. The first term on the right hand side of Equation (4.21) describes the usual linear elastic behaviour, whereas the second term provides the nonlinear or plastic response. Typical curves are shown in Fig. 4.11 for selected values of the hardening exponent n . In the limit as $n \rightarrow \infty$, an elastic-perfectly plastic material behaviour is approach.

For power law hardening materials the stress, strain and displacement field closed to the crack tip was given by Kanninen and Popelar (1985) as

$$\begin{aligned} \sigma_{ij}(r, \theta) &= K r^{-1/(n+1)} f_{ij}^{\sigma}(\theta) \\ \varepsilon_{ij}(r, \theta) &= \alpha \varepsilon_y K^n r^{-n/(n+1)} f_{ij}^{\varepsilon}(\theta) \\ u_{ij}(r, \theta) &= \alpha \varepsilon_y K^n r^{1/(n+1)} f_{ij}^u(\theta) \end{aligned} \quad (4.22)$$

where K is a constant not to be confused with the elastic stress intensity factor, $f_{ij}^{\sigma}(\theta)$, $f_{ij}^{\varepsilon}(\theta)$ and $f_{ij}^u(\theta)$ are the functions of angle θ . It is clear from equation

(4.22) that for other than linear elastic materials ($n=1$) the orders of the stress and strain singularities are different. In the limit as $n \rightarrow \infty$, the stress field is non-singular, whereas the strain field has a $1/r$ singularity for the stationary crack tip.

Consequently, elastic singular elements are not appropriate for elastic-plastic analyses. Fig. 4.12 shows an element that exhibits the desired strain singularity under fully plastic conditions. The element is degenerated into a wedge as before, but the crack tip nodes are untied and the location of the mid-side nodes is unchanged. This element geometry produces a $1/r$ strain singularity which corresponds to the actual crack tip strain field for fully plastic materials (Aliabadi and Rooke, 1991). One of the advantages of using plastic singular element is that it allows the crack tip opening displacement (CTOD) to be computed from the deformed mesh, as illustrated in Fig. 4.13. The untied nodes initially occupy the same point in space, but they move apart as the elements deform subsequently. The CTOD can be inferred from the deformed crack profile by means of the intercept method (Aliabadi and Rooke, 1991).

In this study, 20-node brick elements are used for the joint except at the crack zone. Between the crack front and the field far away from it, 15-node prism elements and 10-node tetrahedron elements are used to refine the mesh gradually.

4.4 Mesh Generation Procedure

Mesh generation is one of the key criteria in any finite element analysis, and a good quality mesh forms the basis of a success in the analysis. In order to generate an accurate cracked model for RHS joints using quadrilateral elements, the sub-zone approach adopted by Lie et al. (2003) is used in this study. Based on the geometry of the joint and element densities for different zones, the meshes of these sub-zones are generated separately, and the transition zones are used to connect these sub-zones together.

The whole process is divided into six key steps: 1) Construction of a consistent geometrical model of the uncracked joint with welding details, 2) Decision of crack tip positions to locate the crack zones, 3) Extraction of elements in the crack zones,

4) Determination of cracked surface to define the crack profiles, 5) Generation of well-graded refined meshes of the crack zones, and 6) Completion of the cracked joint finite element mesh by merging the crack zones back into the model.

4.4.1 Mesh Generation of Uncracked Joints

Since the geometry around the intersection between the chord and the brace is the most complicated one, finer mesh is needed in this region. The mesh of this zone, called a weld zone, is generated first in this study. The mesh of the cross-section used is shown in Fig. 4.14 (a), and the path of the cross-section is defined in Fig. 4.14 (b), then, based on the required mesh density of one round shown in Fig. 4.15, the entire mesh of the weld zone is generated as shown in Fig. 4.16. The coarser meshes of the other zones, namely brace, chord, center and gap, are generated at a later stage. They are then connected together using the transition zone. The final mesh of the entire joint is shown in Fig. 4.17. It can be seen that the entire mesh is very even and the transition between the fine mesh and the coarse mesh is smooth.

4.4.2 Localization and Extraction of Crack Zone

After the crack dimensions were measured experimentally (Chiew et al., 2003) or assumed numerically, the mesh of the cracked joint can be generated by modifying the mesh of the uncracked joint. Based on the positions of the two crack tips, the location of the cracks along the weld toe can be obtained, and the elements along the crack are extracted automatically and re-meshed later as the crack zone. Fig. 4.18 shows the mesh after extracting the crack zone.

4.4.3 Mesh Generation of Crack Zone for Surface Crack

As mentioned earlier, if the curve of the crack front has already been determined, the two-dimensional curve can be mapped onto the three-dimensional crack surface. The coordinates of all the nodes located along the crack front are obtained accordingly. The mesh around the crack front is designed as a “spider web” configuration which consists of concentric rings of four sided elements which are centred toward the crack tip. The inner-most ring of elements is degenerated

into triangles as shown in Fig. 4.19. This type of mesh design was found to be the most efficient one for most problems (Yang, 1996).

Hence, all coordinates of the nodes around the crack front can be calculated using the relationship of the local u - v - w coordinate system and the local x - y - z coordinate system of

$$\begin{bmatrix} x \\ y \\ z \end{bmatrix} = \begin{bmatrix} l_1 & l_2 & l_3 \\ m_1 & m_2 & m_3 \\ n_1 & n_2 & n_3 \end{bmatrix} \begin{bmatrix} u \\ v \\ w \end{bmatrix} \quad (4.5)$$

where l_i , m_i , n_i ($i=1, 2, 3$) are the direction cosines of the u , v , w -axis, and the local x - y - z coordinate system is parallel to the global X - Y - Z coordinate system (Fig. 4.20). In addition, the direction cosines must satisfy the following equation

$$l_i^2 + m_i^2 + n_i^2 = 1 \quad (4.6)$$

The vector $\mathbf{w}$ shown in Fig. 4.20 is tangential to the crack front curvature. Because the mesh is very fine along the crack front, the direction of $\overrightarrow{P_1 D}$ or $\overrightarrow{D P_2}$ can be used approximately as the direction of $\mathbf{w}$. If the coordinates of P_1 is $(X_{P_1}, Y_{P_1}, Z_{P_1})$, and D is (X_D, Y_D, Z_D) , then

$$l_3 = \frac{X_{P_1} - X_D}{d_D}, \quad m_3 = \frac{Y_{P_1} - Y_D}{d_D}, \quad n_3 = \frac{Z_{P_1} - Z_D}{d_D} \quad (4.7)$$

where d_D is the distance between the two points. This distance is given by

$$d_D = \sqrt{(X_D - X_{P_1})^2 + (Y_D - Y_{P_1})^2 + (Z_D - Z_{P_1})^2} \quad (4.8)$$

As the vector $\mathbf{v}$ shown in Fig. 4.18 is perpendicular to the crack surface, the vector $\mathbf{v}$ should be perpendicular to the tangent $\mathbf{t}$ and line $\mathbf{W_0 D}$, which is located in the crack surface. Under the assumption that the angle between crack surface and vertical line is θ , and the angle between tangential direction and Z -axis is ϕ , the direction cosines of $\mathbf{t}$ and $\mathbf{W_0 D}$ are given as $\sin \phi \mathbf{j} + \cos \phi \mathbf{k}$ and $\cos \theta \mathbf{i} - \sin \theta \cos \phi \mathbf{j} + \sin \theta \sin \phi \mathbf{k}$ respectively. Hence, the unit vector $\mathbf{v}$ is given by

$$\begin{aligned} \mathbf{v} = \mathbf{W}_0 \mathbf{D} \times \mathbf{t} &= \begin{vmatrix} \mathbf{i} & \mathbf{j} & \mathbf{k} \\ 0 & \sin \varphi & \cos \varphi \\ \cos \theta & -\sin \theta \cos \varphi & \sin \theta \sin \varphi \end{vmatrix} \\ &= \sin \theta \mathbf{i} + \cos \theta \cos \varphi \mathbf{j} - \cos \theta \sin \varphi \mathbf{k} \end{aligned} \quad (4.9)$$

Hence,

$$l_2 = \sin \theta, \quad m_2 = \cos \theta \cos \varphi, \quad n_2 = -\cos \theta \sin \varphi \quad (4.10)$$

According to the relationship of the coordinate axis in Cartesian coordinate system,

$$\begin{aligned} \mathbf{u} = \mathbf{v} \times \mathbf{w} &= \begin{vmatrix} \mathbf{i} & \mathbf{j} & \mathbf{k} \\ l_2 & m_2 & n_2 \\ l_3 & m_3 & n_3 \end{vmatrix} \\ &= (m_2 n_3 - m_3 n_2) \mathbf{i} - (l_2 n_3 - l_3 n_2) \mathbf{j} + (l_2 m_3 - l_3 m_2) \mathbf{k} \end{aligned} \quad (4.11)$$

Hence,

$$l_1 = m_2 n_3 - m_3 n_2, \quad m_1 = l_3 n_2 - l_2 n_3, \quad n_1 = l_2 m_3 - l_3 m_2 \quad (4.12)$$

Therefore, the coordinate transformation matrix is determined, and all the coordinates of the nodes around the crack front can be calculated accordingly. Transforming the coordinates to the global X - Y - Z coordinate system,

$$X = x + X_D, \quad Y = y + Y_D, \quad Z = z + Z_D \quad (4.13)$$

The generated mesh around crack front is shown in Fig. 4.21.

Finally, some prism and tetrahedron elements are used to connect the fine mesh around the crack front with the coarse mesh far away from the crack. The mesh of the crack zone is generated automatically as shown in Fig. 4.22. Inserting back the mesh into the joint, the mesh of the cracked joint is finally generated. As discussed earlier, the nodes at the crack tip are normally tied together in the elastic analysis, and the mid-side nodes are moved to the 1/4 points position. But for elastic-plastic analysis, the untied crack tip or blunted crack tip should be used instead, and the location of the mid-side nodes is unchanged. In this mesh generation method, it is very convenient to investigate different crack tip models and different number of rounds at the crack tip by only changing the mesh in the local plane.

Because the curve of the crack front can be of any smooth curve, this mesh generation approach also can be used to study the coalesced cracks. For example, the coalescing of two cracks shown in Fig. 4.23, the generated mesh of the crack zone is illustrated in Fig. 4.24.

4.4.4 Mesh Generation of Crack Zone for Through-thickness Crack

As mentioned in Section 4.2, the through-thickness crack is modelled as a rectangular crack. The crack front consists of two straight lines at the two crack tips passing through the chord wall. It is much simpler to generate the mesh of the crack zone compared to the curved surface crack. Using the same approach as that used to generate the mesh of surface crack, the mesh around the two crack tips is generated separately, and they are connected with the other part of the coarse mesh. A typical generated mesh of through-thickness crack is shown in Fig. 4.25.

4.4.5 Local Mesh Refinement and Completion of Cracked Joint Mesh

Since the crack tip region has a steep stress and strain gradient, the mesh refinement should be carried out at this region. The appropriate level of mesh refinement depends on the purpose of the analysis. Elastic analyses of stress intensity factors or energy release rate can be accomplished with relatively coarse meshes. Elastic-plastic problems require additional mesh refinement in the regions of the domain where yielding occurs. When a body experiences net section yielding, narrow deformation bands often propagate across the specimen. The high level of plastic strain in these bands will make a significant contribution to the J -integral, and therefore, the finite element mesh must be sufficiently refined in these regions to capture this deformation accurately.

Except for the mesh of the whole joint, the mesh of the crack zone can be refined easily in the present proposed method. Fig. 4.26 shows the refined mesh of the crack zone. While Fig. 4.27 shows the mesh of the crack zone after refining twice, and the mesh around the crack front can also be refined independently as shown in Fig. 4.28, where the outer round mesh has been doubled as it closes the crack front.

As only the mesh of crack zone needs to be modified, it is considerably easier to generate the mesh of the joints containing several cracks. The only difference is extracting these crack zones separately, and generating the meshes of these crack zones. Fig. 4.29 shows the completed mesh of a typical RHS T-joint containing two surface cracks. While Fig. 4.30 shows the completed mesh of a typical RHS T-joint containing two surface cracks and one through-thickness crack.

The present proposed method can be easily extended to generate finite element mesh of RHS Y, K, and KK-joints as shown in Figs. 4.31, 4.32 and 4.33 separately.

Fig. 4.1 Crack surface under the weld toe

Fig. 4.2 Local coordinate system at point W_0 along the weld toe

Fig. 4.3 Mapping from 2-D to 3-D crack front

Fig. 4.4 Local polar u - v coordinate system

Fig. 4.5 The weld toe on a local u - v coordinate system

Fig. 4.6 The first quadrant of u - v local coordinate system

Fig. 4.7 Through-thickness crack in the u' - v' plane

Fig. 4.8 The degenerated element used around the crack front

Fig. 4.9 The plane where $\zeta=1$

Fig. 4.10 Elastic singularity element

Fig. 4.11 Stress-strain relationship of power-law strain hardening material
(Kanninen and Popelar, 1985)

Fig. 4.12 Plastic singularity element

Fig. 4.13 Deformed shape of plastic singularity elements

Fig. 4.14 Mesh design of the weld zone

Fig. 4.15 Mesh density around the intersection perimeter

Fig. 4.16 The completed mesh of the weld zone

Fig. 4.17 The complete mesh of the entire joint

Fig. 4.18 The mesh after extracting the crack zone

Fig. 4.19 The mesh design around the crack front

Fig. 4.20 The u - v - w and x - y - z local coordinate system

Fig. 4.21 The mesh along the crack front

Fig. 4.22 The mesh of the crack zone

Fig. 4.23 The crack front curve

Fig. 4.24 The mesh of the crack zone of a surface crack

Fig. 4.25 The mesh of the crack zone and the cross-section of a through-thickness crack

Fig. 4.26 The refined mesh of the crack zone

Fig. 4.27 The mesh of the crack zone after refining twice

Fig. 4.28 The mesh around the crack front after doubling the mesh density

Fig. 4.29 T-joint containing two surface cracks

Fig. 4.30 T-joint containing two surface cracks and one through-thickness crack

Fig. 4.31 The completed Y-joint mesh

Fig. 4.32 The completed K-joint mesh

Fig. 4.33 The completed KK-joint mesh

Table 4.1 3-D solid quadratic elements

	Element Types	No. of Nodes	
1.	Hexahedral / Cubic Element – (H20)	20
2.	Prism/Wedge – (P15)	15
3.	Quarter Point/Crack Element – (QP15) (Collapsed Prism)	15
4.	Tetrahedron – (T10)	10
5.	Pyramid – (PR20) (Collapsed Hexahedral)	20

CHAPTER 5

PLASTIC COLLAPSE LOAD OF CRACKED RHS T-JOINTS

5.1 Introduction

The failure assessment diagram (FAD) procedure for predicting the instability loads of damaged structures has been adopted by several important codes of practice (BS7910, 1999; API RP579, 2000; R6, 2001). This procedure recognizes both brittle fracture and plastic collapse of a flawed structure. It is a safety or failure plane defined by the stress intensity factor normalized by fracture toughness ratio as the ordinate and the applied load normalized by plastic collapse load ratio as the abscissa. For a particular load level and defect size, these coordinates can be readily calculated. If the assessment point lies inside the failure assessment curve, no unstable crack growth can occur. If the assessment point lies outside the curve, unstable crack growth is likely to occur.

The FAD approach has been validated and used widely in practice in the last two decades. To extend the application of this approach to more complex geometries, for instance cracked tubular joints, there are three major limitations: (1) accuracy of FAD curve (based on a fixed crack size) for the prediction of instability crack growth, (2) availability of stress intensity factor solutions, and (3) availability of fully plastic collapse load solutions for flawed structures of interest. The validation of FAD curve for cracked RHS T-joints will be discussed in Chapter 8. The stress intensity factor solutions will be obtained using finite element analysis. While, the fully plastic collapse load solutions for the RHS T-joints containing surface cracks and through thickness cracks will be proposed and derived in the following sections.

5.2 Ultimate Strength of Uncracked RHS T-joints under Axial Loads

Before the influence of cracks on the joints strength is discussed, the ultimate behaviour of uncracked joints is studied first. When the width ratio of brace and chord, β , is less than 0.8, normally the failure model of the T-joint under brace end axial loads is chord face yielding (Packer et al., 1992). The ultimate strength of uncracked RHS T-

joint is derived using the yield line theory (Wardenier, 1982). The yield line pattern is shown in Fig. 4.1. The energy participated in the yield lines 1 to 5 is given by

$$\text{yield line 1: } \frac{2\text{tg}\alpha}{1-\beta} \cdot \delta \cdot m_p \quad (5.1)$$

$$\text{yield line 2: } \frac{2\beta \cdot \text{tg}\alpha}{1-\beta} \cdot \delta \cdot m_p \quad (5.2)$$

$$\text{yield line 3: } \left\{ \frac{2\eta}{1-\beta} + 2\cot\alpha \right\} \cdot \delta \cdot m_p \quad (5.3)$$

$$\text{yield line 4: } \frac{2\eta}{1-\beta} \cdot \delta \cdot m_p \quad (5.4)$$

$$\text{yield line 5: } (\text{tg}\alpha + \cot\alpha) \cdot \delta \cdot m_p \quad (5.5)$$

where $m_p = \frac{\sigma_{e0} \cdot t_0^2}{4}$, σ_{e0} is yield strength of the material. The energy of the external load P_u is equal to the participated energy in the yield lines, which is equal to

$$P_u = \frac{2\sigma_{e0} \cdot t_0^2}{1-\beta} \left\{ \text{tg}\alpha + \frac{1-\beta}{\text{tg}\alpha} + \eta \right\} \quad (5.6)$$

and when $\text{tg}\alpha = \sqrt{1-\beta}$, the minimum ultimate strength is obtained (Wardenier, 1982).

Hence,

$$P_u = \frac{\sigma_{e0} \cdot t_0^2}{1-\beta} \left\{ 2\eta + 4\sqrt{1-\beta} \right\} \quad (5.7)$$

5.3 Plastic Collapse Load of RHS T-joints with Surface Cracks

For surface cracks, the ligament in front of the crack has the capability to pull and deform the chord face. Therefore, the axial displacement of the chord face around the entire circumference of the brace and chord intersection can be assumed to be δ as shown in Fig. 5.2. Based on the yield line theory, the following two assumptions are deduced, and they are

1. The yield line pattern remains the same as the uncracked joint.
2. The thickness of the crack region is $t_0 - a$, where t_0 is the thickness of the chord face and a is the vertical depth of the crack under the weld toe.

The yield line pattern of cracked RHS T-joint is shown in Fig. 5.2, and the energy participated in these yield lines 1 to 5 are the same as that of the uncracked joint. Because of the influence of the crack, the energy participated in yield line 2' and 4' is derived as follows:

$$\text{yield line 2': } (b_1 - l_{c1}) \cdot \frac{2\delta}{(b_0 - b_1)\cot\alpha} \cdot m_p + l_{c1} \cdot \frac{2\delta}{(b_0 - b_1)\cot\alpha} \cdot m'_p \quad (5.8)$$

$$\text{yield line 4': } (h_1 - l_{c2}) \cdot \frac{2\delta}{b_0 - b_1} \cdot m_p + l_{c2} \cdot \frac{2\delta}{b_0 - b_1} \cdot m'_p \quad (5.9)$$

where $m'_p = \frac{\sigma_{e0} \cdot (t_0 - a)^2}{4}$ and σ_{e0} is yield strength of the material.

The energy participated by the external load P_u is equal to the participated energy in the yield lines. It can be expressed as

$$P_u = \frac{2\sigma_{e0} \cdot t_0^2}{1 - \beta} \left\{ \text{tg}\alpha + \frac{1 - \beta}{\text{tg}\alpha} + \eta \right\} - \frac{\sigma_{e0} \cdot [t_0^2 - (t_0 - a)^2]}{2} \cdot \left\{ \frac{l_{c1}}{(b_0 - b_1)\cot\alpha} + \frac{l_{c2}}{b_0 - b_1} \right\} \quad (5.10)$$

In accordance with assumption 1, the following relationship can be obtained from the uncracked joint (Wardenier, 1982),

$$\text{tg}\alpha = \sqrt{1 - \beta} \quad (5.11)$$

Substitution of Equation (5.11) into Equation (5.10) produces the yield load,

$$P_u = \frac{\sigma_{e0} \cdot t_0^2}{1 - \beta} \left\{ 2\eta + 4\sqrt{1 - \beta} \right\} - \frac{\sigma_{e0} \cdot [t_0^2 - (t_0 - a)^2]}{2(b_0 - b_1)} \cdot \left\{ l_{c1} \cdot \sqrt{1 - \beta} + l_{c2} \right\} \quad (5.12)$$

In Equation (5.12), the first term is the yield load of uncracked joint, and the second term is the reduction of the yield load due to the introduction of the crack. If there are additional cracks, more reduction terms having the same expression needs to be calculated for different crack depths and lengths. Equation (5.12) becomes

$$P_u = \frac{\sigma_{e0} \cdot t_0^2}{1-\beta} \left\{ 2\eta + 4\sqrt{1-\beta} \right\} - \sum_n \frac{\sigma_{e0} \cdot [t_0^2 - (t_0 - a^n)^2]}{2(b_0 - b_1)} \cdot \left\{ l_{c1}^n \cdot \sqrt{1-\beta} + l_{c2}^n \right\} \quad (5.13)$$

where n represent the number of the cracks. Considering the influence of the welds and the chord wall thickness, the width ratio β should be equal to $\frac{b_1 + 2t_w\sqrt{2}}{b_0 - 2t_0}$, and the brace depth to chord width ratio η should be equal to $\frac{h_1 + 2t_w\sqrt{2}}{b_0 - 2t_0}$ (Wardenier, 1982).

5.4 Plastic Collapse Load of RHS T-joints with Through Thickness Cracks

For the T-joints containing through thickness crack, the chord face deformation around the two crack tips and at the corner is different from that of the surface crack. In accordance with the experimental deformation of the crack part, the yield line pattern is assumed as shown in Fig. 5.3. It is much more complicated than the joints containing surface crack.

In Fig. 5.3, the axial displacement of the uncracked intersection part between the chord and brace is δ . Hence, the displacement of points A and B is δ . The displacement of point C located at the corner of the brace is δ' which depends on the distance between the crack tip and the corner, l_{c1} . If l_{c1} is smaller, δ' is close to δ , otherwise it is close to zero. The minimum plastic collapse load will be used when δ' varies from zero to δ . In accordance with the yield line theory (Johansen, 1962), the angle between the yield line 10 and the chord side wall is 45° . The energy participated in the yield lines 1 to 5 are the same as the uncracked joint. The energy participated in yield line 6 to 15 is derived as follows:

$$\text{yield line 6: } (b_1 - l_{c1}) \cdot \frac{2\delta}{(b_0 - b_1)\cot\alpha} \cdot m_p \quad (5.14)$$

$$\text{yield line 7: } tg\gamma \cdot \delta \cdot m_p + \left[\left(1 - \frac{l_{c1} \sin \gamma}{L_7} \right) \cdot \delta - \delta' \right] \cdot \frac{L_7}{l_{c1} \cos \gamma} \cdot m_p \quad (5.15)$$

$$\text{yield line 8: } \left[\frac{L_8}{L_7 \cdot \sin(\gamma - \alpha)} \cdot \delta - \left(\frac{1}{tg(\gamma - \alpha)} + \frac{1}{tg\alpha} \right) \cdot \delta' \right] \cdot m_p \quad (5.16)$$

$$\text{yield line 9: } \delta' \cdot m_p \quad (5.17)$$

$$\text{yield line 10: } 2\delta' \cdot m_p \quad (5.18)$$

$$\text{yield line 11: } \delta' \cdot \cot \alpha \cdot m_p \quad (5.19)$$

$$\text{yield line 12: } 2\delta \cdot m_p \quad (5.20)$$

$$\text{yield line 13: } \delta \cdot m_p \quad (5.21)$$

$$\text{yield line 14: } \frac{2(h_1 - l_{c2})}{b_0 - b_1} \cdot \delta \cdot m_p \quad (5.22)$$

$$\text{yield line 15: } \left[\frac{b_0 - b_1}{2} \cdot \cot \alpha + h_1 - l_{c2} \right] \cdot \frac{2\delta}{(b_0 - b_1)} \cdot m_p \quad (5.23)$$

where γ is the angle of yield line 7 as shown in Fig. 5.3, L_7 and L_8 are the length of the yield lines 7 and 8 respectively, and they are given by

$$L_7 = \frac{(b_0 - b_1)}{2} \cdot \frac{\cot \alpha}{\cos \gamma} \quad (5.24)$$

$$L_8 = \frac{(b_0 - b_1)}{2 \sin \alpha} \quad (5.25)$$

$$\tan \gamma = \left[\frac{(b_0 - b_1)}{2} + l_{c1} \right] / \left[\frac{(b_0 - b_1)}{2} \cdot \cot \alpha \right] \quad (5.26)$$

The energy participated by the external load P_u is equal to the participated energy in the yield lines. It can be expressed as

$$P_u = \frac{\sigma_{e0} \cdot t_0^2}{1 - \beta} \{ 2\eta + 4\sqrt{1 - \beta} \} - P_R \quad (5.27)$$

where the first term is the yield load of uncracked joint, P_R is the reduction of the yield load due to the introduction of the through thickness crack, and

$$P_R = \frac{\sigma_{e0} \cdot t_0^2}{4} \cdot \left\{ \sqrt{1 - \beta} + \frac{2}{\sqrt{1 - \beta}} + \frac{2}{b_0 - b_1} (l_{c1} \sqrt{1 - \beta} + 2l_{c2} - h_1) + \frac{2\eta}{1 - \beta} \right. \\ \left. - \frac{L_7}{l_{c1} \cos \gamma} - \frac{L_8}{L_7 \sin(\gamma - \alpha)} - 3 + \left(\frac{L_7}{l_{c1} \cos \gamma} + \frac{1}{\tan(\gamma - \alpha)} - 3 \right) \cdot \frac{\delta'}{\delta} \right\} \quad (5.28)$$

5.5 Estimation of L_r Parameter

In the failure assessment diagram (FAD) approach, the L_r parameter is concerned with the interaction between plastic collapse and the effect of plastic at the crack tip. It is usually defined as the ratio of the applied load to the load which would cause plastic collapse of the cracked geometry in a model made from elastic-perfectly plastic material (Burdekin, 2001). In accordance with the failure mode of the RHS T-joint with medium β ratio, the plastic collapse load of the cracked RHS T-joint under brace end axial load is proposed by using the yield line theory. It can be used as a basis for calculation of the L_r parameter in the FAD.

For the purpose of fracture mechanics assessments of RHS T-joints, it is recommended that the L_r parameter be calculated from the following expression:

$$L_r = \frac{P}{P_u} \quad (5.29)$$

where P is the applied load and P_u is the plastic collapse load calculated from the Equation (5.13) or Equation (5.27).

However, this does not mean that the use of L_r calculated in this way will give failure assessments results falling exactly on the standard assessment diagram because the interaction with crack tip driving force is also dependent on the geometry. It is necessary to consider the suitability of the standard diagram for a particular RHS T-joint application, and the plastic collapse load equations must be carefully validated. In Chapters 6 and 7, the ultimate and fracture behaviour of cracked SHS T-joints will be investigated experimentally and numerically, and the specified FAD for SHS T-joint under brace end axial load will be generated using the J -integral approach.

Fig. 5.1 Yield line pattern of an uncracked RHS T-joint under axial load

Fig. 5.2 Yield line pattern of a RHS T-joint containing surface crack under axial load

Fig. 5.3 Yield line pattern of a RHS T-joint containing through-thickness crack under axial load

CHAPTER 6

EXPERIMENTAL TESTS OF SQUARE HOLLOW SECTION (SHS) T-JOINTS

6.1 Introduction

Most of static strength tests on large-scale damaged tubular joints conducted in controlled laboratory have failed in a ductile manner (Hadley et al., 1998; Cheaitani et al., 1998; Klasen and Wastberg, 1997). This is because the material has to pass an acceptance test based on the crack tip opening displacement (CTOD), which ensures ductile behaviour. Health & Safety Executive (HSE) in U.K. has used this basic criteria as the basis for guidance on the static strength of cracked tubular joints (Department of Energy, 1989). As more emphasis is placed on the use of flooded member detection technique to indicate through-thickness cracks, the level of in-service inspection is reducing in practice. This means that a tubular joint in the structure may contain a large crack at the point when the maximum design load is experienced during its service life.

The BS7910 (1999) provides guidances on assessing the significance of defects in fusion welded structures on a fitness-for-purpose basis. The guidance covers both fracture and fatigue assessments. The fracture treatment is based on the failure assessment diagram (FAD) approach, which originated in the Central Electricity Generating Board (CEGB) R6 procedures (Milne et al., 1986), and it was further validated extensively using the pressure vessel data (Milne et al., 1988). Validation of the FAD approach for assessment of offshore tubular joints was carried out using analytical studies and experimental tests results. These have been reported by Burdekin (2001), and they were then adopted as the basis for the assessment of tubular joints in BS7910 (1999). All of these works were focused on circular hollow section (CHS) joints, and there are very few published works reported in the literatures on the residual static strength of cracked rectangular hollow section (RHS) joints.

In practice, many of the fatigue cracks found in welded tubular structures are part-through cracks. In the CHS joints, the crack is found to resemble a semi-ellipse which is perpendicular to the chord surface (Pook, 1993). But for RHS joints, the fatigue crack,

which always initiates from the hot spot stress located at the corner of the brace, is not symmetric and not semi-elliptical in shape (Chiew et al., 2003). In order to validate the FAD analysis procedure, and widen its range of application to the RHS joints, further experimental tests are required on cracked RHS joints.

In the present study, three full-scale square-to-square hollow section (SHS) T-joints under brace end axial load were tested to failure. All of these joints were fatigue cracked by the previous programmes (Chiew et al., 2002; 2003) as shown in Fig. 6.1. The joints were subjected to combined loads that were provided by the actuators mounted on the specific frame as shown in Fig. 6.2. The first specimen contained two surface cracks at two corners, the second one contained one big surface crack, and the third one contained one through-thickness crack. After the tests, the crack dimensions were measured using a clay model as shown in Figs. 6.3 and 6.4 (Chiew et al., 2003). The model was made from the crack surface, and then it was cut into many cross sections to measure the curved crack size. The experimental results were then used to investigate the residual strength of the cracked joints, and to validate the FAD procedure provided by BS7910 (1999).

6.2 Test Rig and Loading System

A specially designed test rig as shown in Fig. 6.5 was used to test the three cracked SHS joints. The rig consists of two servo-hydraulic actuators which can produce ± 1000 kN load and ± 125 mm displacement capacity respectively. The two actuators apply the loads to the two ends of the spread beam, and in turn these loads are transmitted to the brace end through the 6 bolts tightened to the spread beam as shown in Fig. 6.6. Therefore, a total maximum axial load of 2000 kN can be applied to the specimen. The actuators are controlled by the Instron 8500 controller which can be set in load control or displacement control. The controller and the recording system used in these tests are illustrated in Fig. 6.7. The chord ends of the joints were supported on a pin and a roller placed on top of 600 mm $\times$ 600 mm concrete supports as shown in Fig. 6.6, creating a pinned end support condition at both ends.

6.3 Specimen Details

The specimens were fabricated using BS4360 structural steel of grade 50D, and they were available from the previous fatigue test programmes conducted earlier (Chiew et al., 2002). These hot finished square hollow section (SHS) joints dimensions are given in Table 6.1, and the mechanical properties of the BS4360-50D steel supplied by the manufacturer, Corus, in UK, are given in Table 6.2. The weld profile and the specimen preparation were carried out in accordance with the American Welding Society (AWS) Structure Welding Code D1.1-2000 (2000) specifications. They were then checked using the ultrasonic inspection by the Millennium Technical & Trading Services Pte Ltd in Singapore to ensure the welds quality. The ultrasonic inspection test report was presented in appendix D. The welding electrodes were E70XX type conforming to AWS D1.1-2000 (2000). The weld properties were tabulated in Table 6.3. Compared with the parent material yield stress, 380.3 MPa, the yield stress of the weld material is only 5 percent higher, therefore the weld material matches the parent material.

The two chord ends were reinforced by stiffeners to avoid the chord walls from local buckling. The brace end was welded with a 40 mm thick plate connecting to the spread beam, and stiffeners are welded to ensure the strength and even distribution of the applied load.

6.4 Weld and Fatigue Crack Details

The weld details are shown in Fig. 6.8. The weld width, t_w , was measured after the joint was fabricated, and it is tabulated in Table 6.4. The shape of the fatigue cracks are measured after the tests, they are shown in Figs. 6.9 to 6.12. As predicted in Chapter 3, the fatigue crack is not symmetric, and the deepest points always locate at the corner. The detail dimensions of the fatigue crack are also tabulated in Table 6.4.

6.5 Monitoring of Crack Growth

The Alternating Current Potential Drop (ACPD) technique is a well-established technique (Dover et al., 1995) which can be used to monitor the crack depth at regular intervals and hence the shape of a growing crack in the specimen. The ACPD method

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 5 kHz, ferromagnetic mild steel has a skin depth of the order of approximately 0.1 mm (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 Figure 6.13. In principle, 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

$$V_C \propto (\Delta_R + 2d_1) \quad (6.1)$$

$$V_R \propto \Delta_R \quad (6.2)$$

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 Equations (6.1) and (6.2), the crack depth can be obtained as follow:

$$d_1 = (\Delta_R / 2)(V_C / V_R - \Delta_C / \Delta_R) \quad (6.3)$$

If Δ_C and Δ_R used are same, then Equation 6.3 reduces to

$$d_1 = (\Delta / 2)(V_C / V_R - 1.0) \quad (6.4)$$

where Δ is the probe spacing.

In this study, the ACPD crack depth probes distances around the fatigue crack locations are shown in Figs. 6.14, 6.15 and 6.16 for specimens T1, T2 and T3 respectively. Due to the limitation of the channels, only the deepest crack which is the most dangerous one was monitored during the test for specimen T1. Specially manufactured probes of diameter of 0.18 mm and length of 10 mm were spot-welded on

the chord surface securely as shown in Fig. 6.17. All these probes were connected to the U10 Crack Microgauge (TSC, 1991) which provides the high-frequency current and phase-sensitive detection. The U10 Crack Microgauge was connected to an IBM personal computer using the RS232 serial interfaces as shown in Fig. 6.18, and it was controlled by the Flair software (TSC, 1998) that provides the automated instrument control, data storage facilities and dedicated graphical output under the Windows environment. During the test, the crack was scanned at every load step. Fig. 6.19 shows the set-up of the ACPD probes along the fatigue crack.

6.6 Measurement of Crack Mouth Opening Displacement (CMOD)

Three sets of 3 LVDTs with a stroke capacity of 10 mm were used to measure the crack mouth opening displacement (CMOD) along the crack. The locations of the 1st, 2nd and 3rd set of LVDTs are shown in Figs. 6.14, 6.15 and 6.16 for specimens T1, T2 and T3 respectively. Fig. 6.19 shows the arrangement of the three LVDTs. One block and one bracket are attached to the chord face and brace face, and in turn three LVDTs are fixed to the bracket. The three LVDTs are in contact with the block, as shown in Fig. 6.20, so that the vertical displacement of points 1 and 2 and the horizontal displacement of point 3 relative to the brace can be measured accordingly.

In order to transfer the measurements obtained to the crack mouth opening displacement, an extrapolation approach is adopted in the test as shown in Fig. 6.21. If the region between the crack and the measured point is treated as a rigid body, the displacements at the measured points are caused by the displacements and rotations of the crack faces. If the displacement measured by LVDTs 1, 2 and 3 are expressed as d_1 , d_2 and l , the following relationships can be found for

$$1) \quad \text{rotation of crack, } \gamma = \frac{d_2 - d_1}{L_2} \quad (6.5)$$

$$2) \quad \text{vertical displacement of crack mouth, } d_c = d_1 - \gamma \cdot L_1 \quad (6.6)$$

$$3) \quad \text{horizontal displacement of crack mouth, } l_c = l - l' = l - \gamma \cdot h \quad (6.7)$$

$$4) \quad \text{and, crack mouth opening displacement (CMOD), } \delta_{\text{CMOD}} = \sqrt{d_c^2 + l_c^2} \quad (6.8)$$

where L_1 , L_2 and L_3 are the respective distances from the LVDTs 1, 2 and 3 to the weld toe, γ is the rotation angle of crack face which is equal to the rotation angle of the chord face, l is the horizontal displacement caused by the block rotation, h is the vertical distance of the LVDT 3 away from the chord face, and it can be calculated from

$$h = h_0 + d_1 + \gamma \cdot (L_3 - L_1) \quad (6.9)$$

6.7 Measurements of Brace End Axial Load and Displacement

The brace end displacement was obtained from an LVDT with a stroke capacity of 50 mm which was calibrated and attached to the bottom of spread beam aligned with the centreline of the brace. The load was obtained via load cell of the actuators, and the load was also measured using the strain gauges attached at the middle of the brace to confirm the load readings.

6.8 Static Strength Test

Elastic loading was carried out before the test to check the operation of the various gauges, LVDTs and the output of the actuator. The specimen was loaded to 3% of the predicted total capacity, and then unloaded to ensure that the instrumentation readings returned to zero satisfactorily. Fig. 6.22 shows the recording system during the testing process, and the measurements of the strain gauges and LVDTs are recorded by the data logger and they are monitored visually by the computer. The axial tensile load was applied at the end of the brace using displacement control. The tests were considered complete when the joint failed completely, when the loads taken by the specimens dropped substantially.

6.9 Test Results and Discussion

6.9.1 Brace End Axial Load-Displacement Curves

Fig. 6.23 shows the load-displacement curves for the static strength of the three T-joint specimens. In these tests, the deformation caused by the self weight of the joint was not considered. The deformations recorded were global deformations which include the beam action of the chord. The curve of specimen T1 which has two surface cracks shows that the load is linearly related with the displacement up to a load of 600

kN. Subsequently, the slope of the load-displacement curve changes producing an approximately linear region of up to 824 kN. After this point, there is a plateau, and the load can still increase slightly up to the maximum load of 875 kN before it drops down as the displacement increases rapidly. This is mainly because this joint contains two cracks, and after the fracture of the deeper crack at the load of 824 kN, the joint can still take more loading until the second crack fracture at the load of 875 kN as shown in Fig. 6.24. Hence, it is reasonable to adopt the load of the deeper crack fracture of 824 kN as the ultimate strength of this joint.

The curve of specimen T2 also shows a linear relationship in the elastic region up to a load of 680 kN. Then the slope of this curve changes to another linear region up to 899 kN. Unlike the curve of specimen T1, the curve of T2 drops down to 805 kN after the peak load, then they increase slightly up to 825 kN before they drop down dramatically. The final failure of the joint is due to the other side of the intersection between the chord and brace broken up as shown in Fig. 6.25.

The curve of specimen T3, which contains a through-thickness crack, shows a linear relationship in the elastic region up to a load of 530 kN. Then the slope of this curve changes gradually up to a load of 792 kN. Similar to specimen T2, the curve of specimen T3 drops down to 640 kN after this maximum load, and then it increases slightly to 669 kN before the joint failed completely. The final failure of the joint is also due to the intersection of the chord and brace broken up as shown in Fig. 6.26.

It is obvious that all of the three T-joint specimens have a notable plastic deformation, especially for specimen T3. If the twice-elastic-compliance limit (Cheatani and Burdekin, 1994) is used to determine the deformation limits for the plastic collapse load of the joints, it is found that the specimens T1 and T2 which contain surface cracks failed before the displacement had reached the deformation limit. This means that the joints failure is not a full plastic failure. Therefore, the failure of these joints containing surface cracks is fracture dominant even though the material is on the upper shelf of the transverse curve. However, the failure of specimen T3 which contains through thickness crack is very close to a full plastic failure.

6.9.2 CMOD and CTOD

The curves of crack mouth opening displacement (CMOD) versus the brace end axial loads for specimen T1, T2 and T3 are shown in Figs. 6.27, 6.28 and 6.29 respectively. According to the CMOD readings and the geometries of the cracks, the crack tip opening displacement (CTOD) can be determined from these data.

From the experimental tests, it is observed that the fatigue crack surface is slightly curved. Hence, it is reasonable to use the inclined secant line to replace the curved crack surface cross-section as discussed earlier in Chapter 3. Based on the assumption that the CMOD is caused by the displacement of the ligament and rotation of the surface crack face, the CTOD can be deduced from the CMOD measurements as shown in Fig. 6.30. The relationship between CMOD and CTOD is

$$\delta_{\text{CTOD}} = \delta_{\text{CMOD}} - \delta' \quad (6.10)$$

where $\delta' = d_{\text{IC}} \cdot \gamma$, d_{IC} is the inclined crack depth, and γ can be obtained from Equation (6.5).

For the through thickness crack, the crack tip opening displacements (CTOD) at the two crack tips can be estimated approximately from the displacements at the three points of the LVDTs locations. To obtain the CTOD, quadratic extrapolation method is used as shown in Fig. 6.31. The X_1 , X_2 and X_3 are the distances of the LVDTs away from the crack tip2. The d_1 , d_2 and d_3 are the CMOD readings measured from the test. The distance of crack tip1, X_{tip1} , is 0, and the distance of crack tip2, X_{tip2} , can be measured after the test. In accordance with the quadratic extrapolation method, the displacements at the cracks tip1 and tip2 can be obtained and expressed as δ_{tip1} and δ_{tip2} accordingly. Hence the crack tip opening displacement of the through thickness crack are obtained from the specimens. Because the true shape of the through thickness cracks is not a rectangular plane, this extrapolation results give an approximate CTOD estimation only.

6.9.3 Crack Growth Analyses

Figs. 6.32 and 6.33 show the ACPD readings recorded in the test for specimens T1 and T2 respectively. Because the measured crack depth was not perpendicular to the

weld toe, it perpendicular to the crack front which got a shortest current path, the ACPD plot was not the true shape of the crack front. It can be found that the ACPD readings increase with the applied load, even a small increasing of the load. This increasing can be due to the crack tip blunting and the crack propagation. After the tests the crack surface was studied carefully to determine the load of the tearing initiation. There was no evidence of ductile tearing was found around the crack front located at the corner. And in the ACPD readings, there was no notable increasing before the crack opened suddenly. But along the crack front parallel to the chord side wall, the small stable ductile tearing was observed, and a notable increasing of the ACPD readings was found at the loads of 539 kN and 711 kN for specimens T1 and T2 respectively. Therefore, these loads were assumed as the ductile tearing initiation load. A peak load is found at a point in which the brittle fracture took place and the crack around the deepest point extended through the thickness of the chord wall suddenly. Fig. 6.34 illustrates the ACPD readings recorded in the test for specimen T3. In accordance with the ACPD recordings, crack tearing initiates after a load of 438 kN up to the final failure. The peak load was reached when the chord face had yielded in the specimen T3.

Figs. 6.35 and 6.36 show the first and second crack surfaces of specimen T1, while Fig. 6.37 shows the crack surface of specimen T2 after the T-joints had failed. Fig. 6.38 is the crack surface of specimen T3. A typical fracture surface is shown in Fig. 6.39. This fracture surface indicates that ductile tearing had taken place within a small region enclosing the crack tip² which is consistent with the ACPD readings. The fracture surface around the deepest point is shiny and smooth which indicates brittle cleavage fracture. The cross-section of this crack surface at the deepest point is shown in Fig. 6.40. It can be seen that the fatigue crack surface is slightly curved, and a kink is very obvious at the end of the fatigue crack surface. This means that the brittle fracture of the surface crack is under a mixed Mode-I and Mode-II condition.

6.10 Material Properties

6.10.1 Stress-strain Curve

To obtain the mechanical properties of the material used, 6 coupon tests were carried out on specimens cut out from the braces and chords of the damaged T-joints. The specimens were cut from the region far away from the brace to chord intersection

where the stress was largest. In accordance with the strain gauges affixed on these regions, the stress of the brace and chord was less than 110 MPa and 145 MPa respectively which are much smaller than the yield stress of the material, 380 MPa. Therefore the residual stress caused by the applied load can be neglected. The tensile specimens were taken from the middle of the walls far away from the weld seam as shown in Fig. 6.41 (BS EN10210-1, 1994). The specimens dimensions satisfy BS EN10002-1 (2001) specifications as shown in Fig. 6.42. The coupon tests were carried out on the Instron 4486 testing machine with a closed loop command with feedback control. The set-up of the tests is shown in Fig. 6.43. The stress-strain curves of these materials are then plotted and shown in Fig. 6.44. And the tests results were tabulated in Table 6.5. The average yield stress is 385.3 MPa which is very close to the value provided by the Corus, 380.3 MPa.

6.10.2 Charpy V-notch Tests

To estimate the fracture toughness of the material, the Charpy V-notch tests (BS EN10045-1, 1990) were also performed in this study. The orientation of the Charpy V-notch specimens are T-S, T-L, L-S and L-T which are defined in Fig. 6.45. The tests were carried out by Setsco Services Pte. Ltd. in Singapore, and the results are tabulated in Table 6.6

The broken specimens and the failure surfaces are illustrated in Figs. 6.46 and 6.47. It is found that the energies absorption for different specimen directions varies greatly. Hancock and Spurrier (1982) studied the critical CTOD of BS4360-50D steel under different thickness and temperature. It is found that the material is located on the upper shelf of the transition curve if the test temperature is at room temperature which is about 23°C. Hence, it should be ductile fracture. The L-S and L-T specimens orientation exhibit a significant ductile behaviour. The shear lip in the fracture surface is obvious as shown in Fig. 6.47. But the energies absorption for the T-S and T-L specimens orientation are quite smaller, and the fracture surface is clear cut which appears to be a brittle fracture.

In accordance with the BS 7910 (1999), the fracture toughness K_{mat} might be estimated from the corrections of Charpy V-notch impact test data taken from the

material of the type in which the flaw is situated in the specimens. The lower estimate of upper shelf fracture toughness is given by

$$K_{\text{mat}} = 17C_v + 1740 \quad (6.11)$$

This equation can be used if the Charpy test results exhibit a fracture appearance of fully ductile behaviour. Hence, it can only be used for the estimation of fracture toughness in L-T and L-S orientation. For the T-L and T-S direction, the lower bound correction is used as follows:

$$K_{\text{mat}} = \frac{820\sqrt{C_v} - 1420}{B^{1/4}} + 630 \quad (6.12)$$

where B is the specimen thickness. The results of K_{mat} are calculated and tabulated in Table 6.6.

Generally, steel is considered as homogeneous and isotropic material. But the microstructure and thus mechanical properties are often sensitive to direction as found above. The sensitivity to orientation is particularly pronounced in fracture toughness measurements because a microstructure with a preferred orientation may contain planes of weakness where crack propagation is relatively easy (Anderson, 1995). In accordance with the Charpy-V notch test results, the fracture toughness in T-S and T-L directions is much weaker. Therefore, the crack propagation in these directions is easier. In accordance with the ACPD readings and the observed fracture surface, the tearing always initiates from the crack front which is along the chord side wall. It can be seen that the fracture planes of the crack front in the T-S or T-L direction are weaker fracture planes, and this agrees with the Charpy V-notch impact tests results.

6.10.3 Fracture Toughness Tests

In order to obtain a true fracture toughness of the material, the CTOD fracture toughness of the chord material is tested by using the specimens that are cut from the chord wall in accordance with BS7448-1 (1991). The specimen orientation is the T-L direction which gives the lowest Charpy-V notch impact energy as shown in Fig. 6.48. The test is performed by DNV (Det Norske Veritas Pte. Ltd.) in Singapore, and the set-up of the specimens is shown in Fig. 6.49. The test results are tabulated in Table 6.7.

The average critical CTOD, δ_c , is 0.06 mm and the fracture surface of these three specimens is clean and brittle as shown in Fig. 6.50.

6.11 Failure Assessment

In order to analyze the data obtained from the full-scale experimental tests, the Failure Assessment Diagram (FAD) method is employed in this study for these damaged SHS T-joints. FADs are used to consider failure by linear elastic fractures as one limiting criteria and failure by plastic collapse as the second criteria. When performing a structural integrity assessment of a flaw in a stressed structure, an assessment point is derived from two different calculations and plotted on the diagram. The structure is deemed unsafe if the point calculated lies on the curve or falls outside it, and it is safe if the point is within the curve.

For the surface cracks in the specimens T1 and T2, the maximum crack driving force (SIFs or CTOD) is found at the deepest point of the crack. Therefore, the SIFs or CTOD at this point should be used to calculate the K_r values. Because the region of the deepest point of the surface crack is always considered under plane strain state, the critical fracture toughness must use the plane strain fracture toughness which is size independent. Anderson and Dodds (1991) indicated that size independent CTOD values for cleavage fracture can be obtained if the following criteria are met, i.e.

$$B \geq 300\delta_c \quad (6.13)$$

where B is the specimen thickness. The δ_c is equal to 0.06 mm. Hence, the thickness of the specimen for plane strain CTOD should be larger than 18 mm. Because the thickness of the chord wall is only 15 mm, the thickness of the specimens for CTOD test is equal to 14 mm. The value of δ_c only gives an approximation of the plane strain fracture toughness. The calculated values of K_r are tabulated in Table 6.8.

For the through-thickness crack of specimen T3, the CTOD under the load where the crack started to extend is calculated and tabulated in Table 6.9. The critical CTOD, δ_c , with the same thickness as the chord wall is adopted in this study. The K_r value is calculated and tabulated in Table 6.10.

In order to obtain the L_r values of the specimens, the plastic collapse load of the cracked T-joints is calculated using the formulae given earlier in Chapter 5, and the results of L_r are tabulated in Table 6.11.

Fig. 6.51 shows the failure assessment diagrams (FAD) for the specimens using BS7910 (1999) Level 3A analysis. In accordance with the FAD, it can be seen that all the T-joint results fall outside the standard curve. Therefore, if the standard Level 3A FAD is used to assess the cracked SHS T-joint, the value of critical load is less than the value of crack initiation load. Hence, the use of the standard BS7910 (1999) Level 3A FAD is safe for the cracked SHS T-joint under brace end axial loads. Because the highest crack tip driving force and the lowest fracture toughness is used in the calculation of K_r values, it can be found that the result is quite conservative.

For the surface crack, the maximum CTOD which was located in the corner, and the minimum critical CTOD δ_c which was in the T-L direction were used in this diagram. Therefore the K_r values were much higher. In accordance with the observations from the experiments, the crack tearing did not initiate around the corner, it initiated from the crack front parallel to the chord side wall where the critical CTOD is lower, and the crack propagation direction is the T-S direction. From the previous studies about the stress intensity factors (SIFs) along the crack front by Chiew et al. (2003), the SIFs around the corner is much higher than that along the chord side wall. If the SIF values in the crack tearing initiation part were employed to calculate the K_r values, the degrees of conservatism decreased as shown in Fig. 6.52.

For the through-thickness crack, the minimum δ_c which was in the T-L direction, and the experimental CTOD which in the L-T direction were used. In accordance with the Charpy V-notch impact test results, it can be seen that the fracture toughness in the L-T direction was much higher than it in the T-L direction. If the K_r data was corrected using Charpy impact test results, the FAD for the specimen T3 was plotted in Fig. 6.52 too. It can be found the degrees of conservatism also decreased.

Fig. 6.1 Fatigue experiment by Chiew et al. (2002, 2003)

Fig. 6.2 The actuators for applying the fatigue load

Fig. 6.3 The clay model made from the crack surface (Chiew et al., 2003)

Fig. 6.4 The cross section of the clay model used to measure the curved crack (Chiew et al., 2003)

Fig. 6.5 Test rig

Fig. 6.6 Setup of the specimen

Fig. 6.7 The test controller and recording system

Fig. 6.8 The weld details (AWS, 2000)

Fig. 6.9 The crack shape measured from the first crack of specimen T1

Fig. 6.10 The crack shape measured from the second crack of specimen T1

Fig. 6.11 The crack shape measured from the specimen T2

Fig. 6.12 The crack shape measured from the specimen T3

Fig. 6.13 The ACPD theory and notation

Fig. 6.14 A plane view showing the cracks, ACPD probes and LVDTs location of specimen T1

Fig. 6.15 A plane view showing the crack, ACPD probes and LVDTs location of specimen T2

Fig. 6.16 A plane view showing the crack, ACPD probes and LVDTs location of specimen T3

Fig. 6.17 The ACPD probes spot-welded around the weld toe

Fig. 6.18 The U-10 and computer system set-up used to record the crack propagation

Fig. 6.19 Set-up of the ACPD probes and LVDTs along the fatigue crack

Fig. 6.20 The dimensions of the LVDTs before the test

Fig. 6.21 The dimensions of the LVDTs during the test

Fig. 6.22 The recording system set-up used during the test process

Fig. 6.23 Load-displacement curves at the brace end

Fig. 6.24 The final failure of specimen T1

Fig. 6.25 The final failure of specimen T2

Fig. 6.26 The final failure of specimen T3

Fig. 6.27 Load-CMOD curves of specimen T1

Fig. 6.28 Load-CMOD curves of specimen T2

Fig. 6.29 Load-CMOD curves of specimen T3

Fig. 6.30 The assumed crack deformation at the crack tip

Fig. 6.31 Extrapolation of the crack mouth opening displacements

Fig. 6.32 ACPD readings of specimen T1

Fig. 6.33 ACPD readings of specimen T2

Fig. 6.34 ACPD readings of specimen T3

Fig. 6.35 The first crack surface of specimen T1

Fig. 6.36 The second crack surface of specimen T1

Fig. 6.37 The Crack surface of specimen T2

Fig. 6.38 The Crack surface of specimen T3

Fig. 6.39 A typical crack surface of the damaged T-joint

Fig. 6.40 The cross-section of the crack surface at the deepest point

Fig. 6.41 Locations of specimens used for the coupon tests

Fig. 6.42 Dimensions of the coupons

Fig. 6.43 Set-up of the coupon tests

Fig. 6.44 Stress-strain curves of the material

Fig. 6.45 The directions of the charpy V-notch specimens

Fig. 6.46 The charpy V-notch specimens after the tests

Fig. 6.47 The broken surface of the charpy V-notch specimens

Fig. 6.48 The specimen orientation for CTOD fracture toughness test

Fig. 6.49 The set-up of the 3PB specimen

Fig. 6.50 The fracture surface of the 3PB specimens

Fig. 6.51 FAD plotted using maximum crack tip driving force and minimum fracture toughness

Fig. 6.52 FAD plotted using the crack tip driving force at the location where the crack tearing initiated and fracture toughness in the crack propagation direction

Table 6.1 Dimensions of the Specimens

Specimen	b_0 (mm)	h_0 (mm)	t_0 (mm)	b_1 (mm)	h_1 (mm)	t_1 (mm)
T1	350	350	15	200	200	16
T2	350	350	15	200	200	12
T3	350	350	15	200	200	10

Table 6.2 Mechanical Properties of the Material Provided by Corus

Material	Yield Strength (N/mm ²)	Tensile Strength (N/mm ²)	Elongation (A%)	Charpy V-notch Temperature (°C)	Charpy V-notch Energy (J)
BS4360-50D	380.3	529.0	33	-20	170.6

Table 6.3 Mechanical Properties of the Weld (AWS, 2000)

Electrodes	Yield Strength (N/mm ²)	Tensile Strength (N/mm ²)
E70XX	399.9	482.6

Table 6.4 The Weld and Fatigue Crack Details

Specimen	Cracks	Crack Length L (mm)	Crack Depth a (mm)	Weld Width t_w (mm)
T1	1	275	12.7	12
	2	125	9.0	
T2	1	262	13.6	12
T3	1	290	Through	10

Table 6.5 Coupon Tests Results

Coupon	Yield Strength (N/mm ²)	Tensile Strength (N/mm ²)	Elongation (A%)	Young's Modulus (GPa)
Coupon 1	376.7	513.8	35	201.0
Coupon 2	386.1	507.7	32	199.5
Coupon 3	388.4	510.3	34	183.1
Coupon 4	384.9	513.9	33	187.0
Coupon 5	387.1	518.4	35	209.6
Coupon 6	388.6	502.7	32	183.1
Average	385.3	511.1	33.5	194.0

Table 6.6 The Charpy Impact Energies

Specimen Directions	Charpy V-notch Temperature (°C)	Charpy V-notch Energy (Joules)	K_{mat} (N/mm ^{3/2})
L-S	20	300	6840
T-S	20	69.3	3377
T-L	20	58.0	3082
L-T	20	172.7	4665.7

Table 6.7 The CTOD Fracture Toughness

Specimen directions	Test temperature (°C)	CTOD values (mm)	Average CTOD value δ_c (mm)
T-L	23	0.08	0.06
T-L	23	0.04	
T-L	23	0.05	

Table 6.8 The Results of K_r ($K_r = \sqrt{\frac{\delta_{CTOD}}{\delta_{IC}}}$) for Specimens T1 and T2

Specimen	Load (kN)	δ_{CMOD} (mm)	γ	Inclined Crack Depth (mm)	δ_{CTOD} (mm)	δ_C (mm)	K_r
T1	536.0	0.818	0.035	17.5	0.21	0.06	1.87
T2	711.0	1.45	0.05	21.2	0.39	0.06	2.55

Table 6.9 The Crack Tip Opening Displacement (CTOD) Results for Specimen T3

Specimen	Load (kN)	X_1 (mm)	d_1 (mm)	X_2 (mm)	d_2 (mm)	X_3 (mm)	d_3 (mm)	δ_{tip1} (mm)	δ_{tip2} (mm)
T3	438.0	52.0	2.38	243.0	2.9	295.0	1.37	0.21	0.56

Table 6.10 The Results of K_r ($K_r = \sqrt{\frac{\delta_{CTOD}}{\delta_C}}$) for Specimen T3

Specimen	δ_{CTOD} (mm)	δ_C (mm)	K_r
T3	0.56	0.06	3.0

Table 6.11 The Results of L_r ($L_r = \frac{P_0}{P_u}$)

Specimen	Cracks	L_{c1} (mm)	L_{c2} (mm)	Plastic Collapse Load P_u (kN)	Tearing Initiation Load P_0 (kN)	L_r
T1	1	67	217	866.25	536	0.62
	2	47	92			
T2	1	77	187	928.11	711	0.76
T3	1	70	210	811.0	438	0.54

CHAPTER 7

NUMERICAL ANALYSES OF TUBULAR SQUARE HOLLOW SECTION (SHS) T-JOINTS

7.1 Introduction

In the fitness for purpose or fitness for service evaluation of welded structures containing defects, the most widespread and successful approach is the failure assessment diagram (FAD). Based on this approach, API RP579 (2000), BSI Document BS7910 (1999) and R6 (2001) give guidances for assessing the acceptability of defects in welded structures. This approach simplifies the treatment of structures containing defects by bypassing the post yield fracture mechanics theories. It assumes the failure occurs by one of the two mechanisms. The first is fast fracture under conditions where linear elastic fracture mechanics (LEFM) can adequately describe the failure behaviour. The second is ultimate plastic collapse of the structure taking account of the presence of the crack. The fracture parameter (K_r or $\sqrt{\delta_r}$) is the ratio of the applied crack tip driving force against the fracture toughness obtained either by the CTOD or K_{IC} approaches. Since the effects of crack tip plasticity are allowed for by the shape of the failure assessment diagram, only the elastic component of the applied crack tip driving force is used in the calculation. The plastic collapse parameter S_r or L_r is the ratio of applied load to flow strength collapse load, or plastic collapse load of the flawed structure.

Although these codes and standards provide guidances in the form of detailed procedures for fatigue and fracture assessments, they are intended for general applications and do not necessarily give optimal solutions for all types of structures such as welded tubular joints. Because of the complexity of the tubular joint geometries, there are uncertainties with essential fracture parameter solutions as both experimental and numerical studies are costly. Due to the complex 3D stress state near the rectangular tubular joint brace-chord intersection where the crack is located, the chord wall is under combined tension and bending forces, and hence the

material at the crack tip behaves in a mixed mode of tension and shear, and the cracks may be non-planar growing along a doubly curved surface (Chiew et al., 2003). These make the numerical analyses of rectangular tubular joints a complicated job.

In offshore engineering, some researchers had studied the ultimate strength of cracked tubular joints (Kristiansen and Turner, 1993; Cheaitani and Burdekin, 1994; Skallerud et al., 1994; Stacey et al., 1996; Burdekin, 2001). Based on these research works, it had been suggested that the effect of the cracks on the ultimate plastic collapse strength can be estimated by the reduction factor which depends on the cracked area and the geometry applied to lower bound estimates of uncracked strength (Burdekin, 2001). This conclusion has been used as the basis for defect assessment in tubular joints using BSI document PD6493 (1991) and its successor BS7910 (1999). But most of their work is focused on circular hollow section joints. Information for cracked square or rectangular hollow section joints is very rare. In this Chapter, the fracture behaviour of cracked square hollow section T-joints and the assessment of structural integrity of this type joints will be investigated numerically. Hereafter, the two assessment parameters, the plastic collapse load and the crack driving force of cracked joints which are loaded by brace end axial loads will be obtained by non-linear FE analyses. The numerical results will be compared with the BS7910 (1999) approach. Load-displacement curves for uncracked and cracked square hollow section (SHS) T-joints in the same load cases will be determined from non-linear numerical analyses, and they will be compared with full-scale experimental tests data which were discussed in Chapter 6.

7.2 Numerical Modelling

In order to model the welded SHS T-joint accurately, correct choices have to be made on the use of element types, mesh density, integration scheme, the crack and weld shape modelling, and the loading and boundary conditions. There is no standard solution because they depend entirely on the combination of geometry, type of forces, linear or non-linear analysis and the desired accuracy. It is especially so near the region close to the crack tips where the singular nature of the crack tip

stress field must be recognized in a linear or non-linear fracture mechanics approach in discretizing the crack region. In the large-strain analysis, the mesh must be sufficiently refined to model the steep strain gradients around the crack tip as the details are required in this region.

7.2.1 Loading and Boundary Conditions

The boundary conditions used in this study are shown in Fig. 7.1 where the nodes are free at the brace end and pinned at the two chord ends. These boundary conditions correspond to the test rig conditions used in the experimental tests discussed in Chapter 6. In fact, the chord is sufficiently long, and hence the effect of the chord ends boundary conditions on the failure of the damaged joints is negligible. The loads are applied on the brace end along the axial direction as shown in Fig. 7.1, and they are applied by load control or displacement control in the tests.

7.2.2 Material Properties

Corresponding to the tests, the material used is BS4360 structural steel of grade 50D, and the true stress-strain curves have been obtained from 6 coupon tests described in Chapter 6. A piecewise multi-linear relationship is approximated from these curves as shown in Fig. 7.2 so as to simulate the true stress-strain curve in the numerical analyses. The Von Mises yield criterion and isotropic strain hardening are assumed here.

7.2.3 Material and Geometric Non-linearity

In the FE analysis, the PLASTIC parameter in ABAQUS (2002) is used to specify the plastic part of the material model. The material model used expresses the yield and post yield stresses as a function of plastic strain. In ABAQUS (2002), it is recommended to use the true and not the engineering stress-strain curve shown in Fig. 7.2.

It is a common practice to assume that the displacements and strains developed in a structure are small in the analytical problems. In practice, it means that the geometries of the elements remain basically unchanged during the loading process and linear strain approximation can be used accordingly. However, such assumptions fail frequently even though actual strains may be small and elastic limits are not exceeded for common structural materials. If accurate determination of the displacements is required, geometrical non-linearity may have to be considered in certain structures. For instance, stresses due to membrane action usually neglected in plate flexure may cause a considerable decrease of displacements as compared with the linear solution even though displacement is still quite small. Conversely, it may be found that a load is reached where deflections increase more rapidly than predicted by a linear solution. Furthermore, very large displacements can occur without causing large strains (Zienkiewicz et al., 1978) in many cases. For RHS joints, large displacements can occur which are usually associated with membrane forces in the chord face. Since equilibrium conditions between internal and external forces have to be satisfied, geometrical non-linearity has to be modeled in the structures. In ABAQUS (2002), geometrical non-linearity is accounted for by means of using the NLGEOM parameter which has been used in combination with the material non-linearity parameter in this study.

7.2.4 Element Selection and Mesh Density

Mesh design has an important influence on the accuracy of finite element analysis. Generally, meshes should be designed in such a manner that smallest elements are used in the high stress gradient regions. In order to have a better understanding of choice of mesh size, an analyst should attempt to carry out finite element analysis on a number of meshes having different numbers of elements, and to choose the mesh size which if refined further does not affect, to a large extent, the solution, i.e. until all refined meshes converge to the same solution. In fact, to do this for the whole range of geometries would be impracticable and very expensive, and the actual mesh size chosen in this study is a compromise between achieving acceptable accuracy and reducing the computer cost of the analysis.

Based on the numerical model introduced in Chapter 4, a typical mesh of a cracked square hollow section (SHS) joints is generated automatically as shown in Fig. 7.3. This mesh is used in the present analysis, and it can be modified depending on the joint geometries. For a T-joint, three layers with 64 elements in each layer are used around the section in the weld region, and only one layer with half of the elements around the section is adopted far away from the weld region. All the meshes are generated automatically using the developed program described in Chapter 4.

In order to check the performance of the mesh design and the convergence of the mesh, a SHS T-joint with chord width of 350 mm, brace width of 250 mm, chord and brace thickness of 16 mm and weld thickness of 8 mm is modelled using different mesh density. The crack in this T-joint is located at the corner of the brace, and the crack depth and length are 10 mm and 182 mm respectively. In the elastic analysis, the joint is subjected under axial load of 110 kN, in-plane bending (IPB) load of 2.0 kNm and out-of-plane bending (OPB) load of 1.4 kNm. The stress intensity factors calculated using the original mesh, doubled mesh and local refined mesh are obtained directly from ABAQUS (2002), the general purpose finite element computer program. The doubled mesh and local refined mesh are shown in Figs. 7.4 and 7.5. The SIF results are calculated and plotted in Fig. 7.6. From these results, it can be seen clearly that the results have converged, and the local refined mesh gives a very good prediction of the stress intensity factors.

Non-linear analyses of this cracked SHS T-joints subjected under brace end axial loads are performed to check the convergence of the different meshes. The load-displacement curves of the joints analysed using the original mesh, doubled mesh and local refined mesh are shown in Fig. 7.7. It is found that the analyses of the original mesh and doubled mesh have terminated before the analyses are completed. This problem was also mentioned by Du and Hancock (1989) and Kristiansen et al. (1992), and they indicated that the early termination appears to be due to the non-linear geometry analysis causing the convergence problems. In order to understand and overcome this problem, a large number of joints with various

mesh density are investigated in this study. In accordance with the 3-D non-linear analyses of cracked SHS T-joints, it is found that the most crucial factor influencing the convergence of the analyses is the aspect ratio of the elements around the crack front. It is suggested that the length of the elements should not be more than 3 times of the radius. Because the radius of the element around the crack front is very small, the length of element along the crack front must be relatively small too. Therefore, the local refinement of the element along the crack is absolutely necessary. In accordance with Fig. 7.7, the local refined mesh gives a good load-displacement curve. Hence, the local refined mesh has been adopted for most of the cases in this study.

Yang (1996) analysed several general cases of cracked components, such as three-dimensional compact tension (CT) specimens and pipe-plate joints, to explore the behaviour of J -integral values and crack tip open displacement (CTOD). It had been concluded that

- 1) At least three rings of elements should be used so that J -integral values can be calculated from sufficient contours to check the path independence of the results. The radii of rings should be small, especially for the first ring. A model with rings near the crack tip is also very useful to get more exact values of CTOD. It is suggested that the radius of the first ring should be less than 0.1 times the thickness.

- 2) The influence of the element numbers around a ring on J -integral is negligible. However, the eight-element model as shown in Fig. 7.8 is recommended because the connection of this model with the adjacent elements is straightforward.

- 3) The influences of the tied, untied and blunted crack tip models (please refer to Fig. 7.8) on J -integral are small. In 3-D problems, meshes with untied or blunted tip modes are difficult to converge.

The tied, untied and blunted crack tip modes are investigated using ABAQUS (2002) in this study. SHS T-joint with the chord width of 350 mm, brace width of 200 mm, chord and brace thickness of 16 mm and weld thickness of 8 mm is analyzed. The ratio of crack depth against chord thickness, a/T , is 0.66, and the crack length is 220 mm. The deformations of the crack tip using tied and untied

modes are shown in Figs. 7.9(a) and 7.9(b) respectively. While the load-displacement curves under different types of crack tip modes are plotted in Fig. 7.10. It is found that the influence of the crack tip modes on plastic collapse can be neglected. The same conclusions of Yang (1996) were also found where joint with untied and blunted crack tip modes is more difficult to converge than the joint with the tied crack tip mode. As an example, the joint with tied crack tip mode required only 22 steps to complete the calculation, but the joint with untied crack tip mode uses 78 steps to complete the calculation. If the aspect ratio of the elements around the crack front is less than 3, the convergence problem can be avoided completely. Table 7.1 shows the elastic-plastic J -integral, J_{ep} , using tied and untied crack tip modes, and it can be found the influence of different types of the crack tip modes are small when the load is small, but when the extensive yielding occurs, for instance the load is very close to the plastic collapse load, the difference of the J -integral calculated from different types of crack tip modes can be as large as 9.5%.

In accordance with the discussion of Chapter 3 and considering the singularity nature of the crack tip, the tied crack tip mode with collapsed prism elements is used in the elastic analyses, and the untied crack tip mode with prism elements is used in the elastic-plastic analyses. Fig. 7.11 illustrates the untied crack tip deformation of the through-thickness crack for the elastic-plastic analysis. In the linear elastic analysis of the T-joint containing through-thickness crack, the deformation of the crack tip is shown in Fig. 7.12.

Table 7.2 tabulates the elastic J -integral, J_e , values. This cracked joint is subjected under a load close to the plastic collapse load of the joint. It can be seen that a very good path independence of the J -integral is achieved in the elastic analysis. In elastic-plastic analysis, the elastic-plastic J -integral, J_{ep} , is tabulated in Tables 7.3 and 7.4. It can be found that the J -integral calculated from the first ring is not accurate as indicated by ABAQUS (2002). The J -integral calculated from the second and third rings shows the path independence when the load is lower. When the load is very close to the plastic collapse load, the elastic-plastic J -integral

calculated from different rings has a slight difference. The difference is less than 10%. Hence, the proposed mesh is reasonable to be used for subsequent analyses.

7.3 Validation of Numerical Modelling

Three elastic-plastic analyses are conducted on the SHS T-joints under brace end axial loads using the ABAQUS (2002) finite element program. The dimensions of the joints and cracks are the same as those of specimens T1, T2 and T3, which are reported in Chapter 6. In the test of specimen T2 and T3, the crack tip propagated to the path that deviates from the weld toe slightly. Therefore, the crack length along the weld toe is assumed to be the same as the crack length along the true crack path for specimens T2 and T3 models.

Figs. 7.13, 7.14 and 7.15 illustrate the load-displacement curves of the specimens T1, T2 and T3 compared with the experimental and uncracked results. The first crack initiation for the uncracked joint and the crack propagation beginning point for the cracked joint are pointed out according to the 20% tensile strain in the outer surface criterion (Dexter and Lee, 1999) for the uncracked joint and the ACPD recording for the cracked joint. Because of the introduction of the cracks, the load capacity decreased greatly. The decrease is related to the crack size and the crack types. According to these load-displacement curves, the decreases are 20.5%, 14.0% and 25.8% for the specimens T1, T2 and T3 respectively. Comparing the numerical and the experimental curves, at the same displacement, the difference of the load is less than 15%. To affix the strain gauges around the weld toe, the chord surface was grinded smoothly, this process will decrease the chord thickness that was not modelled in the numerical model. And some small defect can not be avoid in the fabrication procedure, especially in the welding process, that was not modelled either. Hence, the numerical curve is slightly higher than the experimental curve. Because the crack propagation did not simulated, the numerical curve did not drop down even the experimental joint failed. Before the joint had failed, it can be seen that a good agreement is obtained even through there is a small ductile tearing that is not modeled in the analysis. It might be because the ductile tearing region is very small compared with the crack size.

Figs. 7.16, 7.17 and 7.18 show the crack mouth opening displacement (CMOD) versus load curves compared with the experimental recordings. It can be seen that good agreement is obtained for the specimens T1 and T2 which contain surface cracks. In specimen T3 which contains through-thickness crack, the difference is slightly larger. This is because the crack shape assumed in the numerical modelling is a rectangular one which is not the real shape of the through-thickness crack as shown in Fig. 7.19. However, the difference is still acceptable. Hence, the modelling of the crack surface is reasonable.

7.4 Calculation of J -integral

In this research work, the J -integral was computed using the virtual crack extension (VCE) technique which is built in ABAQUS (2002) finite element program. The VCE technique was developed independently by Parks (1974) and Hellen (1975) for the calculation of energy release rates in elastic bodies. Subsequently, Parks (1978) generalized the method to non-linear (elastic-plastic) materials. The finite element program interprets the J -integral as a function of position along the crack front which gives the decrease in total potential energy of the loaded structure caused by an increase in the crack opening area at that same position on the crack front. ABAQUS (2002) formulates this as follows:

$$-\delta P = \int_s J(s) \cdot \delta l(s) ds \quad (7.1)$$

where δP is the change in the structure's total potential energy, s is the coordinate which identifies position along the crack front as shown in Fig. 7.20, $J(s)$ is the J -integral value, and $\delta l(s)$ is the advance of the crack front normal to itself and in the local plane of the crack at this point.

The value of $J(s)$ and the virtual crack extension are then interpolated from the value at crack front nodal position using the same order of interpolation as used in the elements abutting the crack front. Thus, equation (7.1) becomes, in the finite element model, a set of equations which relate the set of total potential energy changes caused by crack advances at each crack front nodal position to the J -integral values of the same crack front nodal positions. Once these values are found,

the equations can be solved for these nodal positions values of J . The method is particularly attractive because it is simple to use, adds little to the cost of the analysis, and provides good accuracy (Aliabadi and Rooke, 1991).

In the 2-D conditions as shown in Fig. 7.21, the virtual change of potential energy, δP , is equal to the virtual change in integrated element strain energies, W , at constant nodal displacements resulting from a virtual change in crack length δa , which is achieved by a virtual change of the nodal point coordinate vector,

$$\delta P = \delta W = \sum_e \delta W^e \quad (7.2)$$

where δW^e is the virtual change of strain energy in one element. For linear elastic material behaviour it was shown that

$$\delta W^e = \frac{1}{2} \mathbf{u}^{eT} \cdot \delta \mathbf{K}^e \cdot \mathbf{u}^e \quad (7.3)$$

where $\mathbf{K}^e$ is the elastic stiffness matrix of the element, and it is estimated by

$$\delta \mathbf{K}^e = \mathbf{K}^e(a + \delta a) - \mathbf{K}^e(a) \quad (7.4)$$

Therefore, the evaluation of δP only requires the re-evaluation of the element stiffness matrices. If all nodes on and inside a contour Γ_0 are given a virtual movement δa in the X_1 direction and all nodes on and outside a second contour Γ_1 completely surrounding Γ_0 remain fixed at their original position as shown in Fig. 7.21. As a rigid movement of an element does not result in a change in energy within such elements, only the elements filling the gap between Γ_0 and Γ_1 need to be considered. Hence, only the element stiffness matrices of the elements between contours Γ_0 and Γ_1 need to be re-evaluated. This method was originally termed a “stiffness derivative” technique by Parks (1974).

In cases of non-linear material behaviour, Parks (1978) used the following steps to determine the δW^e . Let V^e be the element volume in real space (X_1 - X_2) and V^0 the element volume in the local space (η_1 - η_2). Then,

$$W^e = \int_{V^e} W dV^e = \int_{V^0} W |\mathbf{J}| dV^0 \quad (7.5)$$

where $|\mathbf{J}|$ is the determinant of the Jacobian $\mathbf{J}$ of the mapping from the local space to the real space. Differentiation of Equation 7.5 yields

$$\delta W^e = \int_{V^0} (W \delta |\mathbf{J}| + \delta W |\mathbf{J}|) dV^0 \quad (7.6)$$

in general,

$$\delta W = \sigma_{ij} \delta u_{ij} = \sigma_{ij} \delta \mathbf{J}_{jk}^{-1} \frac{\partial u_i}{\partial \eta_k} \quad (7.7)$$

Introducing Equation (7.7) into Equation (7.6) finally yields

$$\delta W^e = \int_{V^0} \left(W \delta |\mathbf{J}| + \sigma_{ij} \delta \mathbf{J}_{jk}^{-1} \frac{\partial u_i}{\partial \eta_k} |\mathbf{J}| \right) dV^0 \quad (7.8)$$

which involves derivatives of elements of $\mathbf{J}^{-1}$ and $|\mathbf{J}|$ only.

The VCE technique, as outlined above, is strongly linked to the finite element formulation. De Lorenzi (1981) derived an expression for the energy release rate from a continuum mechanics viewpoint, Equation (7.8) can be transformed to

$$\delta W^e = \int_{V^0} \left(W \delta x_{k,k} + \sigma_{ij} u_{i,k} \delta x_{k,j} \right) |\mathbf{J}| dV^0 \quad (7.9)$$

where δx_k are the virtual perturbations of the coordinates x_k at a point caused by δa . This formulation has the advantage that the variations δx can be determined analytically from the element shape functions, therefore, avoiding the use of the difference estimates of $\delta |\mathbf{J}|$ and $\delta \mathbf{J}^{-1}$ in Equation (7.8).

If the virtual change of strain energy in one element has been obtained, and in accordance with Equation (7.2), the virtual change of potential energy, δP , can be determined. Then, the J -integral can be calculated from Equation (7.1) numerically.

There are two approaches to the application of VCE techniques in the 3-D conditions as shown in Figs. 7.22(a) and 7.22(b). In Fig. 7.22(a), all the crack front

nodes are perturbed, while in Fig. 7.22(b) only one node is perturbed. The first approach was used by Cruse and Besuner (1975) to reduce elliptical cracks to two degrees of freedom by allowing elliptical cracks to be distorted into other elliptical cracks, changing only the size parameters a and c . The local perturbation approach provides a good estimate of the local value of J -integral or stress intensity factors K , and it is not limited to elliptical cracks only. Therefore, the number of degrees of freedom is greatly increased (Cruse, 1988). In this research the local perturbation approach is used to calculate the J -integral along the crack front.

The J -integral calculated from the VCE method is the total J -integral which includes the Mode-I, II and III J -integral together. To calculate the individual stress modes for a crack subjected to mixed-mode loading, Shih & Asaro (1988) had proposed an interaction integral method. They noted that, for the actual (mixed-model) crack field, the J -integral is given by

$$J = \frac{1}{8\pi} [K_I B_{11}^{-1} K_I + 2K_I B_{12}^{-1} K_{II} + 2K_I B_{13}^{-1} K_{III} + (\text{terms not involving } K_I)] \quad (7.10)$$

where K_I , K_{II} and K_{III} are the stress intensity factors for the Mode-I, II and III fracture respectively, and $\mathbf{B}$ is called the prelogarithmic energy factor matrix.

Then they suppose that an auxiliary pure Mode-I field, of intensity k_I , is superimposed on the actual field and the J -integral computed as

$$J_{\text{tot}}^I = \frac{1}{8\pi} [(K_I + k_I) B_{11}^{-1} (K_I + k_I) + 2(K_I + k_I) B_{12}^{-1} K_{II} + 2(K_I + k_I) B_{13}^{-1} K_{III} + (\text{terms not involving } K_I \text{ or } k_I)] \quad (7.11)$$

The auxiliary field itself has the J -integral

$$J_{\text{aux}}^I = \frac{1}{8\pi} k_I \cdot B_{11}^{-1} \cdot k_I \quad (7.12)$$

From the foregoing relations, an interaction J -integral can be defined as

$$J_{\text{int}}^I = J_{\text{tot}}^I - J - J_{\text{aux}}^I = \frac{1}{4\pi} (k_I B_{11}^{-1} K_I + k_I B_{12}^{-1} K_{II} + k_I B_{13}^{-1} K_{III}) \quad (7.13)$$

All three J -integral values, J , J_{tot}^I and J_{aux}^I , including the interaction J -integral, J_{int}^I , are computable from Equation (7.1). When the previously mentioned calculations

are performed for the other two modes, a simple linear system of equations can be obtained as

$$J_{\text{int}}^{\alpha} = \frac{k_{\alpha}}{4\pi} B_{\alpha\beta}^{-1} K_{\beta} \quad (7.14)$$

If k_{α} is assigned with unit values, then Equation (7.14) can be reduced to a form of

$$K_{\alpha} = 4\pi B_{\alpha\beta} J_{\text{int}}^{\beta} \quad (7.15)$$

It may be noted that J_{int} is ready evaluated with high precision via the domain integrals. Then the individual stress intensity factors for a crack subjected to mixed-mode loading can be obtained accordingly.

7.5 Construction of Failure Assessment Diagrams (FADs)

In the standard R6 (2001) Option 1 and BS7910 (1999) Level 2A and 3A for general failure assessment diagram, the assessment curve is presented as

$$K_r = (1 - 0.14L_r^2)[0.3 + 0.7 \exp(-0.65L_r^6)] \quad (7.16)$$

where

$$K_r = \frac{K_I}{K_{IC}} \quad (7.17)$$

$$L_r = \frac{\text{total applied load giving to } \sigma^p \text{ stresses}}{\text{plastic collapse load of the flawed structure}} \quad (7.18)$$

The value of L_r may also be related to a reference stress through

$$L_r = \frac{\sigma_{\text{ref}}}{\sigma_y} \quad (7.19)$$

It is important to recognize that the K_r parameter of the assessment diagram uses the linear elastic stress intensity factor with no allowance for the effect of plasticity on the crack tip driving force. As the L_r value increases so plasticity also increases the effective crack tip driving force. If it is considered that fracture actually occurs when the total effective crack tip driving force, the elastic plastic value of J_{ep} reaches a critical value equivalent to the fracture toughness, then this

will occur at $\sqrt{EJ_{ep}} = K_{IC}$. Since the applied linear elastic stress intensity factor is equivalent to $\sqrt{EJ_e}$ where J_e is the linear elastic J -integral, then

$$K_r = \frac{K_I}{K_{IC}} = \sqrt{\frac{J_e}{J_{ep}}} \quad (7.20)$$

As plastic increases so the ratio $\sqrt{J_e / J_{ep}}$ reduces, and this defines the shape of the assessment curve with the increasing of L_r . The standard assessment curve is originally derived for the case of a large plate under tension loading with a central crack. They have been shown to represent a lower bound curves for other common simple geometries.

Based on this numerical model, the failure assessment diagrams (FADs) for the tubular square hollow section (SHS) T-joints are constructed using the numerical J -integral approach (Burdekin et al., 1998) as discussed above. The vertical (fracture) axis is the ratio of the applied elastic crack tip severity to the fracture toughness (K_I/K_{IC} or $\sqrt{\delta_I/\delta_{IC}}$), and the abscissa (collapse) axis is the ratio of applied load to collapse load, but in reality it represents increasing plasticity towards general yield. With increasing plasticity the applied crack tip severity increases above the elastic value. Fracture is assumed to occur when the enhanced crack tip severity due to plasticity reaches the critical value for the fracture toughness. As plasticity increases the elastic component of the total crack tip driving force reduces, and this is represented by the shape of the failure assessment diagrams reducing in height and moving to the right on the abscissa as shown in Fig. 7.23. This is the basis for calculating the shape of a structure specific assessment diagram from finite element analysis results. For any given loading condition and cracked component, J -integral values are calculated separately for the elastic and elastic-plastic conditions. If failure did occur at any load condition, it is assumed that it would occur at the enhanced elastic-plastic value of the crack tip driving force. The ratio ($\sqrt{J_e/J_{ep}}$) then represents the ratio of elastic stress intensity factor to elastic-plastic fracture toughness in terms of K values (since $K^2 \propto EJ$). Plotting of this ratio at a series of

increasing loads then gives the height of the assessment diagram for each load. If the assessment curve calculated from the results of the numerical analysis falls outside the standard BS7910 (1999) assessment curve, the use of the standard diagram for assessment of cracks would be safe. On the other hand if the calculated assessment curve falls inside the standard curve, the use of the standard curve would be unsafe for any assessment points lying between the two curves. A critical feature of this process is the choice of the collapse load by which the applied load is divided to give the value.

7.6 Determination of Plastic Collapse Loads

The objective of welded joint assessment is to preclude several possible failure mechanisms by performing structural analysis of the welded joints to ensure that the design or service loads, stresses and strains meet specified limits defined in codes of practice such as BS7910 (1999). One of these failure modes, gross plastic deformation, is studied in this chapter using elastic-plastic finite element analysis.

Gross plastic deformation occurs when a welded structure of ductile material experiences excessive static load. Unless the load is limited, this ultimately leads to plastic collapse or rupture of the structure. Two types of stress analysis may be used to guard against gross plastic deformation, namely, elastic analysis and elastic-plastic analysis (Muscat et al., 2003). When elastic analysis is used, the allowable load is calculated indirectly by partitioning the elastic stress into primary, secondary and peak categories and limiting the primary stress to a specified allowable value. Stress categorization is probably the most difficult aspect of the elastic analysis procedure and it has become increasingly more difficult as stress analysis techniques have advanced. When the design is based on elastic-plastic analysis, the allowable load is determined directly from the elastic-plastic response of the welded structure. Two types of inelastic analysis may be used to characterise the response, and they are limit analysis and plastic analysis.

Limit analysis assumes an ideal elastic-perfectly plastic (or rigid-perfectly plastic) material model and small deformation theory. Elastic perfectly plastic

material model is shown in Fig. 7.24(a). The material exhibits linear elastic behaviour up to the yield stress of the material, after which unlimited plastic flow or perfect plasticity occurs. When perfect plasticity and small deformation theory are assumed, the load carrying capacity of the structure is limited by equilibrium considerations. A plot of applied load against resulting deformation for a hypothetical limit analysis is shown in Fig. 7.24(b). Initially, the structural response is linear elastic, but as yield is exceeded the regions of plastic strain develop and the response becomes non-linear, as loading continues equal increments of load cause increasingly greater plastic deformation. At the limit load, the load deformation curve becomes horizontal. The structure can no longer maintain equilibrium with the external loads and unlimited plastic flow occurs. The structure fails by loss of equilibrium at the limit load of the structure. The limit load may be taken as the load producing gross plastic deformation, although this may be a conservative estimate. This is a concise and easily understood design rule. However, real structures may behave rather differently to the limit analysis model in two ways, namely, the material may exhibit post-yield strain hardening and large deformations may occur.

Most steel exhibit linear elastic behaviour up to yield but thereafter the stress and strain increase in a non-proportional manner, as shown in Fig. 7.25(a). This post yield behaviour is called strain hardening. As strain hardening materials can support stresses greater than yield, plastic deformation can continue for loads above the theoretical limit load of the structure without violating equilibrium. Changes in structural configuration as loading progresses can also affect the strength of the structure. If large structural deformations occur, the structural load-path may change, either increasing or decreasing the load carrying capacity of the structure. In structure analysis terminology, elastic-plastic analysis including strain hardening and large deformation effects is called plastic analysis. A hypothetical plastic analysis load-deformation curve is compared with a limit analysis curve for the same structure in Fig. 7.25(b).

The significant problem in plastic analysis is defining a ‘plastic collapse load’ to be used as the basis for calculating the allowable static load for the structure, in the same way the limit load does in limit analysis. In practice, this is done by applying a criterion of plastic collapse. The purpose of this criterion is to define the load at which plastic deformation becomes excessive, not when actual physical collapse occurs. A large number of plastic collapse criteria have been proposed in the literature (Yura et al., 1980; Lu et al., 1994; Cheaitani and Burdekin, 1994; Muscat et al., 2003). In the structure analysis, the plastic collapse load is taken as the load producing gross plastic deformation and used to specify the allowable load. Most of the plastic criteria are originated from the experimental stress analysis.

In the previous research work on the ultimate strength of cracked tubular joints (Cheaitani and Burdekin, 1994), the twice-elastic compliance criterion was used to determine the deformation limits. Stacey et al. (1996) reviewed the investigations on the ultimate capacity of cracked tubular joints. In accordance with the elastic-plastic stress analyses and failure assessment diagram analyses results (Cheaitani, 1992; Laham and Burdekin, 1995; Du and Hancock, 1989; Kristiansen et al., 1992; Stacey et al., 1996), they indicated that the twice-elastic compliance criterion gave good agreement with the Level 3 FAD whilst the other criteria were significantly more conservative. This plastic criterion is specified in ASME VIII Division 2 as the twice-elastic-slope (TES) criterion. This plastic deformation criterion is adopted in this study.

The TES criterion is based on the load–deformation response of a structure as obtained by elastic–plastic analysis. A structural response curve is plotted with load as the ordinate and deformation as the abscissa, as shown in Fig. 7.26. The load and deformation are chosen such that the structural response is characterized adequately. Choice of an appropriate deformation parameter is essential to estimate accurate plastic load. The ASME (1995) code suggests that displacement or maximum principal strain may be used but no further guidance for the form and location of the parameter is given in it. The plastic collapse load, P_u , is the load corresponding to the intersection of the load–deformation curve and a straight line called the collapse

limit line, emanating from the origin of the load–deformation curve with twice the slope of the initial elastic response obtained using

$$\tan \varphi = 2 \tan \theta \quad (7.21)$$

where θ and φ are the angles measured from the load axis as shown in Fig. 7.26.

7.7 Numerical Analyses

Based on the numerical modelling of SHS joints, the actual SHS T-joints which contains surface cracks or through-thickness cracks subjected under brace end axial loads, are studied using ABAQUS (2002) finite element analysis program. The configurations of these SHS T-joints with surface cracks and through-thickness cracks are tabulated in Tables 4 and 5 respectively. The original surface crack details are given in Table 6, and the through-thickness crack details are given in Table 7. The geometry of the weld is in agreement with the weld sizes specified by the AWS (2000) specifications.

In the finite element (FE) analyses, the following investigations are performed: (1) load-displacement curves are evaluated to predict the plastic collapse load of cracked SHS T-joints using the twice elastic compliance criterion; (2) elastic crack driving force, J_e , and elastic-plastic J -integral, J_{ep} , are calculated for the purpose of a defect assessment; and (3) plastic collapse loads of cracked models are analyzed and compared with the formulae deduced from the yield line theory. The formulae are then used to predict the plastic collapse loads for cracked SHS T-joints.

It is well known that at the surface where the description of the crack tip is in a 3-D state, the half-power singularity in elastic stress and strain does not exist (Bazant and Estenssoro, 1979; Ruiz and Epstein, 1985). Therefore the usual stress intensity factor does not exist. In most cases of joints containing surface cracks, the maximum value of K_I occurs at the deepest point of the crack so that the problem is by-passed (Kristiansen and Turner, 1993). When the joints contain through-thickness cracks, the J -integral value at the node which is closest to the outer surface of the chord wall, is adopted by Burdekin and Yang (1997).

7.7.1 General Plastic Behaviour of SHS T-joint

For surface crack subjected with low applied load equals to 0.2 times of the plastic collapse load, the yield from the crack tip has spread from the ligament ahead of the crack in the thickness direction as shown in Fig.7.27, and the stress at the corners of the brace is relatively high. It can be seen that the yield initiated from the crack tip and the corners. Final collapse corresponds to yield having spread the whole intersection and through the chord wall thickness is shown in Fig. 7.28. The chord face stress distribution of the uncracked joint under the plastic collapse load and that of the cracked joint with the same geometry under the plastic collapse load are shown in Fig. 7.29 and 7.30 respectively. It can be found that the yield region of the cracked joints under the plastic collapse load is similar to the yield region of the yielded uncracked joint that has the same geometry. Therefore, the assumption of the yield line discussed in the Chapter 5 to derive the plastic collapse load of the cracked joints with surface crack is reasonable.

For through-thickness crack, the yield also initiates from the crack tip and the corners as shown in Fig. 7.31, and then the yield spread to the whole intersection between the chord and brace except the middle part of the crack as shown in Fig. 7.32. Unlike the joint with surface crack, there are no ligaments along the crack, the region in the middle of the through-thickness crack keep a very low stress level. Hence, the yield line assumptions for through-thickness is quite complicated as discussed in Chapter 5. Figs. 7.33 and 7.34 illustrate the chord face stress distribution for uncracked and through-thickness cracked joints with the same geometry under these plastic collapse loads. It can be found that the yield region of the cracked joint do not change considerably compared with the uncracked joint. Therefore, the yield region in the yield line assumption discussed in Chapter 5 keeps the same as the uncracked one, but the yield line pattern around the through-thickness crack has changed satisfying the stress distribution as shown in Fig. 7.34.

7.7.2 The Plastic Collapse Strength of SHS T-joint

Three categories of SHS T-joints are studied in this research work, the brace wide to chord width β ratio is 0.43, 0.57 and 0.71 respectively. In the category of

$\beta=0.57$, different weld geometry is analyzed to study the influence of the weld width on the plastic collapse strength of the cracked SHS T-joints. Hence, there are 4 series of cracked SHS T-joints are analyzed in this study for joints containing surface crack or through-thickness. The geometries of the joints are tabulated in Table 7.5. The detailed crack dimensions of the surface and through-thickness cracks are given in Tables 7.6 and 7.7 respectively.

Non-linear analyses are performed to obtain the load-displacement curves of these T-joints. Compared with the results of uncracked joints, the load-displacement curves are illustrated in Fig. 7.35 to Fig. 7.42. Based on these load-displacement curves, the plastic collapse loads of the cracked joints are calculated using the twice elastic compliance criterion, and the results of the joints containing surface crack and through-thickness crack are tabulated in Table 7.8 and 7.9 respectively.

The plastic collapse loads, P_u , which are calculated from the Equation (5.12) for surface cracked joint and Equation (5.27) for through-thickness cracked joint, are also tabulated in Tables 7.8 and 7.9 for comparison with the numerical results. It can be found that a good agreement is obtained from these results. The difference between the numerical results and the plastic collapse loads calculated using the above equations is less than 10%. For convenience and general validity, the influence of the welds is neglected for butt and fillet welds, and b_0-2t_0 is taken to be b_0 in practice (Wardenier, 1982). Hence, the results, P'_u , which neglect the weld and chord wall thickness influence are calculated and tabulated in Tables 7.8 and 7.9. It can be seen that this results are quite conservative, especially for high β ratio joints. When the β ratio is 0.71 the difference can be over 40% compared with the numerical results.

7.7.3 Influence and Definition of Contact Crack Surface

The crack surfaces on the joints subjected to certain types of loads may contact and penetrate each other due to compressive stresses. Under this situation, contact surfaces should be modelled on the crack surfaces in numerical models. Therefore,

the influence of the contact surface modelling on the plastic collapse results and stress intensity factor (SIF) of the cracked joints should be investigated too.

For joints with surface crack under brace end axial loads, the mode-I stress intensity factors are presented in Figs. 7.43 and 7.44 for a shallow crack ($a/t_0=0.4$) and a deep crack ($a/t_0=0.85$) respectively. The geometries of the joint is defined as $b_0=350$ mm, $b_1=200$ mm, $t_0=t_1=16$ mm, $t_w=12$ mm. When a brace end axial load of 115 kN is applied, it can be seen that all the K_I values are positive. Therefore, no contact occurs on the crack surfaces, and the results calculated from the model with contact surface definition is the same as that calculated using the model without the contact crack surface definition. The opening crack in the non-linear analysis is shown in Fig. 7.45. It is very clear that there is no contact between the two crack surfaces. Hence, in the numerical model of SHS T-joint containing a surface crack under brace end axial loads, there is no need to define the contact crack surface.

The Mode-I SIF (K_I) results along the chord wall thickness of the joint which contains a through-thickness crack, are illustrated in Figs. 7.46 and 7.47. The geometries and load conditions are the same as the model previous used to study the influence on contact surface crack. The negative K_I values in the crack tip2 without contact surface definition illustrate that the lower part of the crack has penetrated each other as shown in Fig. 7.48. The penetration of the elements on the two crack surfaces is not true. Hence, contact crack surface should be specified in the model. The K_I values at crack tip1 and crack tip2 calculated from the model with contact crack surface definition are illustrated in Figs. 7.46 and 7.47, and they are compared with the results using the model without contact crack surface definition. In Fig. 7.46, all of the K_I values are positive. Therefore, the crack surfaces close to the crack tip1 do not touch each other. It can be found that the K_I values calculated from the two models are the same. But in Fig. 7.47, it can be found that the contact surface affects the K_I values significantly. Because of the influence of the contact between the two crack surfaces, the K_I value is greater than the one without the contact surface definition. The stress and deformation of the crack surface at the crack tip2 in the model with the contact crack surface definition is shown in Fig.

7.49. It can be seen that the two crack surfaces are in contact but they do not penetrated each other.

The influence of contact crack surface definition on the Mode-II and Mode-III stress intensity factors (K_{II} and K_{III}) are also studied and shown in Figs. 7.50, 7.51, 7.52 and 7.53. It can also be seen that the results calculated from the models with and without contact surface definition do not change a lot at the crack tip1 in which no contact is found, but they are much different at the crack tip2 in which the crack surfaces are in contact. The same conclusion is also found for the J -integral values at crack tip1 and crack tip2 as shown in Fig 7.54 and 7.55 respectively. Thus, contact crack surface should be defined in the FE models for joints with through-thickness crack.

In the non-linear analysis of the joints with through-thickness crack, it is more difficult for the model to converge with contact surface definition than the model without contact surface definition. Therefore, much more computer time is needed to complete the analyses. However, the load-displacement curves obtained using these two models are almost the same as shown in Fig. 7.56.

7.7.4 Mixed-mode Fracture Effects

Based on the experimental results, it is found that weld toe surface crack propagates following a curved path in the depth direction of the chord. Therefore, it is common to model a weld toe crack surface as a doubly warped surface. In addition to the crack shape complexity, a simple brace end axial loading on SHS T-joints can produce a mixed-mode behaviour. Hence, the influence of the mixed-mode fracture must be considered in the assessment of cracked SHS T-joints.

Because of the complexity of the geometry and the stress distribution due to interaction between the chord and the brace, the values of the resulting mixed mode stress intensity factors can not be determined analytically (Pook, 1993). Hence, the BS7910 (1999) and the R6 documents recommend a procedure to deal with such cases based on limited mechanistic understanding of mixed-mode loading and

experimental data. Essentially, the procedure uses Mode-I material properties, fracture toughness and yield strength, and Mode-I failure assessment diagrams. The Mode-II and III contributions are incorporated in the calculation of the effective stress intensity factor, K_{eff} , which is a function of the stress intensity factors due to Mode-I, II and III. In BS7910 (1999), the effective stress intensity factor (K_{eff}) is expressed as

$$K_{\text{eff}} = \sqrt{K_I^2 + K_{II}^2 + K_{III}^2 / (1 - \nu)} \quad (7.22)$$

where K_I , K_{II} , and K_{III} are the stress intensity factors of Mode-I, II and III respectively, ν is the Poisson's ratio.

In this study, a SHS T-joint is analysed again. The geometry and load condition is the same as the joint used to study the contact crack surface definition influence. The stress intensity factors K_I , K_{II} , K_{III} and the effective stress intensity factor, K_{eff} are plotted in Figs. 7.57 and 7.58 for a shallow surface crack ($a/t_0=0.4$, $\theta=30^\circ$) and for a deep surface crack ($a/t_0=0.85$, $\theta=45^\circ$) which are measured from the specimens. It can be seen that K_{II} and K_{III} are much smaller than K_I . Therefore, K_{eff} is very close to K_I , but the crack propagation direction is affected by K_{II} and K_{III} . Hence, there is a distinct kink in the intersection between the pre-fatigue crack surface and the fracture surface in the actual specimen as discussed in Chapter 6.

To determine the crack propagation angle θ_0 in the normal plane, the maximum principal stress criterion (Erdogan and Sih, 1963) was adopted in this study. This theory states that cracks grow in a direction perpendicular to the maximum principal stress at the crack tip. If a local coordinate system shown in Fig. 7.59 is taken, then the polar coordinate form of the crack tip circumferential stress derived from the Cartesian coordinate form (Aliabadi, 1991) can be written as

$$\sigma_\theta = \frac{1}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left[K_I \cos^2 \frac{\theta}{2} - \frac{3}{2} K_{II} \sin \theta \right] \quad (7.23)$$

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

Based on the maximum principal stress criterion, the multi-axially loaded crack tends to grow from the crack front into the direction which is perpendicular to the maximum principal stress. This means that the crack will extend perpendicular to the direction where σ_θ is a maximum and $\tau_{r\theta}$ is zero. According to the expression of $\tau_{r\theta}$, the critical angle θ_0 can be calculated as

$$K_I \sin \theta_0 + K_{II}(3 \cos \theta_0 - 1) = 0 \quad (7.24)$$

then

$$\theta_0 = 2 \tan^{-1} \left[\frac{1}{4} \frac{K_I}{K_{II}} \pm \frac{1}{4} \sqrt{\left(\frac{K_I}{K_{II}} \right)^2 + 8} \right] \quad (70.5^\circ \leq \theta_0 \leq -70.5^\circ) \quad (7.25)$$

This relationship is also derived by Pook (2002) using the “extended small branch crack” approach. Comparison with data for several configurations, Pook (1980) suggested that this equation gives θ_0 to within 5° for Mode-I branch cracks. For pure Mode-II, it gives $\theta_0 \approx 70.5^\circ$.

According to Equation (7.25), the crack propagation angle θ_0 is calculated for the surface crack. The angle for the shallow surface crack varies from -20° to -0.06° , and for the deep surface crack, it varies from -28° to 26° along the crack front. Because the crack surface in the numerical model is simplified as compared with the true curved crack surface and this equation does not consider the influence of the Mode-III fracture, there are some differences between the predicted and the experimental results as shown in Fig. 7.61. However, it is found that Equation (7.25) seems to produce a reasonable prediction of the crack propagation direction.

For the through-thickness crack, the condition is much more complex than the surface crack, the SIF values of Mode-II and Mode-III can not be neglected compared with the value of K_I as shown in Figs. 7.62 and 7.63. In Fig. 7.63, the value of zero is used to replace the negative K_I value which is suggested by ABAQUS (2002). Hence, it is found that the effective SIF results are very different from the pure Mode-I SIF results, especially at the crack tip2 where the two crack

surfaces are in contact. Hence, the mixed-mode fracture must be considered carefully in the assessment of the SHS T-joints containing through-thickness cracks.

7.7.5 Influence of Crack Inclined Angle

In accordance with the fatigue experimental results (Chiew et al., 2003), the fatigue crack surface under the weld toe of a SHS T-joint is inclined and varied from approximate 30° to 60° with the increase of the crack depth. Because the crack inclined angle depends on the material, geometry and loading conditions, the influence of this angle on the plastic collapse load and crack driving force must be studied too. A typical T-joint with the same dimensions as the specimen T2 with $b_0=350$ mm, $b_1=200$ mm, $t_0=15$ mm, $t_1=16$ mm, $t_w=12$ mm, $l_{c1}=77$ mm, $l_{c2}=187$ mm, and $a/t_0=0.9$ is analyzed in this study. Fig. 7.64 shows the load-displacement curves for different crack inclined angles. It can be found that the load-displacement curves are almost identical. Therefore, the plastic collapse load is not affected by the crack inclined angle. It is only related to the crack depth perpendicular to the chord face and the crack length, l_{c1} and l_{c2} .

Figs. 7.65 and 7.66 illustrate the elastic and elastic-plastic J -integrals, J_e and J_{ep} , respectively. It is clear that the J -integrals are strongly sensitive to the crack inclined angle. Based on this J -integral value, the FADs of this T-joint are constructed using different crack inclined angles as shown in Fig. 7.67. It can be found that the assessment curves vary with the change of the crack inclined angle. When θ is equal to 60° , the standard BS7910 (1999) Level 3A assessment curve is inside the numerical curve, and hence the standard FAD gives a safe assessment. But when θ is equal to 45° and 30° , the shoulder part of the standard assessment curve is outside the numerical curve. It means that the standard FAD is unsafe when the numerical collapse load is used as the plastic collapse load to calculate the L_r or S_r . Hence, a conservative plastic collapse load is needed to give a safe assessment of the cracked SHS T-joint when using the standard BS7910 (1999) Level 3A FAD.

7.7.6 Influence of Residual Stresses

In standard procedures for fracture assessment residual stresses are taken as part of the driving force, and in many cases the contribution is significant. Allowing for residual stresses in the assessment of service performance varies according to the failure mechanism. It is not usually necessary to take account of residual stresses in calculations of the static strength of ductile materials (TWI, 2000). Residual stresses have a major effect on fracture, and hence the stress intensity, K , or energy release rate, J , due to residual stresses must be calculated and included in the fracture assessment.

Free (1986) studied the residual stresses near to the welded joints extensively, and experimentally measured residual stress distributions near to the weld of the tubular joint were reported. It was summarized that the transverse stresses reach a peak tensile yield at about 2 mm below the top surface, and reduce sharply from there down wards to a minimum value (close to zero) at just below the midpoint of the thickness. They then increase to about 1/3 of the yield strength at the bottom surface of the plate. In the BS7910 annex Q, general residual stress distributions were given for the practice. Because compressive residual stresses are generally beneficial for the fracture and fatigue, in the standard guidance the compressive residual stresses are neglected as zero. But in reality, the tensile stresses will be balanced by the compressive stress in the other part. Based on the expected features of the residual stress distribution in the tubular joints, Finch and Burdekin (1992) assumed the residual stress distribution at the weld toe as shown in Fig. 7.68. In this research work, the residual stress was modelled using the Finch and Burdekin's assumption. The joint having the same dimensions and crack sizes as the experimental specimen T2 was analyzed, and the J -integral at the deepest point of the cracked joint with and without residual stresses were illustrated in Fig. 7.69. It was found that the J -integral values for crack under combined residual stress and external load are higher than those for the same crack under the same external load only. Under the load of 135 kN, the difference between the J -integral values for the crack with and without residual stresses was 0.52 N/mm, but the J -integral under the residual stresses only was 0.026 N/mm. Hence, the J -integral under the

combined residual stresses and external load is not a simple superposition of the J -integral under the residual stresses and the external load respectively, the plastic correction factor, ρ , must be used to allow for interaction of external load and residual stress contributions. Therefore the assessment of the fracture parameters for the failure assessment becomes:

$$K_r = \frac{K_I}{K_{mat}} + \rho \quad (7.20)$$

where ρ is as defined in the standard codes, for example in BS7910 (1999) annex R.

7.7.7 FADs of SHS T-joints under Brace End Axial Loads

To validate the safe usage of the BS7910 (1999) standard assessment procedure for damaged SHS T-joint under brace end axial loads, the FAD specific to the particular material of BS4360-50D and the geometry is obtained by determining the J -integral using both elastic and elastic-plastic analyses. The ABAQUS (2002) finite element analysis software is used to obtain the elastic J -integral, J_e , and the elastic-plastic J -integral, J_{ep} , values. The plastic collapse load which is calculated from Equations (5.12) and (5.27) for T-joints with surface crack and through-thickness crack respectively, is adopted to give the abscissa (collapse) axis.

As discussed previously, the choice of the collapse load is a critical feature in the process of constructing the specified FAD. The assessment diagrams of Figs. 7.70 to 7.85 are plotted in pairs with the first diagram of each pair using P_u which includes the influence of the weld and the chord wall thickness as the basis for L_T , and the second diagram of each pair using the modified collapse load P'_u which gives a conservative collapse load by neglecting the influence of the weld and chord wall thickness. When the assessment curve calculated from the results of $\sqrt{J_e / J_{ep}}$ analysis falls outside the standard BS7910 (1999) assessment diagram, the use of the standard diagram for assessment of cracks would be safe. On the other hand if the calculated assessment curve falls inside the standard curve, the use of the

standard curve would be unsafe for any assessment points lying between the two curves.

It can be seen from the examination of Figs. 7.70 to 7.85 that most of the curves based on the collapse load of P_u fall inside the standard BS7910 (1999) Level 3A curve, whilst almost all the curves based on the modified collapse load P'_u for L_r fall close to or outside the standard BS7910 (1999) Level 3A curve. Therefore, when the β ratio is less than 0.8, safe assessment of cracked SHS T-joints under brace end axial loads seems can be determined from the standard BS7910 (1999) Level 3A FAD provided L_r is calculated using the conservative collapse load P'_u which neglects the influences of weld and chord wall thickness. For other numerical cases which are used to investigate the influence of the crack inclined angle, are also studied using the plastic collapse load neglecting the influence of the weld and the chord wall thickness to get the value of L_r . These FAD curves are illustrated in Fig. 7.86. It is found that all the curves fall outside the standard BS7910 (1999) Level 3A curve.

Fig. 7.1 Loading and boundary conditions

Fig. 7.2 Stress-strain curve of the BS4360-50D structural steel

Fig. 7.3 Original mesh of SHS T-joint

Fig. 7.4 Doubled mesh of SHS T-joint

Fig. 7.5 Local refined mesh of SHS T-joint

Fig. 7.6 Comparison of SIFs calculated using different mesh schemes

Fig. 7.7 Load-displacement curves calculated using different meshes

Fig. 7.8 Crack tip modes

(a) The deformation of tied crack tip

(b) The deformation of untied crack tip

Fig. 7.9 Surface crack tip deformation of the SHS T-joint

Fig. 7.10 Load-displacement curves calculated using different crack tip modes

Fig. 7.11 Through-thickness crack tip deformation in the non-linear analysis

Fig. 7.12 Through-thickness crack tip deformation in the linear elastic analysis

Fig. 7.13 Load-displacement curve of specimen T1

Fig. 7.14 Load-displacement curve of specimen T2

Fig. 7.15 Load-displacement curve of specimen T3

Fig. 7.16 CMOD-Load curves of specimen T1

Fig. 7.17 CMOD-Load curves of specimen T2

Fig. 7.18 CMOD-Load curves of specimen T3

Fig. 7.19 The through-thickness crack

Fig. 7.20 Virtual crack extension

Fig. 7.21 Realization of a 2-D virtual crack extension in a finite element mesh (Bakker, 1983)

(a) Perturbation to all crack front nodes

(b) Perturbation to one crack front node

Fig. 7.22 Crack front perturbation by virtual crack extension

Fig. 7.23 Construction of failure assessment diagram (FAD)

Fig. 7.24 Material model and structural response of elastic-perfectly plastic material

Fig. 7.25 Material model and structural response of elastic-plastic material

Fig. 7.26 Twice elastic compliance criterion

Fig. 7.27 Stress distribution around the intersection between the chord and brace for surface crack, $P/P_u=0.2$

Fig. 7.28 Stress distribution around the intersection between the chord and brace for surface crack, $P/P_u=1.0$

Fig. 7.29 Stress distribution of the chord face and the yield line mode for uncracked SHS T-joint under the plastic collapse load

Fig. 7.30 Stress distribution of the chord face for SHS T-joint with surface crack under the plastic collapse load

Fig. 7.31 Stress distribution around the intersection between the chord and brace for through-thickness crack, $P/P_u=0.2$

Fig. 7.32 Stress distribution around the intersection between the chord and brace for through-thickness crack, $P/P_u=1.0$

Fig. 7.33 Stress distribution of the chord face for uncracked SHS T-joint under the plastic collapse load

Fig. 7.34 Stress distribution of the chord face for SHS T-joint with through-thickness crack under the plastic collapse load

Fig. 7.35 Load-displacement curves for series ST1

Fig. 7.36 Load-displacement curves for series ST2

Fig. 7.37 Load-displacement curves for series ST3

Fig. 7.38 Load-displacement curves for series ST4

Fig. 7.39 Load-displacement curves for series TT1

Fig. 7.40 Load-displacement curves for series TT2

Fig. 7.41 Load-displacement curves for series TT3

Fig. 7.42 Load-displacement curves for series TT4

Fig. 7.43 The Mode-I stress intensity factor (K_I) distribution along the surface crack front for shallow crack

Fig. 7.44 The Mode-I stress intensity factor (K_I) distribution along the surface crack front for deep crack

(a) The surface crack opening displacements

(b) Crack tip1

(c) Crack tip2

Fig. 7.45 The stress distribution around the opened surface crack
(under brace end axial loads)

Fig. 7.46 Crack tip1 stress intensity factor (K_I) distributions through the chord wall (under brace end axial loads)

Fig. 7.47 Crack tip2 stress intensity factor (K_I) distributions through the chord wall (under brace end axial loads)

Fig. 7.48 The stress distribution at the crack tip of through-thickness crack without contact crack surface definition (under brace end axial loads)

Fig. 7.49 The stress distribution at the crack tip of through-thickness crack with contact crack surface definition (under brace end axial loads)

Fig. 7.50 Crack tip1 stress intensity factor (K_{II}) distributions through the chord wall thickness (under brace end axial loads)

Fig. 7.51 Crack tip2 stress intensity factor (K_{II}) distributions through the chord wall thickness (under brace end axial loads)

Fig. 7.52 Crack tip1 stress intensity factor (K_{III}) distributions through the chord wall thickness (under brace end axial loads)

Fig. 7.53 Crack tip2 stress intensity factor (K_{III}) distributions through the chord wall thickness (under brace end axial loads)

Fig. 7.54 Crack tip1 J -integral value distributions through the chord wall thickness

Fig. 7.55 Crack tip2 J -integral value distributions through the chord wall thickness

Fig. 7.56 Load-displacement curves for a typical through-thickness cracked T-joint

Fig. 7.57 The SIF results along the surface crack front under brace end axial loading for a shallow crack with $a/t_0=0.4$

Fig. 7.58 The SIF results along the surface crack front under brace end axial loading for a deep crack with $a/t_0=0.85$

Fig. 7.59 Stress components and cylinder co-ordinates at the crack front

Fig. 7.60 The 3-D surface crack propagation direction

Fig. 7.61 An actual fracture crack direction compared with the numerical prediction

Fig. 7.62 The crack tip1 SIF results across the chord wall thickness for through-thickness crack under brace end axial loading

Fig. 7.63 The crack tip2 SIF results across the chord wall thickness for through-thickness crack under brace end axial loading

Fig. 7.64 Load-displacement curve of a typical T-joint under different crack inclined angles

Fig. 7.65 Elastic J -integral values of a typical T-joint under different crack inclined angles

Fig. 7.66 Elastic-plastic J -integral values of a typical T-joint under different crack inclined angles

Fig. 7.67 The failure assessment curves constructed using different crack inclined angles compared with the standard BS7910 (1999) Level 3A assessment curve

Fig. 7.68 The assumed residual stress distribution in the numerical analyses

Fig. 7.69 The J -integrals of the specimen T2 with and without residual stresses

Fig. 7.70 The failure assessment curves for series ST1 using P_u as the plastic collapse load

Fig. 7.71 The failure assessment curves for series ST1 using P'_u as the plastic collapse load

Fig. 7.72 The failure assessment curves for series ST2 using P_u as the plastic collapse load

Fig. 7.73 The failure assessment curves for series ST2 using P'_u as the plastic collapse load

Fig. 7.74 The failure assessment curves for series ST3 using P_u as the plastic collapse load

Fig. 7.75 The failure assessment curves for series ST3 using P'_u as the plastic collapse load

Fig. 7.76 The failure assessment curves for series ST4 using P_u as the plastic collapse load

Fig. 7.76 The failure assessment curves for series ST4 using P'_u as the plastic collapse load

Fig. 7.78 The failure assessment curves for series TT1 using P_u as the plastic collapse load

Fig. 7.79 The failure assessment curves for series TT1 using P'_u as the plastic collapse load

Fig. 7.80 The failure assessment curves for series TT2 using P_u as the plastic collapse load

Fig. 7.81 The failure assessment curves for series TT2 using P'_u as the plastic collapse load

Fig. 7.82 The failure assessment curves for series TT3 using P_u as the plastic collapse load

Fig. 7.83 The failure assessment curves for series TT3 using P'_u as the plastic collapse load

Fig. 7.84 The failure assessment curves for series TT4 using P_u as the plastic collapse load

Fig. 7.85 The failure assessment curves for series TT4 using P'_u as the plastic collapse load

Fig. 7.86 The failure assessment curves constructed using different crack inclined angles using P'_u as the plastic collapse load

Table 7.1 Elastic-plastic J -integral under different types of crack tip for $\beta = 0.57$
(surface crack, $a/T_c=0.66$, $l_{c1}=40$ mm, $l_{c2}=110$ mm)

Crack Tip type \ Load (KN)	265	460	720	1000
Tied	6.785	32.79	135.9	631.7
Untied	6.784	33.38	142.8	691.3

Table 7.2 Elastic J -integral results for $a/T_c=0.66$ and $P=920$ kN

Contour No.	First ring	Second ring	Third ring
T10053	29.47	29.53	29.54
T10055	30.20	30.27	30.29
T10057	30.12	30.19	30.20
T10059	31.16	31.23	31.24
T10061	31.85	31.93	31.94

Table 7.3 Elastic-plastic J -integral results for $a/T_c=0.66$ and $P=575$ kN

Contour No.	First ring	Second ring	Third ring
T10053	106.3	62.48	60.53
T10055	105.1	62.14	59.76
T10057	122.6	76.52	75.92
T10059	108.9	64.17	61.03
T10061	108.2	64.09	60.76

Table 7.4 Elastic-plastic J -integral results for $a/T_c=0.66$ and $P=920$ kN

Contour No.	First ring	Second ring	Third ring
T10053	738.5	495.0	461.7
T10055	743.6	488.0	446.0
T10057	707.9	560.3	509.4
T10059	684.6	480.6	441.7
T10061	691.9	482.3	440.0

Table 7.5 The T-joints dimensions of the cases investigated in this study

Series	b_0 (mm)	b_1 (mm)	t_0 (mm)	t_1 (mm)	t_w (mm)	Type of crack
ST1	350	150	16	16	8	surface
ST2	350	200	15	16	8	surface
ST3	350	200	15	16	12	surface
ST4	350	250	16	16	8	surface
TT1	350	150	16	16	8	Through-thickness
TT2	350	200	15	16	8	Through-thickness
TT3	350	200	15	16	12	Through-thickness
TT4	350	250	16	16	8	Through-thickness

Table 7.6 The surface crack details of SHS T-joints

Series	No.	No. of cracks	a/t_0	l_{c1} (mm)	l_{c2} (mm)	θ
ST1	1	1	0.4	33	83	30^0
	2	1	0.6	43	108	45^0
	3	1	0.8	63	158	45^0
ST2	1	1	0.4	38	108	30^0
	2	1	0.6	58	128	45^0
	3	1	0.8	68	178	45^0
ST3	1	1	0.4	42	102	30^0
	2	1	0.6	62	132	45^0
	3	1	0.8	72	182	45^0
	4	1	0.85	67	217	45^0
		2	0.6	47	92	45^0
ST4	1	1	0.4	63	203	30^0
	2	1	0.6	73	223	45^0
	3	1	0.8	93	243	45^0

Table 7.7 The through-thickness crack details of SHS T-joints

Series	No.	No. of cracks	l_{c1} (mm)	l_{c2} (mm)	θ
TT1	1	1	33	83	30^0
	2	1	43	108	45^0
	3	1	63	158	45^0
TT2	1	1	48	108	30^0
	2	1	58	128	45^0
	3	1	68	178	45^0
TT3	1	1	52	112	30^0
	2	1	62	132	45^0
	3	1	72	182	45^0
TT4	1	1	63	203	30^0
	2	1	73	223	45^0
	3	1	93	243	45^0

Table 7.8 The plastic collapse load of SHS T-joints with surface crack compared with the ultimate strength of uncracked joints

Series	No.	Numerical (kN)	P_u (kN)	Difference	P'_u (kN)	Difference
ST1	Un.	793.0	785.3	1.0%	660.7	16.7%
	1	784.8	753.6	4.0%	646.1	17.6%
	2	762.8	738.3	3.2%	634.8	16.8%
	3	732.3	713.4	2.5%	615.9	15.9%
ST2	Un.	963.9	955.1	0.9%	750.4	22.1%
	1	960.4	921.0	5.5%	728.6	25.3%
	2	942.6	899.4	4.5%	713.8	24.2%
	3	897.4	869.5	3.1%	693.2	22.8%
ST3	Un.	1045.0	1023.4	2.0%	750.4	28.2%
	1	1040.3	987.8	9.4%	730.4	32.9%
	2	1030.5	961.3	8.9%	713.8	32.0%
	3	1005.4	928.7	7.6%	693.1	31.1%
	4	925.5	866.25	6.4%	659.2	28.8%
ST4	Un.	1990.1	1957.5	1.6%	1214.4	38.9%
	1	1947.1	1820.7	6.5%	1144.5	41.2%
	2	1785.6	1759.1	1.5%	1112.3	37.7%
	3	1675.5	1705.5	1.8%	1083.4	35.3%
<p>Note: P_u includes the influence of weld and chord wall thickness, $\beta = \frac{b_1 + 2t_w}{b_0 - 2t_0}$</p> <p>$P'_u$ does not include the influence of weld and chord wall thickness, $\beta = \frac{b_1}{b_0}$</p>						

Table 7.9 The plastic collapse load of SHS T-joints with through-thickness crack

Series	No.	Numerical (kN)	P_u (kN)	Difference	P'_u (kN)	Difference
TT1	Un.	793.0	785.3	0.97%	660.7	16.7%
	1	759.1	756.8	0.3%	632.6	16.7%
	2	741.8	738.6	0.4%	619.4	16.5%
	3	675.8	687.2	1.7%	572.1	15.3%
TT2	Un.	963.9	955.1	0.9%	750.4	22.1%
	1	899.2	883.9	1.7%	683.3	24.0%
	2	880.0	865.1	1.7%	665.4	24.4%
	3	822.8	821.7	0.03%	622.5	24.3%
TT3	Un.	1045	1023.4	2.0%	750.4	28.2%
	1	958.6	937.8	2.2%	671.2	30.0%
	2	938.4	917.5	2.2%	649.7	30.8%
	3	875.3	870.5	0.55%	603.5	31.1%
TT4	Un.	1990.1	1957.5	1.6%	1214.2	38.9%
	1	1526.2	1559.9	2.2%	845.3	44.6%
	2	1453.4	1518.7	4.5%	804.6	44.6%
	3	1379.8	1473.7	6.8%	765.7	44.5%
TT5	Un.	1048.4	987.9	5.8%	750.4	28.4%
	1	820.5	811.0	1.2%	662.0	19.3%
<p>Note: P_u includes the influence of weld and chord wall thickness, $\beta = \frac{b_1 + 2t_w}{b_0 - 2t_0}$</p> <p>$P'_u$ does not include the influence of weld and chord wall thickness, $\beta = \frac{b_1}{b_0}$</p>						

CHAPTER 8

CONCLUSIONS AND RECOMMENDATIONS

8.1 Conclusions

Flaws such as fatigue cracks, welding defects and corrosion damage in welded structures can arise during the fabrication process or in service. For certain critical parts, the failure of a single component due to the presence of a flaw can be quite catastrophic as well as having severe economic and environmental consequences. Other flaws may be harmless, as they will not lead to failure during the lifetime of the component, and hence repair of such flaws or replacement of respective component is economically wasteful. Therefore, it is necessary to establish acceptance levels for flaws present in welded structures. Fitness for purpose is a procedure of which is consciously chosen to be the right level of material and fabrication quality for each application, having regard to the risks and consequences of failure (Wells, 1981). In practice, non-validated design and uninformed maintenance can result in the premature failure of these components, and there is a need to replace them at an earlier period in time than anticipated. Fitness for service assessments are quantitative engineering evaluations which are performed to demonstrate the structural integrity of a component containing a flaw discovered during pre-service or in-service inspections.

Based on the concepts of fitness for purpose and fitness for service, several standard procedures are published in the literature, such as BS7910 (1999), API RP579 (2000), and R6 (2001). The standard failure assessment diagrams (FADs) are recommended in these codes. In order to validate the use of the standard BS7910 (1999) Level 3A FAD for the integrity assessment of the square hollow section (SHS) T-joints under brace end axial loads, experimental and numerical analyses are performed in this study. Several conclusions can be summarised as follows:

1. In accordance with the stress distribution around the intersection between the brace and chord of rectangular and square hollow section (RHS and SHS) joints, the hot spot stress is always located at one of the corners of the brace. Therefore, it can be expected that the fatigue crack will initiate from this corner, and the crack will initiate and propagate in the chord wall under the weld toe with different loading conditions.
2. A general 3-D RHS joint model with and without cracks has been developed successfully. The weld model satisfying the American Welding Society specifications (AWS, 2000) is constructed in the model. Based on some reasonable simplifications, the geometry of the cracked surface obtained from the fatigue experimental result is used as a benchmark. To complete the crack model, the crack front locus is defined using a simple mapping procedure. For a surface crack, a two-halves semi-elliptical curve is mapped onto the 3-D cracked surface, and for through-thickness crack, a rectangular crack is used to map onto the 3-D cracked surface.
3. Based on accurate geometrical model created, a special-purpose automatic finite element mesh generator is developed to generate the T-joint meshes for analysis. In the proposed method, the sub-zone technique is used to adjust the mesh density in different regions of the T-joint. Because of the steep stress gradient at the stress concentration region, the mesh around the crack front is refined using several mesh refinement steps so as to capture the stress distribution accurately and give an accurate crack tip driving force. Compared with the mesh of uncracked joint, only the crack zone need to be remeshed. Hence, this method is very flexible and effective. It can be used to generate the mesh of cracks with any length, position, quantity, shape, and type for surface crack or through-thickness crack. The proposed FE mesh generator is extended easily for cracked T, Y, K and KK-joints.
4. In accordance with the failure mode of the SHS T-joint with a medium β ratio, the yield line theory is proposed to investigate the plastic collapse load of this cracked SHS T-joint under brace end axial loads. The formulae for T-joint

containing surface crack and through-thickness crack are derived and validated numerically. It is found that the plastic collapse loads calculated from the assumed yield line pattern is acceptable, and a good agreement is obtained compared with the numerical results.

5. A series of 3 static strength tests were performed on full-scale cracked SHS welded T-joints under brace end axial loads. The crack propagation is monitored by an alternating current potential drop (ACPD) technique, and the crack mouth opening is recorded by a set of LVDTs. In these tests, it is found that the joint failure is due to brittle fracture at the deepest point of the surface crack although the material is on the upper shelf of the transition curve. In accordance with the ACPD readings and observation of the crack surface, a small amount of ductile tearing is observed, but the ductile tearing region is quite small.
6. In accordance with the experimental data, the numerical model of SHS T-joint is validated accordingly. Compared with the experimental results, it is found that the numerical model gives a good prediction of the plastic collapse load and the crack mouth opening displacement.
7. In accordance with the 3-D non-linear analyses of cracked SHS T-joints, it is found that the most crucial factor to influence the convergence of the analyses is the aspect ratio of the elements around the crack front. It is suggested that the length of the elements is not more than 3 times of its radius. Because the radius of the element around the crack front is very small, the length of element along the crack front must be small too. Therefore, the local refinement of the element along the crack is necessary to get the correct results.
8. In accordance with the investigation of the contact crack surface, it is found that no contact is observed for the surface crack, but it is found that there is contact for the through-thickness crack at the bottom of the crack tip. Hence, the contact crack surface should be considered in the modelling of SHS T-joint containing through-thickness crack.

9. In the elastic fracture mechanics analyses, the mixed mode fracture is studied to investigate the influence on the assessment of the cracked T-joints. For surface crack, the Mode-II and Mode-III stress intensity factors (SIFs) are much smaller than the Mode-I SIFs. Therefore, the effective SIF value is very close to the Mode-I SIF value. However, the Mode-II and Mode-III fracture will affect the crack propagation direction which is observed in the tests. For through-thickness crack, the Mode-II and Mode-III SIFs can not be neglected compared with the Mode-I SIFs. Therefore, effective SIF which considers the mixed mode influence, should be used in the assessment of the cracked T-joints.
10. Three categories of SHS T-joints having β ratios of 0.43, 0.57, and 0.71 are investigated numerically to obtain the load-displacement curves. Based on the twice elastic compliance criterion, the plastic collapse load is calculated and compared with the values calculated from the proposed yield line equations. It is found that the yield line equations can give a good prediction of the plastic collapse load. If the influence of the weld and chord wall thickness is neglected, a conservative plastic collapse load is produced using the yield line equations.
11. The failure assessment diagrams (FADs) of 4 series SHS T-joints under brace end axial loads are constructed using the J -integral approach. It is found that the shoulder part in most of the specified FADs fall inside the standard BS7910 (1999) Level 3A FAD provided that the plastic collapse load adopts the value calculated from the yield line equations including the influence of the weld and the chord wall thicknesses. Therefore, the use of standard FAD curve is unsafe. Hence, a conservative plastic collapse load is needed here. If the conservative plastic collapse load calculated from the yield line equations neglecting the influence of the weld and chord wall thicknesses is adopted, then all the specified FADs fall outside or close to the standard BS7910 (1999) Level 3A FAD curve. Therefore, the use of the standard FAD curve is safe for the assessment of crack SHS T-joints.
12. In accordance with the fatigue test results, the fatigue crack surface is inclined and slightly curved. Because there is no benchmark available for the crack

shape, the influence of the crack inclined angle on the plastic collapse load and the J -integral is investigated for T-joints. It is found that the plastic collapse load is not sensitive to the crack inclined angle. It is only related to the crack depth and crack length along the weld toe. However, the J -integral is influenced by the crack shape. Therefore, the specified FAD curve constructed from the J -integral results is affected too.

8.2 Recommendations for Further Research Works

Based on this research, the crack tip driving force, J -integral or SIF, is sensitive to the crack shape under the weld toe. The benchmark of the crack shape for the RHS and SHS joints need to be established from the experimental data. Therefore, the 3-D fatigue crack propagation of the RHS and SHS joints has to be investigated further.

Another potential area of further research is the numerical simulation of a 3-D crack propagation under the weld toe. Because the fatigue crack is slightly curved and inclined, more layers of elements are needed through the chord wall thickness to simulate the curved crack shape.

The numerical modelling and automatic mesh generation is proposed for cracked RHS Y, gaped K and KK-joints. The experimental validation of these numerical models is necessary, and the influence of the crack on the static strength of the joints should be studied extensively. The usage of the standard FAD curves for the RHS and SHS joints should be validated carefully.

The failure mode of cracked SHS T-joints depends on the geometry. For high β ratio, the failure mode might be changed from the chord face yielding to the chord wall buckling. Hence, the yield line theory is not valid anymore for the study of the ultimate strength, and also the influence of the crack on the ultimate strength might be different from medium β ratio condition analyzed in the study. When the β ratio is very small, the failure mode might change to punching shear failure mode. In this case, the influence of the crack might be proportional to the cracked area located

around the chord and brace intersection region. The experimental investigation is necessary to study the ultimate behaviour and to predict the plastic collapse loads.

In this study, only brace end axial loading is investigated in the analyses and tests. The in-plane bending (IPB) and out-of-plane bending (OPB) load conditions should be included in the tests to check whether the yield line theory can still be used to predict the plastic collapse loads of cracked SHS T-joints.

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APPENDIX A

MATERIAL CERTIFICATION

379

10:

Corus Tubes
20 H.P.L. Mill
Brenth Road
Hartlepool, Cleveland, UK TS23 2EG
Tel: +44 (0)1429 266611
Fax: +44 (0)1429 234828

Customer:
CONTINENTAL STEEL PTE LTD
NO 100 GUL CIRCLE
SINGAPORE 629586

INSPECTOR'S CERTIFICATE
(Section 3.1.1b)
6410484-1

Corus Tubes Ref No: Sales: GXA132769
Customer Order No: P/O: TC/ORDER/093/01
PROJECT ORDER LOT B
Product Description: HOT FORMED RECTANGULAR HOLLOW SECTIONS
TO BS 4360:1990 GRADE 50D AND BS 4848:
PART 2:1991.

Page No 01 of 01
Work: 3567

Date: 30/07/01
Cert No: 280/0131/0097
Doc No: 100

Corus

Product Dimensions
29 250.0MM X 250.0MM X 16.00MM
11.800-11.950 344.23M

Analysis %

Chem No.	C	SI	MN	P	S	CR	MO	NI	AL	CU	N	NB	SN	TI	V	CEV
PT82152 LADLE	.15	.3481.36	.020	.013	.017	.001	.019	.028	.009	.0042	.042	.001	.002	.002	.001	.38
PT82152 CHECK	.16	.3601.36	.022	.012	.019	.003	.017	.024	.012	.0000	.039	.004	.002	.002	.003	.39
PT82255 LADLE	.15	.3661.34	.016	.013	.021	.001	.016	.036	.011	.0032	.036	.006	.002	.001	.37	
PT82255 CHECK	.15	.3621.37	.014	.011	.020	.003	.017	.032	.012	.0000	.035	.007	.002	.003	.39	
PT82260 LADLE	.15	.3961.33	.018	.010	.022	.001	.016	.037	.011	.0030	.041	.005	.002	.001	.37	
PT82260 CHECK	.16	.4061.35	.015	.012	.016	.003	.016	.042	.006	.0000	.038	.002	.002	.003	.39	

Steel Making Process
C40 C41 C42 C43 C44 C45 C46 C47 C48 C49 C50 C51 C52 C53 C54 C55 C56 C57 C58 C59 C60 C61 C62 C63 C64 C65 C66 C67 C68 C69 C70 C71 C72 C73 C74 C75 C76 C77 C78 C79 C80 C81 C82 C83 C84 C85 C86 C87 C88 C89 C90 C91 C92 C93 C94 C95 C96 C97 C98 C99 C100

Flatness Test Satisfactory

END OF TEST CERTIFICATE

Section Manager Technical
D.J. SIMMONS

Date: 20/7/02

APPENDIX B**THE CHARPY V-NOTCH TESTS RESULTS** **SETSCO SERVICES PTE LTD**

18 Teban Gardens Crescent
Singapore 608925
Tel : (65) 6566 7777
Fax: (65) 6566 7718
Website: www.setsco.com
Business Reg. No. 196900269D

Your Ref: Letter dtd 02/10/04

Our Ref: MM-1939/NAM

Date: 6 Oct 2004

TEST REPORT

(This Report is issued subject to the terms & conditions set out below)

Page 1 of 2

Subject : Testing of structural steel submitted by Nanyang Technological on 02 October 2004.

Tested For : NANYANG TECHNOLOGICAL UNIVERSITY
School of Civil & Environmental
Engineering Geotechnics Laboratory
Blk N1 #B2-26C Nanyang Ave
Singapore 639798

Attn: Mr. Edmund Lee

Date and Place of Test : 05 October 2004 at Setsco Laboratory

Method of Test : BS EN 10045-1 : 1993 - 'V'-Notch Charpy Impact Test

Description of Sample : One (01) piece of structural steel was received as follows:

S/No	Description	Qty
1	SHS 350 x 350 x 16mm thk	01

Results : 'V'-Notch Charpy Impact Test
Refer to Table attached.

NG AIK MIU
Testing Officer

CHENG KWANG MENG
Engineer (Mechanical Testing)
Mechanical Technology Division

(MTD Program)/hw/ Fax No: -

Terms & conditions:

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MM-1939/NAM

Results:

Table 1. : 'V'-Notch Charpy Impact Test

Sample Reference	Item Measured	Dimension of Test Specimen	Impact Value at 20°C (Joules)
SHS 350 x 350 x 16mm thk (L-S)	Test 1	10 x 10 x 55 mm	Unbroken at 300J
	Test 2		Unbroken at 300J
	Test 3		284
	Average		-
SHS 350 x 350 x 16mm thk (T-S)	Test 1		72
	Test 2		68
	Test 3		68
	Average		69.3
SHS 350 x 350 x 16mm thk (T-L)	Test 1		60
	Test 2		57
	Test 3		57
	Average		58.0
SHS 350 x 350 x 16mm thk (L-T)	Test 1		174
	Test 2		172
	Test 3		172
	Average		172.7

fan

Wm

APPENDIX C**THE CTOD TESTS RESULTS**

TECHNICAL REPORT		
Date of test: 11 May 2005 Report No: DNV/SL/R20050643		
<h1>TECHNICAL REPORT</h1>		
<h2>NANYANG TECHNOLOGICAL UNIVERSITY</h2> <p>(SCHOOL OF CIVIL & ENVIRONMENTAL ENGINEERING)</p>		
<h3>CTOD TEST</h3>		
SAMPLE MARK : T-L		
REPORT NO : DNV/SL/R20050643		
Name of Client: NANYANG TECHNOLOGICAL UNIVERSITY Name of Project: CTOD Test Name of Engineer: [Signature] Name of Checker: [Signature] Name of Approver: [Signature]	Name of Inspector: [Signature] Name of Tester: [Signature]	Name of Witness: [Signature]
This laboratory is accredited by the National Association of Testing Authorities (NATA), Australia and Singapore Accreditation Council-Singapore Laboratory Accreditation Scheme (SAC-SINGLAS). The results reported herein have been performed in accordance with the laboratory's terms of accreditation under NATA and SAC-SINGLAS. This report shall not be reproduced, except in full, without the written approval of the laboratory.
DET NORSKE VERITAS PTE LTD

DET NORSKE VERITAS

TECHNICAL REPORT

Date of first issue: 11 May 2005	Project No: 11286 - 019	DET NORSKE VERITAS PTE LTD
Approved by: YUAN WEN GUO	Organisational unit: 253	Strength and Material Technology Laboratory 18 Science Park Drive DNV Technology Centre Singapore 118254 Tel: 65 6779 6363 Fax: 65 6774 5725 http://www.dnv.com
Client: NANYANG TECHNOLOGICAL UNIVERSITY (School of Civil & Environmental Engineering)	Client ref.: ATTN : MR YANG ZHENGMAO	198200541Z

Summary:

Client's Description of Sample

Material : 350mm x 350mm BS 4360 Gr. 50D Hollow section

Report No: DNV/SL/R200500643	Subject Group:
Report title: CTOD Test Sample : T-L	Indexing terms Keywords CTOD Test
Work carried out by: PK Chan/YQ Yeo	Service Area 2 Market Sector Oil & Gas
Work verified by: Steven Au	☐ No distribution without permission from the client or responsible organisational unit (however, free distribution for internal use within DNV after 3 years) ☒ No distribution without permission from the client or responsible organisational unit. ☐ Strictly confidential ☐ Unrestricted distribution
Date of this revision:	Revision No:
	Number of pages: 6

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DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT

Table of Contents

CTOD TEST1

PHOTOGRAPHS 2 - 3

GRAPHS 4 - 6

Page

CTOD TEST RESULTS

Client: *Shanghai East Asia International Engineering, Nanyang Technological University*

Client's sample description: *Structure steel - P501601-5013 (below section)*

Test standard: *BS 7448, Part 1 and client's instructions*

PRECRACKING

Yield stress (WM) : <i>None</i>	K _{IC} : <i>0.1</i>
Tensile stress (WM) : <i>None</i>	Sample identification : <i>B x 25, T4</i>
Yield stress (BM) : <i>340</i>	Crack orientation : <i>Strength-Reduction notch</i>
Tensile stress (BM) : <i>320</i>	Loading span : <i>112 mm</i>
Yield stress (BAZ) : <i>None</i>	σ _W : <i>0.25</i>
Tensile stress (BAZ) : <i>None</i>	σ _W : <i>0.40</i>
RT (BM) : <i>953</i>	Test temperature : <i>23.0 °C</i>
RT (WM) : <i>0</i>	Date of test : <i>27-29/04/2005</i>
RT (BM) : <i>0</i>	Tested by : <i>Chen PC/Yuan WP</i>
RT (WM) : <i>0</i>	Witnessed by :

TESTING TO FRACTURE

Nominal thickness (B) : <i>14.0 mm</i>	Test temperature : <i>23.0 °C</i>
Nominal width (W) : <i>28.0 mm</i>	Loading rate : <i>0.25 mm/min</i>
Loading span : <i>112 mm</i>	Date of test : <i>05/04/2005</i>
σ : <i>1.0 mm</i>	Tested by : <i>Chen PC/Yuan WP</i>
RT (BM) : <i>0</i>	Witnessed by : <i>Hu Yang Zhi (NTU CHN)</i>
RT (WM) : <i>0</i>	

SAMPLE	SPECIMEN'S DIMENSIONS (mm)												Yield stress	Load F	Disp. Yp	CTOD Value, δ	Remarks
	W	B	a1	a2	a3	a4	a5	a6	a7	a8	a9	a10					
T-1	28.0	14.0	14.11	14.12	14.16	14.17	14.45	14.28	14.37	14.21	14.18	370	11.83	0.215	0.05	2c +	
T-2	28.0	14.0	14.18	14.28	14.35	14.45	14.50	14.44	14.41	14.37	14.25	370	11.65	0.100	0.04	2c +	
T-3	28.0	14.0	14.12	14.36	14.30	14.31	14.73	14.85	14.52	14.29	14.44	370	11.20	0.121	0.05	2c +	

Note : * Yield stress corrected to CTOD test temperature

Young's Modulus of Elasticity = 207800 N/mm² and Poisson's ratio = 0.3

Significant pre-crack was observed prior to the attainment of a maximum force plateau

CTODR (1/100)

- 0.00 -

Page i

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DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT**CTOD TEST RESULTS**

Client : School of Civil and Environmental Engineering, Nanyang Technological University
 Attention : Mr Yang Zhengmao
 Client reference : CEE 1/05/Q011
 Client's sample description : Structure steel : BS4360-50D hollow section
 Test standard : BS7448 : Part 1 and client's instructions

PRECRACKING

Yield stress (WM)	: -	N/mm ²	R	:	0.1
Tensile stress (WM)	: -	N/mm ²	Sample size/orientation	:	B x 2B, T-L
Yield stress (BM)	: 380	N/mm ²	Crack orientation	:	through-thickness notch
Tensile stress (BM)	: 529	N/mm ²	Loading span	:	112 mm
Yield stress (HAZ)	: -	N/mm ²	a/W	:	0.50
Tensile stress (HAZ)	: -	N/mm ²	M/W	:	0.40
Kf (BM)	: 863	Nmm ^{3/2}	Test temperature	:	23.0 °C
Kf (WM)	: 0	Nmm ^{3/2}	Date of test	:	27-29/04/2005
Ff (BM)	: 6	kN	Tested by	:	Chan PK/Sect HP
Ff (WM)	: -	kN	Witnessed by	:	-

TESTING TO FRACTURE

Nominal thickness (B)	:	14.0 mm	Test temperature	:	23.0 °C
Nominal width (W)	:	28.0 mm	Loading rate	:	0.28 mm/min
Loading span	:	112 mm	Date of test	:	03/05/2005
z	:	2.0 mm	Tested by	:	Chan PK/Yuan WG
Kf (BM)	:	1760 Nmm ^{3/2}	Witnessed by	:	Mr Yang ZM (NTU CEE)
Kf (WM)	:	- Nmm ^{3/2}			

SAMPLE MARK	SPECIMEN DIMENSIONS (mm)											*Yield stress (N/mm ²)	Load F (kN)	Disp. Vp (mm)	CTOD value, δ (mm)	Remark
	W	B	a1	a2	a3	a4	a5	a6	a7	a8	a9					
T-L-1	28.0	14.0	14.11	14.15	14.34	14.37	14.45	14.38	14.37	14.21	14.15	379	11.83	0.233	0.08	δ c +
T-L-2	28.0	14.0	14.19	14.28	14.35	14.48	14.50	14.44	14.41	14.37	14.25	379	11.42	0.100	0.04	δ c +
T-L-3	28.0	14.0	14.12	14.36	14.70	14.81	14.73	14.65	14.62	14.29	14.44	379	11.23	0.121	0.05	δ c +

Note : * Yield stress corrected to CTOD test temperature.
 - Young's Modulus of Elasticity = 207000 N/mm² and Poisson's ratio = 0.3.
 - Significant pop-in was observed prior to the attainment of a maximum force plateau.

CTODN (11/95)

DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT

P1 : Fracture surface of sample T-L 1

P1 : Fracture surface of sample T-L 1

P2 : Fracture surface of sample T-L 2

Page 2

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DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT

Operator's Name: Yuan/Chan/Lee
 Test Date: Tuesday, May 03, 2005
 Specimen ID: T-L 1
 Job Number: 11286 - 019
 Material: BS 4360 Gr.50D Hollow section
 Test Param File: T-L 1

Load N Versus Extension mm

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Page 4

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DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT

Operator's Name: Yuan/Chan/Lee
 Test Date: Tuesday, May 03, 2005
 Specimen ID: T-L 2
 Job Number: 11286 - 019
 Material: BS 4360 Gr.50D Hollow section
 Test Param File: T-L 2

Load N Versus Extension mm

DET NORSKE VERITAS

Report No: SL/R20050643

TECHNICAL REPORT

Operator's Name: Yuan/Chan/Lee
 Test Date: Tuesday, May 03, 2005
 Specimen ID: T-L 3
 Job Number: 11286 - 019
 Material: BS 4360 Gr.50D Hollow section
 Test Param File: T-L 3

Load N Versus Extension mm

Page 6

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APPENDIX D**THE ULTRASONIC INSPECTION TEST REPORT**
☐ CLIENT'S COPY
☐ SSFS COPY

SAN SOON FATT ENGINEERING PTE LTD
 14 Tuas View Loop Singapore 637680
 Tel : (65) 6 861 1610 Fax : (65) 6 861 1612 BackUp Fax Only: (65) 6 452 2373
 Email : sandmich@pacific.net.sg

GST Regn No M2 0036117-4

RECEIPT MEMO

Page: 1/1

TO :	NANYANG TECHNOLOGICAL UNIVERSITY	DATE:	23rd July 2003
DELIVER:	Heavy Structure Laboratory		
	Blk N1 #1A-29, 50 Nanyang Ave	HP: 9 4347558	
ATTN:	Ms Ji Hong Li	TEL: 6 790 5339	YR REF: CEE/ST/QR18121/02
ORDER BY:	Ms Woo M S	TEL: 6 7905257	OUR REF: do-293.2-02-03
		FAX NO:	

File A7-ssf RECEIPT MEMO

Dear Sir,

Kindly received the belowmentioned items :

RE : T - JOINT - COMPLETE INSTALLATION

PROJECT:

S/N	Qty	Description		
1	3 sets	COMPLETE INSTALLATION OF T - JOINT Structures c/w Ultrasonic Test, Painting		
2	2 sets	Dismantle of 2 sets of Tested Specimen		

We acknowledge acceptance of the above:

Best Regards,
 San Soon Fatt Engineering Pte Ltd.,
 Michelle W Y

Ji Hongli
 (Kindly sign, stamp & confirm the above)
 Name: Ji Hongli Date: 04/08/2003
 Designation:
 I/C No:

NANYANG TECHNOLOGICAL UNIVERSITY
 School of Civil & Environmental Engineering

Millennium Technical & Trading Services Pte Ltd Block 15 Pioneer Road North #01-81 Singapore 628464. Telephone: 65-62655655 Facsimile: 65-62656656 Email : mttsp@singnet.com.sg		ACCREDITED LABORATORY SAC - SINGLAS Certificate No. LA-2001-0237-Q	
ULTRASONIC INSPECTION TEST REPORT			
REPORT NO : MUT/A11267/0703		CLIENT : SAM SOON FATT ENGINEERING PTE LTD	
INSPECTION DATE : 25/07/03		14 SUNGEI KADUT STREET 3	
PAGE 1 OF 1		SINGAPORE 729145	
		PROJECT : SCHOOL OF CIVIL & ENVIRONMENTAL ENGINEERING	
ACCEPTANCE CRITERIA : AWS D1.1 2002 EDITION		PROBE DETAILS:	
PROCEDURE NO. : MTT-UT2 REV.1		0°-2mHz-10mmø	
TECHNIQUE NO. : UT-5		60°-2mHz-8X9mmø	
Nature of Test: ULTRASONIC FLAW DETECTION		70°-2mHz-8X9mmø	
Method of Testing : PULSE 'A' SCAN			
Scanning Technique: ORBITAL, TRANSVERSE, LONGTUDINAL			
Ultrasonic Unit : KRAUTKRAMER USK 7			
Serial No. : 27274-944			
Calibration Block: IIW VI BLOCK			
Material : CARBON STEEL		Weld Preparation: SINGLE BEVEL	
Material Thickness: 12mm		Welding Process: SMAW	
Backing: NIL		Heat Treatment: -	
Surface Condition: SATISFACTORY		Couplant Used: STARCH	
Calibration:			
0° : SET TO 80% FULL SCREEN HEIGHT ON 2 ND BACK WALL ECHO.			
ANGLE : SET TO 80% FULL SCREEN HEIGHT ON 1.5mm SIDE DRILLED HOLE OF IIW BLOCK FOR REFERENCE dB.			
	PROBE	RANGE (mm)	REF (dB)
	0°	100	36
	70°	100	52
	60°	100	44
RESULTS			
JOB LOCATION : NTU			
PROJECT : SCHOOL OF CIVIL & ENVIRONMENTAL ENGINEERING			
ITEM INSPECTED : SQUARE HOLLOW SECTION OF BRACE TO CHORD.			
1) JOINT 1			
2) JOINT 2			
3) JOINT 3			
AREA INSPECTED : UT WERE CARRIED OUT ON FULL PENETRATION T-JOINTS OF BRACE TO CHORD WELDMENT.			
(REFER TO ATTACHED PHOTO)			
PURPOSE OF INSPECTION: TO DETECT ANY INDICATION PRESENT ON THE FULL PENETRATION WELD.			
FINDING & RESULTS : JOINT 1 & JOINT 3 - NO REJECTABLE INDICATION WERE DETECTED ON THE JOINT TESTED/ACCEPTED IN ACCORDANCE WITH THE REFERENCING CODE SECTION.			
: JOINT 2 - CLASS 'B' DEFECT DETECTED AT THE DEPTH OF 10mm WITH THE LENGTH OF 40mm. RECTIFICATION CARRIED OUT IMMEDIATELY WITH GRINDING AND WELDING. RE-UT ON THE REPAIRED AREA AND FOUND NO REJECTABLE INDICATION/ACCEPTED IN ACCORDANCE WITH THE REFERENCING CODE SECTION.			
TECHNICIANS: AHMSAM		APPROVED BY :	
SNT LEVEL : II		SNT LEVEL:	
DATE: 25/07/03		DATE: 25/07/03	

PROJECT : SCHOOL OF CIVIL & ENVIRONMENTAL ENGINEERING.

JOINT NO : 3 UT - OK

JOINT NO: 2

JOINT NO : 2 UT - OK After repair