New insights into the competition between ductile tearing and plastic collapse in 304(L) stainless steel components

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New insights into the competition between ductile tearing and plastic collapse
## CONTENT

<table>
<thead>
<tr>
<th>CONTENT</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>List of Figures</td>
<td>3</td>
</tr>
<tr>
<td>List of Tables</td>
<td>9</td>
</tr>
<tr>
<td>COPYRIGHT STATEMENT</td>
<td>18</td>
</tr>
<tr>
<td>Abstract</td>
<td>20</td>
</tr>
<tr>
<td>Declaration</td>
<td>22</td>
</tr>
<tr>
<td>Acknowledgment</td>
<td>23</td>
</tr>
<tr>
<td>Problem definition</td>
<td>24</td>
</tr>
<tr>
<td>Objective</td>
<td>25</td>
</tr>
</tbody>
</table>

### CHAPTER 1 LITERATURE REVIEW .......................................................... 26

1.1. INTRODUCTION TO FRACTURE MECHANICS ................................................ 26

#### 1.1.1. Griffith Theory ............................................................................ 26

#### 1.1.2. Stress intensity factor ............................................................... 28

#### 1.1.3. J-integral ..................................................................................... 29

#### 1.1.4. HRR singularity & the path independent contour integral: ............. 30

1.3. FRACTURE TOUGHNESS ............................................................................. 33

#### 1.3.1. Fracture toughness test standard .................................................. 33

#### 1.3.2. Constraint effect: ......................................................................... 33

1.3.2.1. General introduction ...................................................................... 33

1.3.2.2. T stress .......................................................................................... 34

1.3.2.3. J-Q Two parameter fracture mechanics ............................................ 36

1.3.2.4. Effect of constraint on J-Resistance curves ................................... 36

1.3.2.5. Constraint effect on initiation toughness ......................................... 38

1.3.2.5.i. Brittle fracture: ........................................................................... 38

1.3.2.5.ii. Ductile tearing: .......................................................................... 38

1.3.2.6. Limitations: ..................................................................................... 39

1.4. PLASTICITY IN METALS ........................................................................... 41

#### 1.4.1. Yield Criteria ................................................................................. 41

1.4.1.1. Tresca Yield criterion .................................................................... 41

1.4.1.2. Von Mises Yield criterion .............................................................. 42

1.4.1.3. Plastic deformation ahead of a sharp defect .................................... 43

#### 1.4.2. Plastic collapse: .............................................................................. 45

1.4.2.1. Introduction: .................................................................................... 45

1.4.2.2. Material definition .......................................................................... 45

1.4.2.3. Upper Bound and Lower Bound Limit Theorem: ............................... 46

1.4.2.4. Local and Global Plastic collapse .................................................... 47

1.4.2.5. Engineering derivation of Limit Load .............................................. 48
New insights into the competition between ductile tearing and plastic collapse

1.4.2.5.i. Handbook solution: ................................................................. 48
1.4.2.5.ii. Graphical derivation: ................................................................. 49
1.4.2.6. Finite Element Analysis: ................................................................. 50
1.4.3. Experimental Measurement of Plasticity using Digital Image Correlation ........................ 51
1.5. DUCTILE FRACTURE ........................................................................ 53
  1.5.1. Definition: ..................................................................................... 53
  1.5.2. Void initiation ................................................................................. 53
  1.5.3. Void growth .................................................................................. 54
  1.5.4. Void coalescence .......................................................................... 54
1.6. LOCAL APPROACH METHODOLOGY ............................................. 56
  1.6.1. Defining the facture process zone: .................................................. 57
  1.6.2. Rice and Tracey Void growth models ............................................. 57
  1.6.3. Work of Fracture ........................................................................... 58
1.7. STRUCTURAL INTEGRITY .................................................................. 60
  1.7.1. The R6 Defect assessment procedure ............................................ 60
  1.7.2. R6: Assessment of Structure containing defects .............................. 61
    1.7.2.1. Failure Assessment Diagram ..................................................... 61
      1.7.2.1.i. Option 2 FAC: Reference stress based methodology ............. 61
      1.7.2.1.ii. Option 1 FAC: Simplified FAC ............................................ 63
      1.7.2.1.iii. Option 3 FAC: Material, geometry, loading and crack size dependent FAC .... 64
  1.7.3. Analysis of Margins ...................................................................... 65
  1.7.4. Constraint modification ................................................................. 66
    1.7.4.1. Procedure I: Modification of the FAC() .................................. 67
    1.7.4.2. Procedure II: Modification of $K_c$ ......................................... 67
1.8. TEST PROGRAMMES ...................................................................... 69
  1.8.1. Transferability of fracture parameters from specimens to component level ............ 69
  1.8.2. Obtaining high constraint toughness from 304(L) stainless steel .................. 69
  1.8.3. Assessment of safety margins in Assessment procedure: ................................ 70
    1.8.3.1. Degraded Piping Program ....................................................... 71
    1.8.3.2. JAERI Program: ................................................................. 72
    1.8.3.3. R6 conservatism ................................................................... 73
1.9. KNOWLEDGE GAP ........................................................................ 75

CHAPTER 2  EXPERIMENTAL METHODOLOGY ........................................ 76

2.1. MATERIAL CHARACTERIZATION .................................................. 76
  2.1.1. Chemical composition .................................................................. 76
  2.1.2. Metallographic: ........................................................................... 77
  2.1.3. Tensile test ................................................................................ 77
2.1.4. **Hardness Testing:** ................................................................................................................. 79

2.2. **Fracture Toughness Testing** ....................................................................................................... 81

2.2.1. **Pipe Material testing** ................................................................................................................ 81

2.2.2. **Compact Tension Specimen** ..................................................................................................... 87

2.2.3. **Validity limits** ............................................................................................................................. 91

2.3. **Digital Image Correlation** ............................................................................................................. 92

2.3.1. **Surface preparation:** .................................................................................................................. 92

2.3.1.1. Digital speckle correlation (speckle size: 100μm-400μm): ......................................................... 92

2.3.1.2. Micro-structural features correlation (20μm to 100μm): ......................................................... 93

2.3.2. **Strain analysis methodology** .................................................................................................... 93

**CHAPTER 3 FINITE ELEMENT ANALYSIS METHODOLOGY** ........................................................................... 96

3.1. **H Factor Calculation** .................................................................................................................... 96

3.1.1. **2D shallow and deep cracked bend specimen** .......................................................................... 96

3.1.2. **2D deep cracked Tension specimen** ......................................................................................... 97

3.1.3. **Validation of the models:** ......................................................................................................... 98

3.1.3.1. Shallow and deep cracked SEN(B) ......................................................................................... 98

3.1.3.2. Deep cracked SEN(T) ................................................................................................................. 99

3.2. **3D Single Edge Cracked Specimen** ............................................................................................. 100

3.2.1. **3D Shallow and Deep Cracked Bend Specimen** ................................................................... 100

3.2.2. **3D Deep cracked tension specimens:** ....................................................................................... 102

3.2.3. **Validation of the models** ........................................................................................................ 102

3.2.3.1. 3D Shallow and deep crack Bend specimen ........................................................................... 102

3.2.3.2. 3D Deep cracked tension specimen ....................................................................................... 103

3.3. **Compact Tension Specimen** ...................................................................................................... 104

3.3.1. **Validation** .................................................................................................................................... 105

3.4. **Small-Scale Yielding Model** ......................................................................................................... 107

3.4.1. **Validation:** ................................................................................................................................. 108

3.5. **Pipe Material:** .............................................................................................................................. 109

3.6. **RIKS Analyses:** ............................................................................................................................ 111

3.7. **Local Approach to Ductile Failure** .............................................................................................. 112

3.7.1. **Rice and Tracey Ductile Failure model:** ................................................................................. 112

3.7.2. **Work of Fracture** ....................................................................................................................... 113

3.7.3. **Calibration of the local approaches:** ....................................................................................... 113

**CHAPTER 4 MATERIAL CHARACTERISATION** ....................................................................................... 115

4.1. **Metallurgical** .................................................................................................................................... 115

4.1.1. **Pipe Material** ............................................................................................................................. 115
New insights into the competition between ductile tearing and plastic collapse

4.1.2. Plate Material...................................................................................................................... 116

4.2. TENSILE TESTING .............................................................................................................. 117

4.2.1. Pipe Material ...................................................................................................................... 117

4.2.1.1. Experimental results ...................................................................................................... 117

4.2.1.2. Defining material behaviour for FEA analysis ................................................................. 118

4.2.2. Plate Material ...................................................................................................................... 119

4.3. HARDNESS TESTING ......................................................................................................... 122

4.4. FRACTURE TOUGHNESS TESTING .................................................................................... 124

4.4.1. Pipe Material testing ........................................................................................................... 124

4.4.1.1. Fracture toughness testing ............................................................................................... 124

4.4.1.2. Digital Image Correlation ............................................................................................... 131

4.4.2. Plate Material ...................................................................................................................... 134

4.4.2.1. Fracture toughness testing ............................................................................................... 134

4.4.2.2. Material variation through the length of the plate ........................................................... 138

4.4.2.3. J-Resistance Curves ........................................................................................................ 142

4.4.2.4. Digital image correlation ................................................................................................ 146

4.5. CONVENTIONAL FRACTURE MECHANICS: ................................................................. 148

4.5.1. J-Q locus ............................................................................................................................. 148

4.5.2. SEN specimen manufactured from the pipe material ......................................................... 149

4.5.3. CT specimen manufactured from the plate material ........................................................... 151

4.5.3.1. Interim conclusions: ......................................................................................................... 152

4.6. RICE & TRACEY AND WORK OF FRACTURE LOCAL APPROACHES ................................ 154

4.6.1. SEN specimen ................................................................................................................... 155

4.6.2. Plate Material: .................................................................................................................. 158

4.6.2.1. Damage in the fracture process zone ................................................................................ 158

4.7. PREDICTION OF VALID INITIATION TOUGHNESS USING A MODIFIED BOUNDARY LAYER MODEL: ................................................................. 161

4.7.1. Definition of the Plastic zone size and MBLM methodology validity: ................................ 161

4.7.2. Methodology and Results: ................................................................................................ 165

4.8. PREDICTION OF ENGINEERING FAILURE IN A PIPE CONTAINING A CIRCUMFERENTIAL DEFECT: .............................................................................. 169

4.8.1. Limit Load Analysis: ........................................................................................................... 169

4.8.2. Conventional Failure Assessment Diagram ......................................................................... 171

4.8.3. Effect of initiation toughness measurements: ....................................................................... 172

4.8.4. Analysis of pipe failure using Option 1 FAD and J_{\text{validity}}: ......................................... 174

4.8.5. Analysis of pipe failure using Option 1 FAD and J_{\text{MBLM}}: ............................................. 174

4.8.6. Modified FAD for ductile tearing ....................................................................................... 180

4.8.6.1. Loading condition \( \lambda = 0.5 \) ...................................................................................... 181

4.8.6.2. Loading condition \( \lambda = 0.75 \) ...................................................................................... 185

4.8.6.3. Loading condition \( \lambda = \infty \) ......................................................................................... 189
New insights into the competition between ductile tearing and plastic collapse

4.8.4. Comparison with Option 1 FAD analysis: .......................................................... 194
4.8.7. Constraint corrected Failure Assessment Diagram .............................................. 196

CHAPTER 5 DISCUSSION ........................................................................................................... 199

Section 1: Defining Failure Assessment methodology ..................................................... 201
5.1. DEFINING FRACTURE PROPERTIES FOR USE IN A STRUCTURAL INTEGRITY ASSESSMENT ........................................................................................................... 201
5.1.1. Defining initiation toughness experimentally: ...................................................... 201
5.1.2. Initiation toughness effect of Failure Assessment Diagram: ............................... 204
5.1.3. Comparison with fracture data obtained in the literature: ................................. 205
5.1.4. Defining Constraint dependent fracture toughness experimentally ..................... 205
5.1.4.1. Defining J-Q fracture locus: .............................................................................. 206
5.1.4.1.i. Initiation toughness constraint dependence: ................................................. 206
5.1.4.1.ii. Component constraint loss with increase in load: ...................................... 208
5.1.4.1.iii. Defining the J-Q fracture locus: ................................................................ 209
5.1.5. Interim conclusions: ............................................................................................... 209
5.2. IMPLEMENTATION OF LOCAL APPROACH METHODOLOGIES ................................................................. 210
5.2.1. Calibration of the critical damage criteria: ......................................................... 210
5.2.2. Averaging methodology ...................................................................................... 211
5.2.3. Local Approach criterion as a constraint independent fracture toughness measurement ........................................................................................................... 213
5.2.4. Interim conclusion: ............................................................................................... 214
5.3. LOCAL APPROACH BASED FAILURE ASSESSMENT METHODOLOGY: ............................................................. 215
5.3.1. Application of the methodology ....................................................................... 216
5.3.2. Local Approach Methodology example case: Primary system piping with circumferential defect 218
5.3.3. Interim Conclusions: ......................................................................................... 219
5.4. DEFINING FRACTURE TOUGHNESS LOCUS ANALYTICALLY: ................................................................. 220
5.4.1. Defining the J-Q constraint correction method: .................................................. 220
5.4.2. Defining material properties in relation to T/σy ................................................ 223
5.4.3. Comparison with existing results ....................................................................... 224
5.4.4. Application of the methodology to define R6 FAD and constraint corrected FAD .................................................................................................................. 225
5.4.4.1. Conventional FAD approach ....................................................................... 225
5.4.4.2. Constraint corrected FAD ........................................................................... 225
5.5. Failure of a pipe under internal pressure and remote tension ................................ 228
5.6. LIMITATIONS OF LOCAL APPROACH METHODOLOGIES ........................................................................ 232
5.7. GEOMETRICAL LIMITATIONS OF THE APPROACH .................................................................................... 232
5.7.1. Size effect on initiation toughness: ..................................................................... 232
5.7.1.1. Reduction in toughness with reduction in specimen size ............................... 233
New insights into the competition between ductile tearing and plastic collapse

5.7.1.1.i. Transition from tensile ductile fracture to shear ductile fracture .................................. 234
5.7.1.1.ii. Variation in material throughout the plate ................................................................. 236
5.7.1.2. Interim conclusion: ........................................................................................................ 236

CHAPTER 6 CONCLUDING REMARKS .................................................................................. 237

CHAPTER 7 FUTURE WORK........................................................................................................ 240

7.1. EXPERIMENTAL VALIDATION OF THE LOCAL APPROACH MODIFIED FAD...................... 240
7.2. DERIVATION OF THE J-Q FRACTURE LOCUS FOR A RANGE OF MATERIAL PROPERTIES .......... 240
7.3. ANALYSIS OF FAILURE IN A RANGE OF COMPONENTS AND MATERIALS ................................ 241

References.................................................................................................................................. 242
Appendix 1-1 ................................................................................................................................ 248
Appendix 1-2 ................................................................................................................................ 252
Appendix 1-3 ................................................................................................................................ 256

Word count: 49,970
List of Figures

FIGURE 1: CRACK TIP LOADING MODES ................................................................. 28
FIGURE 2: CONTOUR INTEGRAL AROUND A CRACK-TIP USED TO CALCULATE THE J-INTEGRAL .......................................................... 30
FIGURE 3: TANGENTIAL STRESS FIELD AHEAD OF A CRACK IN A BOUNDARY LAYER FORMATION AT DIFFERENT LEVELS OF T-STRESSES ........................................................................................................... 35
FIGURE 4: CONSTRAINT CORRECTED J-RESISTANCE CURVES. EXPERIMENTAL RESULTS OBTAINED FROM DEEP CRACK SE(B) SPECIMENS OF HY80 TESTED AT ROOM TEMPERATURE ......................................................... 37
FIGURE 5: CRITICAL VALUE OF J AS A FUNCTION OF T/s, FOR A THREE POINT BEND AND CCT SPECIMEN FOR A MILD STEEL AT -50°C[24] ........................................................................................................ 38
FIGURE 6: CRITICAL VALUE OF J AS A FUNCTION OF Q FOR A THREE POINT BEND AND CCT SPECIMEN FOR A MILD STEEL AT -50°C[24] ........................................................................................................ 38
FIGURE 7: DUCTILE TOUGHNESS OF AN A710 STEEL AT VARIOUS CRACK EXTENSION AS A FUNCTION OF T-STRESS [27] ........................................................................................................ 39
FIGURE 8: RELATIONSHIP BETWEEN Q AND T IN SMALL SCALE YIELDING CONDITIONS[29] .................................................................................. 40
FIGURE 9: Q VS. DISTANCE AHEAD OF THE CRACK TIP FOR SEN(B) SPECIMENS, JOYCE & LINK [32] .................................................................................. 40
FIGURE 10: THE VON MISSES AND TRESCA YIELD SURFACE IN THREE DIMENSIONAL STRESS SPACE .................................................................................. 43
FIGURE 11: SCHEMATIC REPRESENTATION OF THE PLASTIC ZONE AT THE EDGE (PLANE STRESS AND THE CENTRE OF A MATERIAL CONTAINING A CRACK) .......................................................................................... 44
FIGURE 12: EFFECT OF CONSTRAINT, DEFINED USING THE ELASTIC T-STRESS, ON THE PLASTIC ZONE SIZE .................................................................................. 45
FIGURE 13: MATERIAL MODELS AND STRUCTURAL RESPONSES [33] .................................................................................................................. 46
FIGURE 15: A)TWICE ELASTIC ZONE CRITERION, B) TANGENT INTERSECTION CRITERION [33] .................................................................................. 49
FIGURE 16: PLASTIC WORK CRITERION [33] .................................................................................. 50
FIGURE 17: DIGITAL IMAGE CORRELATION OF FEATURES[40] .................................................................................................................. 51
FIGURE 18: SEM FRACOGRAPHY OF A STAINLESS STEEL .................................................................................................................. 54
FIGURE 19: VOID NUCLEATION OF A 304 STAINLESS STEEL, A) EXHIBITING COALESCENCE OF MULTIPLE POPULATION OF PARTICLES, AND B) SINGLE POPULATION OF PARTICLES .................................................................................. 55
FIGURE 20: THE PROCESS OF PLASTIC DAMAGE AND DUCTILE FAILURE [48] .................................................................................. 55
FIGURE 21 THE PROCESS ZONE IN A) THE UN-CRACKED LIGAMENT AND B) THE CRACK PROPAGATION PLANE [48] .................................................................................. 55
FIGURE 22: OPTION 1 FAILURE ASSESSMENT DIAGRAM AND LOADING LINE. LOAD A REPRESENTS AN ANALYSED LOAD AND B REPRESENTS THE POINT AT FRACTURE .................................................................................. 65
FIGURE 23: CONSTRAINT CORRECTED FAILURE ASSESSMENT DIAGRAM .................................................................................. 67
FIGURE 24 FRACTURE SPECIMEN GEOMETRY DESIGNED ACCORDING TO ASTM 1820 IN [86] .................................................................................. 70
FIGURE 25: J-RESISTANCE CURVE FOR A SOLUTION ANNEALED 304 STAINLESS STEEL [87] .................................................................................. 70
New insights into the competition between ductile tearing and plastic collapse

FIGURE 26: CRACKED GEOMETRY ..............................................................................................................71
FIGURE 27: JAERI EXPERIMENTAL SOLUTIONS [88] ..................................................................................73
FIGURE 28: PIPE MATERIAL COORDINATE SYSTEM ......................................................................................76
FIGURE 29: PLATE MATERIAL COORDINATE SYSTEM ....................................................................................76
FIGURE 30: TENSILE SPECIMEN DESIGN .......................................................................................................79
FIGURE 31: SINGLE EDGE DEEP CRACKED SPECIMEN ENGINEERING DRAWINGS ........................................81
FIGURE 32: SINGLE EDGE SHALLOW CRACK PRE-CRACK GEOMETRY ENGINEERING DRAWINGS ..................82
FIGURE 33: SINGLE EDGE SHALLOW CRACKED SPECIMEN ENGINEERING DRAWINGS ...............................82
FIGURE 34: SPECIMEN CUTTING PLAN FROM PIPE SECTION 1 ........................................................................83
FIGURE 35: SPECIMEN CUTTING PLAN PIPE SECTION 2 ..................................................................................84
FIGURE 36: SINGLE EDGE DEEP AND SHALLOW CRACKED SPECIMEN LOADING CONDITIONS ...................85
FIGURE 37: SINGLE EDGE DEEP CRACKED TENSION SPECIMEN LOADING CONDITIONS .................................85
FIGURE 38: COMPACT TENSION SPECIMEN CUTTING PLAN - PLATE MATERIAL .................................................87
FIGURE 39: 15MMCT ENGINEERING DRAWING .............................................................................................88
FIGURE 40: 10MMCT ENGINEERING DRAWING .............................................................................................88
FIGURE 41: EXAMPLE OF A SPECKLE PATTERN USED FOR DIGITAL IMAGE CORRELATION ON A SEN(B) S  
BEFORE TESTING.............................................................................................................................................95
FIGURE 42: HALF SEN(B) SPECIMEN, 2D FEA MODEL, LENGTH= 120MM, W= 20MM, S= 4W AND S= 5W ......97
FIGURE 43: HALF SEN(T) SPECIMEN, 2D FEA MODEL, LENGTH= 200MM, W= 20MM, S= 4W AND S= 5W ....98
FIGURE 44: THREE DIMENSIONAL SINGLE EDGE CRACKED MODEL - CRACK TIP MESH REFINEMENT ..........101
FIGURE 45: THREE DIMENSIONAL SINGLE EDGE CRACKED THREE POINT BEND SHALLOW AND DEEP CRACK  
MODEL - SYMMETRY CONDITIONS - LOADING CONDITIONS - BULK MESH REFINEMENT ...............................101
FIGURE 46: THREE DIMENSIONAL SINGLE EDGE CRACKED TENSION MODEL - SYMMETRY CONDITIONS - LOADING  
CONDITIONS - BULK MESH REFINEMENT .................................................................................................102
FIGURE 47: ONE-QUARTER OF CT SPECIMEN WITH A/W=0.55, W=2B AND B=25MM .................................104
FIGURE 48: CRACK TIP MESH REFINEMENT OF CT SPECIMEN WITH A/W=0.55, W=2B AND B=25MM ..........105
FIGURE 49: MODIFIED BOUNDARY LAYER MODELS WITH COLLAPSED NODE MESH REFINEMENT ...............107
FIGURE 50: MODIFIED BOUNDARY LAYER MODEL WITH SQUARE CRACK TIP MESH REFINEMENT .............108
FIGURE 51: AXI-SYMMETRIC PIPE MODEL WITH RADIUS (R) = 147 MM AND THICKNESS (T) = 33 MM – SQUARE  
CRACK TIP MESH OF DIMENSION 0.2 X 0.2 MM ............................................................................................110
FIGURE 52: MICROGRAPH OF PIPE MATERIAL IN THE A-R ORIENTATION – MAGNIFICATION 100 TIMES ..........115
FIGURE 53: MICROGRAPH OF PIPE MATERIAL IN THE H-R ORIENTATION – MAGNIFICATION 100 TIMES ..........115
FIGURE 54: MICROGRAPH OF PLATE MATERIAL IN THE L-S ORIENTATION – MAGNIFICATION 100 TIMES ........116
FIGURE 55: MICROGRAPH OF PLATE MATERIAL IN THE T-S ORIENTATION – MAGNIFICATION 100 TIMES ........116
FIGURE 56: MICROGRAPH OF PLATE MATERIAL IN THE T-L ............................................................................116
FIGURE 57: MICROGRAPH OF PLATE MATERIAL IN THE T-L ............................................................................116
FIGURE 58: ENGINEERING STRESS VS. STRAIN - PIPE MATERIAL - TENSILE SPECIMEN TESTED AT ROOM  
TEMPERATURE ..............................................................................................................................................117
FIGURE 59: TRUE STRESS VS. TRUE STRAIN BEHAVIOUR, 304 SS PIPE MATERIAL, TENSILE SPECIMEN TESTED AT  
ROOM TEMPERATURE ........................................................................................................................................118

10
New insights into the competition between ductile tearing and plastic collapse

Figure 60: Engineering stress vs. strain, 304SS plate material, tested at room temperature.... 119
Figure 61: True stress vs. true strain behaviour, 304 SS plate material, tensile specimen tested at room temperature .................................................. 120
Figure 62: Hardness variation in T-S orientation of the 304SS plate material - HV20............ 122
Figure 63: Yield strength variation in T-S orientation of the 304SS plate material calculated from the hardness measurements.................................................. 123
Figure 64: Initial fatigue crack length through specimen thickness - shallow cracked Single Edge Crack Bend specimen .................................................. 124
Figure 65: Initial fatigue crack length through specimen thickness - deep cracked Single Edge Crack Tension specimen .................................................. 125
Figure 66: Initial fatigue crack length through specimen thickness - deep cracked Single Edge Crack Bend specimen .................................................. 125
Figure 67: (a) Measured and (b) averaged using the 9 point average scheme, final crack length - deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 Multi-specimen fracture toughness testing methodology .................................................. 126
Figure 68: (a) Measured and (b) averaged using the 9 point average scheme, final crack length - deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 Multi-specimen fracture toughness testing methodology .................................................. 126
Figure 69: (a) Measured and (b) averaged using the 9 point average scheme, final crack length - deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 Multi-specimen fracture toughness testing methodology .................................................. 127
Figure 70: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_S specimen - tested according to P2-92 – displacement controlled 0.2mm/min - room temperature .................................................. 127
Figure 71: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_D specimen - tested according to P2-92 – displacement controlled 0.2mm/min - room temperature .................................................. 128
Figure 72: J-Resistance curves for SEN(B)_S, SEN(D)_D and SEN(T)_D specimen, room temperature, displacement controlled 0.2mm/min.................................................. 130
Figure 73: Initial fatigue crack length through specimen thickness – 25mmCT ...................... 134
Figure 74: Initial fatigue crack length through specimen thickness – 15mmCT ...................... 135
Figure 75: Initial fatigue crack length through specimen thickness – 10mmCT ...................... 135
Figure 76: Load vs. CMOD – Compact Tension specimen - 25mmCT – B=25mm – W=2B – tested at room temperature following ASTM 1820 ........................................................................ 136
Figure 77: Load vs. CMOD – Compact Tension specimen - 15mmCT – B=15mm – W=2B – tested at room temperature following ASTM 1820 ........................................................................ 136
Figure 78: Load vs. CMOD – Compact Tension specimen - 10mmCT – B=10mm – W=2B – tested at room temperature following ASTM 1820 ........................................................................ 137
Figure 79: Load vs. CMOD – Compact Tension specimen - 10mmCT – Yield = 424 MPa - room temperature - opening strain observed using DIC and FEA at a normalised distance of 0.1 ........................................................................ 139
New insights into the competition between ductile tearing and plastic collapse

Figure 81: Normalised load vs. CMOD - 25mmCT, 15mmCT, 10mmCT - yield = 424 MPa - room temperature - opening strain observed using DIC and FEA at a normalised distance of 0.1

Figure 82: J-resistance curves - CT specimen B=25mm, 15mm and 10mm - W=2B - average of 3 tests per specimen size - ASTM 1820 [12]

Figure 83: Load vs. CMOD - compact tension specimen - 25mmCT, 15mmCT and 10mmCT - tested at room temperature following ASTM 1820 - with initiation points

Figure 84: 1_25mmCT J-resistance curve - ASTM 1820 - tested at room temperature - equivalent plastic strain obtained from digital image correlation

Figure 85: 4_15mmCT J-resistance curve - ASTM 1820 - tested at room temperature - equivalent plastic strain obtained from digital image correlation

Figure 86: 4_10mmCT J-resistance curve - ASTM 1820 - tested at room temperature - equivalent plastic strain obtained from digital image correlation

Figure 87: Crack tip stress field at the centre of the SEN(B)_S, SEN(B)_D and SEN(T)_D specimen at a value of J corresponding to initiation

Figure 88: Variation in constraint, as defined by a modified Q parameter, at initiation of ductile tearing, for 304 stainless steel pipe material tested at ambient temperature

Figure 89: Variation in constraint, as defined by the conventional Q parameter, at initiation of ductile tearing, for 304 stainless steel plate material tested at ambient temperature

Figure 90: Evolution of damage averaged through thickness defined using the high constraint Rice & Tracey local approach vs. J-integral averaged through thickness in the SEN(B)_S, SEN(B)_D and SEN(T)_D geometries

Figure 91: Evolution of damage averaged through thickness defined using the work of fracture local approach vs. J-integral averaged through thickness in the SEN(B)_S, SEN(B)_D and SEN(T)_D geometries

Figure 92: Evolution of damage averaged through thickness defined using the generalised Rice & Tracey local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT

Figure 93: Evolution of damage averaged through thickness defined using the high constraint Rice & Tracey local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT

Figure 94: Evolution of damage averaged through thickness defined using the work of fracture local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT

Figure 95: Evolution of damage averaged through thickness defined using the work of fracture local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT

Figure 96: Plastic zone size with yield defined as the deviation from the elastic loading line, in a MBLM model of r= 200000 mm for an applied J=1000kJm⁻², and range of elastic T-stress conditions

Figure 97: Plastic zone size with yield defined as the proof stress, in a MBLM model of r= 200000 mm for an applied J=1000kJm⁻², and range of elastic T-stress conditions
New insights into the competition between ductile tearing and plastic collapse

FIGURE 98: HIGH CONSTRAINT RICE & TRACEY vs. J OBTAINED FROM A MODIFIED BOUNDARY LAYER MODEL FOR A RANGE OF BIAXIALITY RATIOS - PIPE MATERIAL ........................................................... 165

FIGURE 99: \( K_{\text{mat}}^C / K_{\text{mat}} \) AS DEFINED USING THE HIGH CONSTRAINT RICE & TRACEY PARAMETER vs. T - PIPE MATERIAL ........................................................................................................ 167

FIGURE 101: VARIATION IN LIMIT LOAD WITH CRACK DEPTH FOR LOADING CONDITIONS \( \Lambda = 0.5, 0.75 \) AND \( \infty \) .............................................................................................................................. 170

FIGURE 102: R6 OPTION 1 & 3 FAILURE ASSESSMENT DIAGRAM – FAILURE PREDICTION OF CIRCUMFERENTIALLY CRACKED PIPE WITH FRACTURE TOUGHNESS DEFINED USING SPECIMEN VALIDITY LIMIT, \( J_{0.2} \) AND HIGH CONSTRAINT FRACTURE TOUGHNESS DEFINED ANALYTICALLY USING LOCAL APPROACH CRITERIA COMBINED TO MBLM. .................................................................................. 173

FIGURE 103: ENGINEERING PREDICTION OF LR AT FAILURE USING R6 FAD OPTION 1 & J SPECIMEN VALIDITY LIMIT vs. CRACK DEPTH FOR A CIRCUMFERENTIALLY CRACKED PIPE LOADED UNDER A RANGE OF \( \Lambda \) CONDITIONS .................................................................................................................... 174

FIGURE 105: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = 0.5 \) AND \( a/W = 0.6, 0.7, 0.8 \) AND 0.9 .................................................................................................................. 176

FIGURE 106: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = 0.75 \) AND \( a/W = 0.1, 0.2, 0.3, 0.4 \) AND 0.5 .................................................................................................................. 177

FIGURE 107: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = 0.75 \) AND \( a/W = 0.6, 0.7, 0.8 \) AND 0.9 .................................................................................................................. 177

FIGURE 108: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = \infty \) AND \( a/W = 0.1, 0.2, 0.3, 0.4 \) AND 0.5 ............................................................................................................. 178

FIGURE 109: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = \infty \) AND \( a/W = 0.6, 0.7, 0.8 \) AND 0.9 ............................................................................................................. 179

FIGURE 110: ENGINEERING PREDICTION OF LR AT FAILURE USING R6 FAD OPTION 1 & J SPECIMEN VALIDITY LIMIT vs. CRACK DEPTH FOR A CIRCUMFERENTIALLY CRACKED PIPE LOADED UNDER A RANGE OF \( \Lambda \) CONDITIONS ....................................................................................................................... 179

FIGURE 111: LOCAL APPROACH MODIFIED FAILURE ASSESSMENT DIAGRAM .......................................................................................... 181

FIGURE 112: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = 0.5 \) AND \( a/W = 0.1, 0.2, 0.3, 0.4 \) AND 0.5 ............................................................................................................. 182

FIGURE 113: MODIFIED FAILURE ASSESSMENT DIAGRAM USING HIGH CONSTRAINT RICE & TRACEY LOCAL APPROACH METHODOLOGY – PIPE MATERIAL – PWR PRIMARY SYSTEM PIPING ANALYSIS WITH \( \Lambda = 0.5 \) AND \( a/W = 0.6, 0.7, 0.8 \) AND 0.9 ............................................................................................................. 182
New insights into the competition between ductile tearing and plastic collapse

Figure 114: Modified Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.5$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 183

Figure 115: Modified Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.5$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 183

Figure 116: Modified Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.5$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 184

Figure 117: Modified Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.5$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 184

Figure 118: LA Failure Assessment Diagram using High Constraint Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 186

Figure 119: LA Failure Assessment Diagram using High Constraint Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 186

Figure 120: LA Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 187

Figure 121: LA Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 187

Figure 122: LA Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 188

Figure 123: LA Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 188

Figure 124: Modified Failure Assessment Diagram using High Constraint Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 190

Figure 125: Modified Failure Assessment Diagram using High Constraint Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9 ................................................................. 190

Figure 126: Modified Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\Lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5 ................................................................. 191
New insights into the competition between ductile tearing and plastic collapse

Figure 127: Modified Failure Assessment Diagram using General Rice & Tracey Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9

Figure 128: Modified Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\lambda=0.75$ and $A/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 129: Modified Failure Assessment Diagram using Work of Fracture Local Approach Methodology – Pipe Material – PWR Primary System Piping Analysis with $\lambda=0.75$ and $A/W=0.6$, 0.7, 0.8 and 0.9

Figure 130: Prediction of $L_r$ at Ductile Failure against Crack Depth – Internally Circumferentially Cracked Pipe – $\lambda=0.5$

Figure 131: Prediction of $L_r$ at Ductile Failure against Crack Depth – Internally Circumferentially Cracked Pipe – $\lambda=0.75$

Figure 132: Prediction of $L_r$ at Ductile Failure against Crack Depth – Internally Circumferentially Cracked Pipe – $\lambda=\infty$

Figure 133: Percentage Difference between the Average $L_r$ at Ductile Initiation Predictions Using the Local Approaches and HE $L_r$ at Ductile Initiation Prediction Using the RG Option 1 Using Lower Bound $J_{0.2BL}$ Obtained from the MLBML Model for $\lambda=0.5$, 0.75 and $\infty$ for a Range of Crack Depths

Figure 134: Evolution of $T/L_r$ against $L_r$ in the Pipe Geometry - $\lambda=0.5$ - $A/t=0.5$

Figure 135: Evolution of $Q$ against $L_r$ in the Pipe Geometry - $\lambda=0.5$ - $A/t=0.5$

Figure 136: Q Parameter Constraint Corrected Failure Assessment Diagram – Pipe Geometry – $\lambda=0.5$ - $A/t=0.5$

Figure 137: Failure Assessment of Structure Methodology Decision Map

Figure 138: Failure Assessment of Structure Methodology Decision Map – Experimental Definition of Fracture Toughness Valid according to Test Standards

Figure 139: J Validity Limit as a Function of Thickness with Initiation Toughness Defined as the Lower Bound Toughness Obtained from the MLBML Model

Figure 140: J Validity Limit as a Function of Thickness with Initiation Toughness Defined as $J_{0.2BL}$ Obtained from the SEN(B)$_D$ Specimen

Figure 141: RG Option 1 & 3 FAD with Loading Line Defined using $K_{0.2L}$, $K_{V,\text{Validity}}$ and $K_{\text{MBML}}$

Figure 142: Variation of Normalised Q Parameter by the Q Parameter at a Normalised Distance of 0.1 against Normalised Distance to the Crack Tip in the SEN(B)$_D$, SEN(B)$_S$ and SEN($T$)$_D$

Figure 143: Variation of the Generalised Rice & Tracey Local Approach Parameter through Thickness at Initiation

Figure 144: Variation of the Work of Fracture Local

Figure 145: Local Approach to Defining the Failure Mechanism of a Component Containing a Defect
New insights into the competition between ductile tearing and plastic collapse

**Figure 148**: R6 constraint corrected FAD approach to defining the failure mechanism of a component containing a defect ................................................................. 217

**Figure 149**: LR at ductile initiation for a circumferentially cracked pipe laded under internal pressure and tension ($\lambda=0.5$) for a range of crack depths ........................................ 219

**Figure 150**: Methodology to define analytically the constraint dependent fracture locus using a modified boundary layer model .................................................. 223

**Figure 151**: Methodology to define the constraint corrected R6 FAD using the constraint dependant fracture locus obtained analytically ........................................ 226

**Figure 152**: Cracked geometry .......................................................................................................... 228

**Figure 153**: JAERI experimental solutions [88] ........................................................................... 230

**Figure 154**: Variation in fracture toughness with specimen thickness [109] ................................. 233

**Figure 155**: SEM fractography showing the rupture mode for Weldox 420, at triaxiality=1.1 under tensile ductile failure ................................................................. 235

**Figure 156**: SEM fractography showing the rupture mode for Weldox 420, at triaxiality=0.85 under tensile/shear ductile fracture transition ........................................ 235

**Figure 157**: SEM fractography showing the rupture mode for Weldox 420, at triaxiality=0.47 under tensile/shear ductile fracture transition ........................................ 235

**Figure 158**: SEM fractography of plate material, 25mmCT, at the centre of the specimen ......... 235

**Figure 159**: SEM fractography of plate material, 15mmCT, at the centre of the specimen ......... 235

**Figure 160**: SEM fractography of plate material, 10mmCT, at the centre of the specimen ......... 235

**Figure 161**: The effective plastic strain at failure vs. stress triaxiality, where solid circle and round circle denote different measures of plastic strain and the open square presents results from smooth round bars [111] ......................................................... 236

**Figure 162**: Load vs. Crack mouth opening displacement (CMOD) for SEN(B)_D specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2MM/Min – Room temperature ................................................................. 248

**Figure 163**: J-Resistance curve for SEN(B)_D specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled 0.2MM/Min – Room temperature ................................................................. 249

**Figure 163**: Load vs. Crack mouth opening displacement (CMOD) for SEN(B)_S specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2MM/Min – Room temperature ................................................................. 252

**Figure 164**: J-Resistance curve for SEN(B)_S specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled 0.2MM/Min – Room temperature ................................................................. 253

**Figure 165**: Load vs. Crack mouth opening displacement (CMOD) for SEN(T)_D specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2MM/Min – Room temperature ................................................................. 256
New insights into the competition between ductile tearing and plastic collapse

**Figure 166:** J-Resistance curve for SEN(T)_D specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled

0.2mm/min – room temperature

257
List of Tables

TABLE 1: ACCURACY OF DISPLACEMENT VECTOR AS A FUNCTION OF INTERROGATION WINDOW SIZE, USING THE
DAVIS DIC SOFTWARE .......................................................... 52
TABLE 2: TENSILE AND HARDNESS PROPERTIES OF SOLUTION ANNEALED 304LN STAINLESS STEEL[86] ........ 70
TABLE 3: FOUR POINT BEND PIPE TEST RESULTS [87] .......................................................... 72
TABLE 4: COMPARISON BETWEEN PREDICTED AND MEASURED MAXIMUM LOADS IN STANDARDS SPECIMENS
AND PIPE SPECIMENS BY THE R6 METHOD ........................................................................ 73
TABLE 5: CHEMICAL COMPOSITION OF PLATE AND PIPE MATERIAL AND ASTM A250 GRADE DEFINITION .... 77
TABLE 6: POSITION OF EXTRACTION OF TENSILE SPECIMEN IN THE PLATE MATERIAL. ...................... 78
TABLE 7: SINGLE EDGE CRACKED SPECIMEN TEST MATRIX .................................................................. 83
TABLE 8: COMPACT TENSION SPECIMENS DIMENSIONS ...................................................................... 87
TABLE 9: UNLOADING COMPLIANCE CYCLES ..................................................................................... 89
TABLE 10: TENSILE BEHAVIOUR - PIPE MATERIAL TESTED AT ROOM TEMPERATURE - COMPARISON WITH ASTM 250 ........................................................................................................ 118
TABLE 11: TENSILE BEHAVIOUR - PLATE MATERIAL TESTED AT ROOM TEMPERATURE - COMPARISON WITH
ASTM 250 [97] .................................................................................. 120
TABLE 12: SEN(B)_D INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED
USING A TRAVELLING OPTICAL MICROSCOPE ........................................................................ 129
TABLE 13: SEN(T)_D INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED
USING A TRAVELLING OPTICAL MICROSCOPE ........................................................................ 129
TABLE 14: SEN(B)_S INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED
USING A TRAVELLING OPTICAL MICROSCOPE ........................................................................ 129
TOUGHNESS FOR SEN(B)_S, SEN(B)_D AND SEN(T)_D AS DEFINED BY THE BLUNTING LINE AT 0.2MM
CRACK EXTENSION, J_{0.2} AND J_{0.2BL} .................................................................................. 131
TABLE 16: 25MMCT INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED USING
A TRAVELLING OPTICAL MICROSCOPE .................................................................................. 135
TABLE 17: 15MMCT INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED USING
A TRAVELLING OPTICAL MICROSCOPE .................................................................................. 136
TABLE 18: 10MMCT INITIAL CRACK LENGTH, FINAL CRACK LENGTH AND CRACK EXTENSION MEASURED USING
A TRAVELLING OPTICAL MICROSCOPE .................................................................................. 136
TABLE 19: CORRECTED YIELD STRENGTH - 25MMCT, 15MMCT, 10MMCT - CORRECTED USING A
COMBINATION OF DIC AND FEA ....................................................................................... 140
TABLE 20: J-VAILDITY LIMITS ACCORDING TO ASTM [12], BS7448 [6] AND ESIS P2-92 AND INITIATION
TOUGHNESS FOR 25MMCT, 15MMCT AND 10MMCT AS DEFINED BY THE BLUNTING LINE AT 0.2MM CRACK
EXTENSION, J_{0.2} AND J_{0.2BL} ...................................................................................... 144
TABLE 21: LOCAL APPROACH CRITERIA AVERAGED THROUGH THICKNESS AT INITIATION DEFINED USING
J_{0.2BL} IN SEN(B)_S, SEN(B)_D AND SEN(T)_D SPECIMEN .................................................. 155
New insights into the competition between ductile tearing and plastic collapse

TABLE 22: LOCAL APPROACH CRITERIA AVERAGED THROUGH THICKNESS AT INITIATION DEFINED USING J0.2BL IN 25MMCT, 15MMCT AND 10MMCT SPECIMEN .................................................................158

TABLE 23: ANALYTICALLY DEFINED INITIATION TOUGHNESS AS A FUNCTION OF CONSTRAINT ........................................166

TABLE 24: INTERNAL LIMIT PRESSURE AS A FUNCTION OF CRACK DEPTH AND A FOR THE CIRCUMFERENTIALLY CRACKED PIPE MODEL ..............................................................................................................170

TABLE 25: JVALIDITY AS DEFINED USING ESIS P2-92, J0.2 DEFINED USING ESIS P2-92 BLUNTING LINE AND JMMLM

FRACTURE TOUGHNESS MEASUREMENTS FOR THE PIPE MATERIAL .......................................................................................172

TABLE 26: LR AT DUCTILE FRACTURE PREDICTED BY THE OPTION 1 FAD USING JVALIDITY AND JMMLM TO DEFINE FRACTURE TOUGHNESS ..................................................................................................................180

TABLE 27 LR AT DUCTILE FRACTURE PREDICTED BY THE LOCAL APPROACH METHODOLOGY AND THE R6 FAD ..........................................................................................................................193

TABLE 28: PERCENTAGE DIFFERENCE BETWEEN LR AT INITIATION DEFINED USING THE MBLM OR JVALIDITY LIMIT AND THE LOCAL APPROACH DEFINED FAILURE .................................................................................205

TABLE 30: ANALYTICAL AND EXPERIMENTAL PIPE TEST RESULTS .................................................................................................229
New insights into the competition between ductile tearing and plastic collapse

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New insights into the competition between ductile tearing and plastic collapse
New insights into the competition between ductile tearing and plastic collapse in 304(L) stainless steel pipe components

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September 2012

Structural integrity assessment of nuclear components assessed using the R6 Failure Assessment Diagram approach requires an understanding of the limiting condition in terms of both fracture and plastic collapse. For ductile materials, such as stainless steels used for nuclear components, including the primary pipe-work of a Pressurised Water Reactor (PWR), the limiting condition defined by plastic collapse is likely to occur prior to the initiation of fracture. This is due to the relatively low yield stress of the material and the high fracture toughness. If this is the case, structural integrity may be solely assessed on plastic collapse criteria, with little or no reference to fracture toughness; thus considerably simplifying the assessment procedure, whilst maintaining the integrity of the plant. Nevertheless, an in-depth understanding of fracture under plastic collapse conditions is required to make a robust case for single parameter assessments based on a plastic collapse criterion alone.

The challenge in this project lay in understanding and predicting ductile fracture initiation under large-scale yielding conditions, i.e. outside the normal validity limits of conventional elastic-plastic fracture mechanics as plastic collapse conditions are achieved. The approach developed in this research has explored three fracture assessment methods: (a) two parameter fracture mechanics based on the J-integral and a refined Q-parameter calculated closer to the crack-tip under widespread plasticity than is conventionally the case, (b) two local approach methods based on critical void growth ratio defined by Rice and Tracey, and (c) a local approach method based on the critical work of fracture. All three methodologies were found to adequately describe failure across a range of constraint conditions.

The fracture toughness constraint dependence of 304(L) stainless steel was studied experimentally and analytically. Significant constraint loss was shown to occur in nominally high constraint fracture toughness specimens due to extensive plastic deformation at fracture initiation. Furthermore, significant fracture toughness constraint dependence was observed experimentally. An analytical method using local approach criteria was developed to predict high constraint fracture toughness, required for structural integrity assessments, and to quantify the constraint dependence fracture toughness as a function of two parameter fracture mechanics based on the J-integral and the refined Q-parameter.

The influence of constraint on the prediction of failure in a stainless steel pipe containing a fully circumferential crack of various depths was investigated analytically for a range of loading conditions. A refined constraint independent failure assessment methodology was developed using local approach analyses. Using this methodology, the pipe component was shown to consistently fail by plastic collapse irrespective of the crack depth or loading condition. The conservatism of the conventional structural integrity assessment was quantified and shown to vary with crack depth and with loading conditions.

This research has suggested that failure in a 304(L) stainless steel pipe will be by plastic collapse prior to ductile initiation for a limited range of defects and loading conditions. Further analytical studies and experimental work will be required to demonstrate whether this observation is general for a wider range of defects and loading conditions.
Declaration

I, Andrew Paul Wasylyk, state that no portion of the work referred to in the thesis have been submitted in support of an application for another degree or qualification of this or any other university or other institute.
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Lastly, but most importantly, I would like to thank my girlfriend, as she has survived five years of thesis.
Problem definition
This project focuses on the evaluation of failure of stainless steel components. Ductile fracture in conditions close to plastic collapse is not, currently, well defined in failure assessment methodologies. Furthermore, robust engineering methodologies to obtain valid, lower bound, fracture toughness measurements for low yield high toughness materials, are not defined in the literature. Current guidance for the acquisition of fracture toughness data (ASTM, BS, ESIS) provides validity limits in order to ensure lower bound initiation toughness. However, very large specimens are required in order to obtain valid fracture toughness measurements for materials exhibiting high ductility and high fracture toughness, such as stainless steel. Furthermore, the current methodology to define fracture toughness constraint dependence requires the experimental derivation of fracture toughness under a range of constraint conditions, which leads to large material availability requirements.

Objective
The aim of this research project is to develop a robust methodology to define component failure under large scale yielding conditions. This required investigation into engineering methodologies to define constraint independent fracture for ductile tearing. This would lead to new understanding of stainless steel component failure conditions. This project had five objectives:

- To assess the initiation toughness constraint dependence for ductile tearing in 304(L) stainless steel.
- To define a methodology to assess constraint independent fracture toughness.
- To define a robust methodology to define lower bound fracture toughness and constraint dependent fracture locus.
- To develop a robust constraint independent failure assessment methodology.
- To assess the failure of a stainless steel pipe component.
Chapter 1 Literature Review

The literature review chapter summarises the current knowledge available regarding failure of components, specifically in stainless steels. This chapter includes two sections; the first section defines the current knowledge regarding fracture parameters, ductile fracture and plastic collapse. The subsequent section identifies and details the previous work that has been done on defining failure for stainless steel components. This includes an introduction into constraint corrected failure assessment methodologies and large scale tests performed on pipe components.

1.1. Introduction to Fracture mechanics

1.1.1. Griffith Theory

The analytical investigation of the behaviour of materials containing defects can be said to have started with Griffith in 1920 when he published his essay on the *Phenomena of Rupture and Flow in fluids*[1], where he mathematically described the creation of fracture surfaces in brittle material.

Griffith based his understanding on the “*theorem of minimum energy*”, which states that a material deforms in such a way that minimum potential energy is kept when going from one state of equilibrium to the next. He formulated the theory that “*In an elastic body deformed by the specific forces applied at its surface, the sum of the potential energy of the applied forces and strain energy of the body is diminished or unaltered by the introduction of a crack whose surfaces are traction-free.*”

Since an increase of potential energy is required for the creation of new surfaces, there must be an equal or larger decrease in the strain energy stored in the material. This can be expressed as equation (1).

\[
\frac{dW}{dA} = \frac{dU}{dA} + \frac{dS}{dA}
\]  

Where \(dW\) is the change in total energy, \(dU\) the strain energy stored in the material and \(dS\) the energy required to create new surfaces. For an idealised linear elastic material, failure
New insights into the competition between ductile tearing and plastic collapse

(i.e. crack extension) is the point at which the energy stored in the material is equal to the energy required to create new surfaces.

Griffith derived his equation for a homogeneous isotropic plate of uniform thickness, which contains a defect perpendicular to the remote stresses applied to the plate. For a material following the behaviour described by the elastic theory and Hookes Law (e.g. a material exhibiting brittle fracture such as glass), the crack will propagate when the critical load $P_e$ (equation(2)) will be exceeded.

$$P_e = \sqrt{\frac{2ET}{\pi c}} \quad (2)$$

With $E$ the Young’s Modulus, $T$ the surface tensions of the body and $c$ the width of crack.

It was soon found that the Griffith’s theorem is only applicable for perfectly brittle material such as glass and when the crack tip radius is of the order of magnitude of the atomic spacing [2]. The theory brakes down when larger crack tip radii are present and when material exhibit non perfectly brittle behaviour. The Griffith methodology was modified simultaneously by Irwin [3] and Orowan (equation (3)) [3] to address this problem and to acknowledge some amount of plastic flow prior to fracture. This meant that some metals fracture properties could be analysed.

$$P_e = \sqrt{\frac{2ET}{\pi c(1-\sigma^2)}} \quad (3)$$

With $\sigma$ the opening stress applied.

To date, two methodologies exist for the analysis of fracture behaviour of materials. The first one is based on an energy balance which finds its roots in Griffith’s equation. The second one is based on the stress concentration effect of defects and the description of the stress state ahead of the crack tip.
1.1.2. **Stress intensity factor**

The stress intensity parameter quantifies the stress and strain field ahead of a sharp defect in linear elastic conditions. Loading condition applied to a crack can be characterized by the combination of three different loading modes (Figure 1) [4]:

- Mode I describes the tensile component of the load which has for effect to open the crack
- Mode II is the in-plane-shear component of the load
- Mode III is the out of plane shear component of the load.

Mode 1 is considered as the most severe loading condition and hence the most relevant to defects in structures.

![Figure 1: crack tip loading modes](image)

In a body which has linear elastic response and in which the crack tip plastic field is very small and fully contained in an elastic field, a boundary layer formulation can be used to describe the stresses ahead of the crack tip. Irwin and Westergaard [4, 5] showed that the stresses at a point P ahead of the crack tip are only dependant on the mode and the magnitude of the loading and polar coordinate (r,θ) of the point from the crack tip (equation(4)), with K being the Stress Intensity Factor, and f_{ij} being a dimensionless function of the polar angle, A_m being the amplitude of the higher order terms and g a dimensionless angular function.
New insights into the competition between ductile tearing and plastic collapse

\[ \sigma_{ij} = \frac{K}{\sqrt{2\pi r}} \times f_{ij}(\theta) + \sum A_m r^m g_{ij}^m(\theta) \]  \hspace{1cm} (4)

Close to the crack tip, the higher order terms in equation (4) are small compared to the SIF controlled term in linear elastic conditions. This means that the crack tip stress field can be approximated using equation (5), with the stress field proportional to the mode of loading to which the crack tip is subjected. When a combination of loading modes is acting on the crack tip, the total stress intensity factor is the sum of the stress intensity factors for each loading modes.

\[ \sigma_{ij} = \frac{K_{(1,1,1,1)}}{\sqrt{2\pi r}} \times f_{ij}(\theta) \] \hspace{1cm} (5)

For mode 1 loadings, the crack tip stress field can be described using equation (6).

\[ \sigma_{xx} = \frac{K_1}{\sqrt{2\pi r}} \cos \left( \frac{\theta}{2} \right) \left[ 1 - \sin \left( \frac{\theta}{2} \right) \sin \left( \frac{3\theta}{2} \right) \right] \]

\[ \sigma_{yy} = \frac{K_1}{\sqrt{2\pi r}} \cos \left( \frac{\theta}{2} \right) \left[ 1 + \sin \left( \frac{\theta}{2} \right) \sin \left( \frac{3\theta}{2} \right) \right] \]  \hspace{1cm} (6)

\[ \tau_{xy} = \frac{K_1}{\sqrt{2\pi r}} \sin \left( \frac{\theta}{2} \right) \cos \left( \frac{\theta}{2} \right) \cos \left( \frac{3\theta}{2} \right) \]

The Stress Intensity Factor \( K \) describes the stress field ahead of the crack tip, and consequently the stress intensity factor at failure \( K_{IC} \) is a good fracture parameter for failure under LEFM conditions. This is why \( K_1 \) is conventionally used to define the crack tip stress field in fracture toughness specimens and components, and \( K_{IC} \) is still currently used as material fracture parameters in standards nowadays [6].

1.1.3. \textbf{J-integral}

Under elastic-plastic condition, when the plastic zone is still well contained within an elastic field, the J integral is used to define the stress strain field ahead of the crack tip. The
value of \( J \) at initiation, defined as \( J_{IC} \), is used as a material parameter in cracked bodies, describing the stress field at failure. Rice defined in 1968 [7] the non-linear energy release rate, \( J \), in a two dimensional deformation field (e.g. plane stress or plane strain) as shown in Figure 2, with \( \Pi \) being the potential energy and \( A \) the area of the cracked body.

\[
J = -\frac{d\Pi}{dA} \tag{7}
\]

Rice furthermore showed that under quasi-static conditions in an elastic or non-linear elastic material, \( J \) can be defined as a path independent contour integral given by equation (8) with \( W \) being the strain energy density and \( T \) a traction vector pointing outwards normal to the contour path

\[
J = \int_{\Gamma} \left( W \, dy - T \frac{du}{dx} \, dS \right) \tag{8}
\]

![Figure 2: contour integral around a crack-tip used to calculate the J-integral](image)

**1.1.4. HRR singularity & the path independent contour integral:**
Hutchinson [8], Rosenberg and Rice[9] published simultaneously a crack tip solution for non-linear elastic materials that can be described using Ramberg-Osgood power law relationship[10] to represent the true stress versus true strain behaviour of a hardening material.
New insights into the competition between ductile tearing and plastic collapse

\[
\frac{\varepsilon}{\varepsilon_0} = \frac{\sigma}{\sigma_0} + \alpha \left( \frac{\sigma}{\sigma_0} \right)^n
\]  

1.2. (9)

Where \( \sigma \) is the true stress, \( \varepsilon \) is the true strain, \( \sigma_0 \) is the reference stress and \( \varepsilon_0 = \sigma_0/E \) is the reference strain, E is Young’s modulus and \( \alpha \) is the R-O coefficient and \( n \) is the Ramberg-Osgood strain hardening exponent. Typical parameters for 304 Stainless Steel are \( \alpha = 1.691 \) and \( n = 5-10 \). [11].

Hutchinson showed[8], using a plastic deformation stress function that the J integral was a solution to the amplitude of the stress field within the plastic zone when this is well contained within an elastic zone and these stresses can be written as:

\[
\sigma_{\tilde{q}} = \sigma_0 \left( \frac{EJ}{\alpha \sigma_0^2 I_n r} \right)^{\frac{1}{n+1}} \tilde{\sigma}_{\tilde{q}}(n, \theta)
\]  

1.2.1. (10)

With \( \tilde{\sigma}_{\tilde{q}} \) being a dimensionless function of \( n \) and \( \theta \), and \( I_n \) being a dimensionless geometry function dependant on the work hardening exponent. The LEFM stress intensity factor being a solution of the above equation, for an elastic material with \( n = 1 \).

As with \( K_{IC} \), the value of \( J \) at which failure is deemed to occur can be used to characterize the material’s fracture toughness.

**Small Scale Yielding approximation:**

In a linear elastic body containing a defect, under plane strain conditions, the crack tip stress field can be defined using LEFM solutions of the form shown in equation (11).

\[
\sigma_{ij} = Kr^{-1/2} f_i j(\theta) + \text{non – singular terms}
\]  

(11)
When considering geometry with an elastic-plastic material formulation, loaded in such a way as a small plastic zone occurs ahead of the defect, the crack tip stress field can no longer be described using the linear elastic solution for crack tip stress fields within and around the crack tip plastic zone. The small scale yielding approximation assumes that the singular term should still govern the deformation state within a plastic zone, when surrounded by a large elastic zone. Hence, the elastic plastic problem is simplified to a boundary layer problem, whereby a semi infinite crack in an infinite body is considered, where the boundary conditions can be defined using linear elastic fracture mechanics.
New insights into the competition between ductile tearing and plastic collapse

1.3. Fracture Toughness

In order to assess the structural integrity of a component, it is important that the crack tip stress field and the critical criteria to define fracture for the material and the constraint condition considered, are well characterised. In this section, fracture toughness standards for ductile materials and the two parameter fracture mechanic defining constraint ahead of a defect are introduced.

1.3.1. Fracture toughness test standard

There are number of fracture toughness test standards available, such as ASTM 1820 [12], BS 7448-4 [6]and ESIS P2-92[13], to define the lower bound fracture toughness property of a material experimentally. These methodologies have been used in this work to perform fracture toughness tests. The requirements set by the test standards are defined in more detail in the methodology section of this report (section 2.2.1.)

1.3.2. Constraint effect:

1.3.2.1. General introduction

The Fracture Mechanics parameters K and J are valid for idealised conditions ahead of a crack tip, where the higher terms of the Williamson and Westergaard equations can be assumed to be negligible. These assumptions can be considered to be valid for linear elastic conditions for K; and when the plastic zone is very small compared to all other characteristic lengths and is embedded within and elastic field, where SSY conditions are prevalent for the J-integral.

In engineering components and for materials exhibiting low yield high toughness properties, SSY conditions are not always maintained. As the plastic zone increases and interacts with the free surfaces, SSY conditions are no longer prevalent ahead of the crack tip and the single fracture mechanic parameters can no longer characterise the crack tip stress conditions.

Understanding and quantifying constraint in components has been the subject of research for over three decades. This section will present the various accepted methodologies to quantify constraint and the effect of constraint on measured initiation toughness and Resistance curves.
1.3.2.2. **T stress**

If one considers a crack tip stress field which has a small plastic zone compared to the remaining ligament, one can say that the yield zone is surrounded by the dominant elastic zone and the problem can be analyzed as a modified boundary layer problem described by the stress intensity factor. The crack tip stress solution for such a situation can be described using Williams series (equation(12))[14].

\[
\sigma_y = A_y(\phi)r^{-\frac{1}{2}} + B_y(\phi) + C_y(\phi)r^{\frac{1}{2}}
\]  

(12)

For fracture mechanics analysis’s in which only small scale yielding is considered, only the first term is used as the effect of the following terms are considered insignificant. Larson and Clarsson [15] showed, using elastic perfectly plastic finite element analysis, that for some geometry and loading conditions, the second term of the expansion can have significant effect on the shape and size of the plastic zone ahead of the crack tip. The effect of the second term on the crack tip stress field was quantified by Rice using equation [14].

\[
\begin{bmatrix}
\sigma_{11} & \sigma_{12} \\
\sigma_{21} & \sigma_{22}
\end{bmatrix} = \frac{K}{\sqrt{2\pi\alpha}} \begin{bmatrix}
f_{11}(\phi) & f_{12}(\phi) \\
f_{21}(\phi) & f_{22}(\phi)
\end{bmatrix} + \begin{bmatrix}
T & 0 \\
0 & 0
\end{bmatrix}
\]

(13)

The T-stress, which can be considered as a geometry and load dependant uniform elastic stress acting parallel to the crack plane, can be used as a correcting factor for the deviation from the small scale yielding observed even at low loads in experimental results. It is directly proportional to the magnitude of the load applied, the geometry of the component, the size of the defect in the component and the loading condition applied to the component.

Leevers and Radon proposed a non-dimensional parameter \( \beta \), relating T to the applied load in terms of K and the crack depth.

\[
\beta = \frac{T\sqrt{\pi\alpha}}{K}
\]

(14)
As the biaxiality parameter $\beta$ is independent of the load applied, specimen constraint can be described for a specific specimen geometry and crack depth. Compendia of biaxiality ratios defining the constraint of a range of geometries are available to engineers [16].

Betegon and Hanckock [17] used the biaxiality ratio to investigate the effect of elastic $T$ stress on $J$ dominance large scale yielding leading to fully yielded condition. They concluded that $T$ stress can be used in a large scale yielding in conjunction with $J$ to describe the stress field ahead of the crack tip. Fields with positive $\beta$ exhibit $J$ dominance and can be described by HRR and $J$ as amplitude whilst loss of $J$ dominance is characteristic of negative $T$ stress, which leads to a $J$-$T$ two parameter description of the stress field (Figure 3).

![Figure 3: Tangential stress field ahead of a crack in a boundary layer formation at different levels of $T$-stresses](image)

Specimens, such as centre cracked specimens, that exhibit negative $T$ stress ahead of the crack tip, have a more rapid reduction of the highly tri-axial stresses ahead of the crack tip which are typical of the HRR. This has an important effect on the ductile fracture as high stress levels are required for void nucleation. Rice’s $T$ stress correction was the first attempt at correcting fracture analysis for constraint and understanding the effect on ductile fracture.
1.3.2.3. J-Q Two parameter fracture mechanics

O’Dowd and Shih propose to define constraint conditions ahead of crack tip through an elastic plastic stress triaxiality parameter Q.

They considered a Ramberg-Osgood material, exhibiting an a stress field ahead of the crack tip that could be described using the asymptotic stress formulation derived by Hutchinson, (equation (15)). In the derivation of the HRR solution, it is assumed that the higher order solutions are irrelevant. O’Dowd and Shih propose a similar methodology to what was proposed by Rice for the derivation of T but using Hutchinson’s asymptotic derivation of the stress field ahead of the crack tip. The Q parameter represents the sum of all the second and higher order terms ((16)),

\[
\frac{\sigma}{\sigma_0} = \left( \frac{J}{\alpha \varepsilon_0 \sigma_0 I_n r} \right)^{1/(n+1)} \tilde{\sigma}_y (\theta; n) + Q \left( \frac{r}{J/\sigma_0} \right) \tilde{\sigma}_y (\theta; n)
\]  (15)

When distances are normalized by J/σ0, the Q parameter characterises near crack tip stress distribution and the maximum stress, whilst J sets the size of the process zone.

The value of Q in a component is defined as the deviation of the stress field from the SSY solution at a normalised distance ahead of the crack tip, and thus can also be defined as in equation(16).

\[
\sigma_y = (\sigma_y)_{SSY} + Q \sigma_y \delta_y
\]  (16)

1.3.2.4. Effect of constraint on J-Resistance curves

Constraint has an important influence over the fracture mechanics parameters such as Jc, Kc, Jr, etc[18]. In a similar manner, the shape of the Jr curves is also found to be dependent on the mode of loading and constraint [19].
Joyce and Link[20] developed an interpolation scheme which allows an engineer to correct the J-Resistance curve for its level of constraint using the Q parameter [21]. This means that a highly constrained experimental J-Resistance curve can be corrected for different levels of constraint. Given that it is possible to know the level of constraint of the structural component analysed, it is possible with this methodology to have a better prediction of the fracture resistance of the material. A good example for the benefits of the constraint corrected J-Resistance curve can be seen in Figure 4. From experimental results, a lower bound plane strain J-Resistance curve is found and then corrected J-Resistance curves for different amount of constraint loss are extrapolated. If one considers the case of a defect in a structure being subject to a J=600kJ/m$^2$, an analysis using plane strain conditions or results from a highly constrained laboratory specimen such as a side-grooved CT specimen [6], would exhibit a high amount of tearing; in this case 6.4 mm. When applying the true component constraint, let’s say a Q of -0.5 or -0.75, the amount of predicted crack would be highly reduced.

![Constraint corrected J-Resistance curves](image)

**Figure 4:** Constraint corrected J-Resistance curves. Experimental results obtained from deep crack SE(B) specimens of HY80 tested at room temperature

Q changes with the increase in load. This means that Q can be used as a quantitative estimate of the difference in J-Resistance curves due to constraint. Neimitz et al [18] confirmed the validity of Q to quantitatively predict the J-Resistance curve.
New insights into the competition between ductile tearing and plastic collapse

A novel methodology used to describe constraint and correct J-Resistance curves for constraint is the J-A2 methodology[22]. This methodology is based on the asymptotic expansion of the stress field ahead of the crack tip in a power-law nonlinear material. The authors show that J and their A2 parameter describe the four first terms of the expansion for high hardening capacity material (n<3) and the three first terms for materials exhibiting lower hardening characteristics (n>3). A2 is currently gaining importance as a constraint parameter. Q can be related to A2 at a distance of $r=2J/\sigma_0$ and $\theta=0[23]$.

1.3.2.5. Constraint effect on initiation toughness

The effect of constraint on initiation toughness for ductile and brittle materials has been greatly explored in the literature.

1.3.2.5.i. Brittle fracture:

In brittle fracture, a marked increase in toughness with reduction in crack tip constraint has been shown in low grade mild steel at -50 degrees C by Sumpter and co-workers[24, 25], in high strength weld materials and carbon steels [26]. An example of the effect of constraint is presented in Figure 5 as a function of T-stress and as a function of Q in Figure 6.

![Figure 5: Critical value of J as a function of T/\(\sigma_y\) for a three point bend and CCT specimen for a mild steel at -50C[24]](image)

![Figure 6: Critical value of J as a function of Q for a three point bend and CCT specimen for a mild steel at -50C[24]](image)

1.3.2.5.ii. Ductile tearing:
The effect of ductile tearing was investigated by Hancock et al [27] on a pressure vessel steel and Sherry et al [28] on an A533B-1 steel plate. Results obtained by Hancock [27] show initiation toughness constraint dependence when initiation toughness is defined as 0.2 or 0.4mm of crack growth. It is also noted that initiation toughness defined as 0mm crack growth, the point at which the Resistance curve deviates from the blunting line, showed little constraint dependence. It is also important to note that the larger amount of crack growth defined at initiation leads to a larger measured constraint effect on the crack tip. This is due to the increase in steepness in the J-Resistance curve with increase in toughness.

\[ \text{Figure 7: Ductile toughness of an A710 steel at various crack extension as a function of T-stress [27]} \]

1.3.2.6. Limitations:
T stress is a useful parameter as it is easy to determine in structures as it only requires an elastic analysis. As has been shown numerically (FEA) by Betegon and Hancock it can provide some indication of the stress field beyond the elastic and small-scale yielding[17]. Nevertheless, T stress remains an elastic parameter is only proportional to Q for a limited range of constraint conditions, as shown in Figure 8. It is important to note that in some cases, such as shallow cracked Centre Cracked Tension specimen under biaxial loading; the T-stress can provide non-conservative estimations of constraint at high Lr[29].

39
New insights into the competition between ductile tearing and plastic collapse

Figure 8: Relationship between $Q$ and $T$ in small scale yielding conditions[29]

In the same year as Hancock showed the $T$ influence on elastic-plastic cases, O’Dowd and Shih [21, 30] derived the triaxiality parameter $Q$ characterizing elastic-plastic crack tip fields.

Sharma et al and Nikishkov et al [31] and Joyce and Link [32] showed that the initial definition of $Q$ was distance dependant under large loading, i.e. the value of $Q$ varied with increasing distance ahead of the crack tip (Figure 9). This is due to the impingement of the boundary of the specimen on the crack tip stress field. Beyond the $Q$-annulus, which is the normalised distance over which $Q$ is constant, $Q$ is no longer a transferable parameter.

Figure 9: $Q$ vs. distance ahead of the crack tip for SEN(B) specimens, Joyce & Link [32], analysing results from O’Dowd & Shi [21, 30]
1.4. **Plasticity in metals**

Metals deformation can be defined by two phases of deformation, 1) elastic deformation, where the material will undergo small deformations and return to their original configuration, and 2) plastic deformation, where the material undergoes permanent deformation. This section will provide a simple definition of plasticity, introduction of the yield criteria used in engineering assessments and finally discuss the concept of plastic collapse.

1.4.1. **Yield Criteria**

Yield criteria describe the condition of stress that separates recoverable elastic deformation from non-recoverable plastic deformation. For uni-axial stress state, the yield criterion is simply expressed as the point where the applied stress is equal to the yield stress. For three dimensional stress fields, the yield point is proportional to each component of the stress field.

1.4.1.1. **Tresca Yield criterion**

The Tresca yield criterion is based on the assumption that material yielding occurs as a result from pure shear. The Tresca criterion states that yielding occurs if the maximum shear stress, defined in equation (17) reaches a critical value.

\[
|\sigma_2 - \sigma_3| = 2\tau_c
\]

\[
|\sigma_1 - \sigma_3| = 2\tau_c
\]

\[
|\sigma_1 - \sigma_2| = 2\tau_c
\]  \hspace{1cm} (17)

The critical shear stress can be defined from a tensile test, which is known to yield under the conditions defined in equation (18). This leads to the critical yield criterion being defined as half the yield strength (equation (19))
New insights into the competition between ductile tearing and plastic collapse

\[ \sigma_1 = \sigma_y \]  \hspace{1cm} (18)  
\[ \sigma_2 = \sigma_3 = 0 \]  
\[ \tau_c = \frac{\sigma_y}{2} \]  \hspace{1cm} (19)  

1.4.1.2. Von Mises Yield criterion

The Von Mises criterion is derived from the strain density formulation for linear elastic materials defined in equation (20). Following Hook's law, the strain energy density can be defined following equation (21), with \( \mu \) the shear stress and \( K \) the bulk modulus. Von Mises Yield criteria assume that yield is independent on dilatation deformation.

\[ W = \frac{1}{2} \sigma_{ij} e_{ij} \]  \hspace{1cm} (20)  
\[ W = \frac{1}{12\mu} \left[ (\sigma_1 - \sigma_2)^2 + (\sigma_1 - \sigma_3)^2 + (\sigma_2 - \sigma_3)^2 \right] \]  \hspace{1cm} (21)  

The critical value of strain energy can be obtained from a tensile test following equation (22). This leads to the critical Von Mises yield criterion to be defined by equation (23).

\[ W_c = \frac{1}{\sigma_y} \sigma_y^2 \]  \hspace{1cm} (22)  
\[ \sigma_{y-VMS} = \frac{1}{\sqrt{2}} \left[ (\sigma_1 - \sigma_2)^2 + (\sigma_1 - \sigma_3)^2 + (\sigma_2 - \sigma_3)^2 \right] \]  \hspace{1cm} (23)  

According to Von Mises and Tresca plasticity criteria, no plastic deformation will occur due to hydrostatic stresses and a yield surface (Figure 10) can be defined for both failure criteria.
New insights into the competition between ductile tearing and plastic collapse

1.4.1.3. Plastic deformation ahead of a sharp defect

The crack tip stress field in an elastic material under a range of constraint conditions can be defined using equation (24)

\[
\begin{align*}
\sigma_1 &= \frac{K_I}{\sqrt{2\pi r}} \cos \left(\frac{\theta}{2}\right) [1 + \sin \theta] + T \\
\sigma_2 &= \frac{K_I}{\sqrt{2\pi r}} \cos \left(\frac{\theta}{2}\right) [1 - \sin \theta] \\
\sigma_3 &= 0 \\
\end{align*}
\] (24)

Assuming the Von Mises yield criterion, and T-stress = 0, the yield surface can be defined following equation (25) for plane stress condition and equation (26) for plane strain conditions.

\[
r_y(\theta) = \frac{1}{4\pi} \left(\frac{K_I}{\sigma_y}\right)^2 \left[1 + \cos \theta + \frac{3}{2} \sin \theta^2\right]
\] (25)

Figure 10: The Von Mises and Tresca yield surface in three dimensional stress space
New insights into the competition between ductile tearing and plastic collapse

\[ r_y(\theta) = \frac{1}{4\pi} \left( \frac{K_i}{\sigma_y} \right)^2 \left[ (1 - 2\nu)^2(1 + \cos \theta) + \frac{3}{2} \sin \theta^2 \right] \]  

(26)

For plane strain conditions, the effect of T-stress on the crack tip stress field can be defined using equation

\[ r_y(\theta) = \frac{1}{2\pi} \left( \frac{K_i}{\sigma_y} \right)^2 \left[ \frac{1}{c} (-b \times T) - \sqrt{b^2T^2 - 2ac} \right]^2 \]

\[ a = \frac{3}{2} \sin \theta^2 + V(\cos \theta + 1) \]

\[ b = \cos \left( \frac{\theta}{2} \right) \left[ -3 \left( \cos \left( \frac{\theta}{2} \right)^2 - \cos \theta^2 \right) + V \right] \]

\[ c = 0.5(3 + V) \ast T^2 - 2 \]

\[ V = (1 - 2\nu)^2 \]

The conditions at the surface of the specimen can be assumed to be plane stress, as the stresses can redistribute in the \( \sigma_3 \) direction leading to negligible stresses in the 3 direction. The centre of the specimen can be assumed to be plane strain. This leads to large differences in the plastic zone size from the centre of the specimen to the outside face of the specimen, as shown in Figure 11.

**Figure 11:** Schematic representation of the plastic zone at the edge (plane stress and the centre of a material containing a crack
New insights into the competition between ductile tearing and plastic collapse

A reduction in crack tip T-stress leads to large increase in the plastic zone size, as shown in Figure 12.

![Figure 12: Effect of constraint, defined using the elastic T-Stress, on the plastic zone size](image)

1.4.2. Plastic collapse:

1.4.2.1. Introduction:
Gross deformation in metals can lead to a plastic engineering failure criterion referred to as plastic collapse. This is of particular importance for piping structures in which exceptional strain conditions can occur. Limit load analysis and plastic collapse solutions provide the plastic limiting factor. This can be calculated using a range of methodologies. In all the methodologies described below, the Tresca or the Von Mises yield criteria can be used. The use of the Tresca yield criteria tend to yield more conservative, yet less accurate estimations of the plastic deformation limiting factor.

1.4.2.2. Material definition
If we consider the case of a loaded cracked structure exhibiting plastic deformation, plasticity will develop ahead of the crack tip and spread through the specimen with increasing load.

The plastic response of materials can be described in two distinct manners [33]. The first one is elastic-perfectly-plastic response in which the material exhibits an elastic region and then a flat or perfectly plastic region as shown in Figure 13 a). Figure 13 b) shows a
New insights into the competition between ductile tearing and plastic collapse

hypothesised elastic-perfectly-plastic structural response in loading. The second one is a material showing strain hardening capability with its stress-strain curve shown in Figure 13 c). The material exhibits an increase in its load capacity as plasticity develop and Figure 13 d) shows the difference between the two cases.

![Figure 13: material models and structural responses [33]](image)

Conventionally, limit load analysis are performed using an elastic perfectly plastic material deformation definition. This provides a conservative estimate of the plastic deformation ahead of a defect and the maximum load sustainable by the structure analysed.

**1.4.2.3. Upper Bound and Lower Bound Limit Theorem:**
The upper and lower bound theorem are derived from fundamental principles of plasticity by bounding the exact collapse load, through the derivation of the collapse load for a material exhibiting elastic-perfectly-plastic deformation characteristics (drucker et al 1952). The lower bound theorem, which leads to a conservative estimation of the limit load, states that the collapse load occurs when, analysing a static stress field, the stresses are in equilibrium and do not exceed yield criteria anywhere. The lower bound theorem therefore defines the limiting condition as the state of first yield. The upper bound theorem defines the limiting condition as the point where, when the material properties follow the plastic flow rule, the increment of work done by the external load in an increment of displacement is equal to the work done by the internal stresses. The upper bound theorem therefore
defines the limiting condition as the point where no increase in load can be accommodated by elastic deformation of the material.

Large compendia of lower bound limit load solutions for specific geometry can be found in the literature [34].

1.4.2.4. Local and Global Plastic collapse

R6 [35] defines two sets of plastic deformation limiting conditions, the “local” and the “global” limit loads. The “local” limit load, which is defined as the yielding of the ligament analysed. In the case of a cracked structure, the local limiting solution will be attained when plasticity has developed throughout the un-cracked ligament. The “global” limit load, is defined as the overall collapse of the component analysed, hence the maximum load carrying capability before continuous plastic flow occurs without further increase in load [36].

Figure 14 is a schematic representation of a section through a pipe wall containing a semi-elliptical defect. The filled area represents ‘local’ collapse region over which failure by gross deformation occurs. The striped and filled area represents the ‘global’ collapse region over which failure by gross plastic deformation occurs.
Figure 14: Schematic representation of a pipe wall with a defect. The region in grey represents gross deformation of the ligament ahead of the defect and the striped grey represents the global yielding of the pipe.

Limit load solutions of ‘standard’ geometries obtained from elastic-perfectly plastic Finite Element (FE) can be found in handbook solutions [37].

1.4.2.5. Engineering derivation of Limit Load

Limit load solution can be obtained using three methodologies:

1.4.2.5.i. Handbook solution:

Limit loads for specific geometries and crack depths have been defined as a function of the yield strength of the material analysed using the lower bound theorem or the global limit load of elastic-perfectly plastic materials.
1.4.2.5.ii.  

*Graphical derivation:*

The most used graphical methodologies are the twice elastic slope criterion and the tangent intersection criterion shown in Figure 15 and the plastic work criterion shown in Figure 16 [33]. The twice elastic slope criterion and the tangent intersection criterion shown in Figure 15 are based on the load-deformation curves that characterize the inelastic response of the structural component. The TES is the intersection between the slope of gradient twice the elastic component and the curve. The TI criterion is derived as the intersection between the tangent to the elastic response and the tangent of the plastic response of the component. TES and TI are global plastic collapse definitions and the choice of characterizing parameter influences the results obtained. ASME [38] defines TES as their preferred methodology to define plastic collapse.

![Graphical representation of TES and TI criteria](image)

*Figure 15: a) twice elastic zone criterion, b) Tangent intersection criterion [33]*

The Plastic Work criterion is based on a global characterization of the plastic response of the component; it is based on the curvature of the load vs. plastic work response [39]. Lambda represents global loads applied to a structure applied in proportional loading analysis. PWC is defined as the intersection between the tangent to the point where excessive plasticity has occurred and the y axis.
New insights into the competition between ductile tearing and plastic collapse

The graphical methodologies described above are particularly suited for experimentally or analytically derived limit load solutions. The Twice Elastic Slope and Tangent Intersection methodologies are the more commonly used, but can cause issues when multiple loads are acting on a structure. The plastic work criterion provides a less common, yet more versatile methodology to define the limiting condition.

1.4.2.6. Finite Element Analysis:
A flawed component can be analysed using small displacement finite element method with an elastic perfectly plastic material model. The analysis should be performed with a monotonically increasing load, with the maximum load attained defining the “global” limit load solution. As the limit load is proportional to the yield strength, the limit load obtained can be extrapolated for a range of yield strength solutions. The maximum load can be defined as the load applied during the last converging step. Specific formulations, such as the RIKS analysis model, allows for the maximum load to be computed, leading to more accurate definition of the limit load.
1.4.3. Experimental Measurement of Plasticity using Digital Image Correlation

Digital image correlation methodology is a full-field surface strain mapping technique, which allows experimental measurement of the strain distribution of the surface of a specimen. The methodology is based on the correlation of displacement of surface features in a series of images. In order to obtain a detailed strain map, each image is sub-divided into multiple interrogation windows, each containing a set amount of features. The features in each interrogation window in image $n$ are then compared to the features in the same interrogation window in image $n+1$. The analysis of displacement of features provides a displacement vector, which represents the average of the displacements of features within this window. Figure 17 describes the methodology used to define the displacement vectors.

![Digital Image Correlation of features](image)

*Figure 17: Digital Image Correlation of features*[40]*
New insights into the competition between ductile tearing and plastic collapse

Displacement accuracy is dependent on the interrogation window size. Table 1 shows that an increase in interrogation window size will lead to an increase in the accuracy of the displacement vector calculated.

Table 1: accuracy of displacement vector as a function of interrogation window size, using the DAVIS DIC software

<table>
<thead>
<tr>
<th>Size of Interrogation Window (pixels)</th>
<th>Accuracy of Calculated Vector</th>
</tr>
</thead>
<tbody>
<tr>
<td>128 x 128</td>
<td>0.01 to 0.03 pixels</td>
</tr>
<tr>
<td>64 x 64</td>
<td>0.02 to 0.05 pixels</td>
</tr>
<tr>
<td>32 x 32</td>
<td>0.05 to 0.2 pixels</td>
</tr>
<tr>
<td>16 x 16</td>
<td>Larger than 0.1 to 0.3 pixels</td>
</tr>
</tbody>
</table>

In order to obtain a good strain map resolution, the sub-regions must be as small as possible, yet large enough to contain a sufficient number of features to be able to define a pattern unambiguously. Following Numquits sampling theorem and taking into account sampling noise, in order to obtain good quality correlation, each feature should be no smaller than 3*3 pixels. Hence size of the area that can be analysed is directly proportional to the analysed feature’s size.

There is a competition between the accuracy of the displacement vector, and the special resolution of the strain map. In order to optimise the accuracy and resolution, an over-lap of interrogation windows can be created. Sub-regions of 32*32 with an overlap of 20% provide a good compromise between special resolution and displacement accuracy in most cases[40].
1.5. **Ductile fracture**

Most modern engineering materials exhibit large plastic deformation and ductile failure at the temperatures and loading conditions experienced during service. This section investigates the mechanisms behind ductile fracture, the models used to define fracture by ductile tearing and finally the effect of crack tip constraint on ductile initiation.

1.5.1. **Definition:**
Global crack tip behaviour due to ductile crack growth can be described as three stages, (i) plastic crack tip blunting due to high stress concentration and initiation, (ii) stable crack propagation and (iii) unstable crack propagation [41]. The ductile initiation process can be described in three stages, the initiation of voids in the material, void growth and void coalescence leading to crack propagation.

1.5.2. **Void initiation**
During the process of crack blunting, voids initiate at inclusions or second phase particles in the area directly ahead of the crack tip, located either at the grain boundary or within the grain [42] [43]. Void nucleation occurs through de-cohesion of the matrix/particle interface as shown in Figure 20 and Figure 18, or fracture of the particles themselves [44]. The void nucleation process is influenced by the size, shape and orientation of the particle, by the strength of the particle-matrix bond and by the distribution of the particles within the material matrix. The hard particle tends to de-cohere from the material matrix when in a soft material. The soft matrix tends to prevent the stress in the particle to reach the critical values required for the particle to crack. In harder material matrix, the stress can concentrate in the hard particle, leading to fracture of the particle before de-bonding from the material matrix.

Void nucleation occurs preferentially at larger particles within the particle distribution in steels [45, 46]. Materials with a more uniform distribution of smaller particles tend to require a larger strain to initiate voids, which has significant effects on the material measured toughness. Void initiation is hard to measure as there is no overall effect on the global behaviour of the material and due to the discontinuous nature of the nucleation process.
New insights into the competition between ductile tearing and plastic collapse

1.5.3. Void growth
After nucleation, the voids will expand with increase in load. Void growth occurs through the plastic flow around the initial void. The rate and shape of void growth is controlled by a combination of the stress triaxiality and the plastic strain ahead of the crack and by the shape of the inclusion. The numerical models describing the growth of voids are discussed in more details in the Local Approach methods section (section 1.6.).

1.5.4. Void coalescence
Increased plastic deformation leads to the increase in void size until a critical value is reached at which voids coalesce and join the crack tip. Void coalescence may occur through two mechanisms,

Activated of a single population of particles during the fracture process (i.e. Other particles are bonded too strongly to the material to nucleate). Coalescence will therefore occur when the voids will grow up to the point where they interact, through the buckling of inter-void material matrix. This process is controlled by local shear bands.

Void nucleate around larger particles first, as they require smaller stains to de-cohere from the material matrix. An increase in strain with increase in load can lead to the activation of smaller particles, such as carbides. Void coalescence will then occur through the interaction of large voids and the growth of micro-voids.

Figure 18: SEM fractography of a stainless steel
Dimpled fracture with hard second phase particles at the centre of voids
New insights into the competition between ductile tearing and plastic collapse

Figure 19: Void nucleation of a 304 stainless steel, a) exhibiting coalescence of multiple population of particles, and b) single population of particles.

The ductile fracture process occurs in the highly triaxial stress field ahead of the crack tip termed the ‘fracture process zone’ [47], which is typically in the region of a few hundred micrometers ahead of the crack tip.

The crack propagation process can occur as a staged or continuous process. Materials exhibiting low initial void volume fraction, the blunted crack tip will interact with a first void ahead of the defect. Once coalescence has occurred, the new crack tip will interact with the following void (Tvergaard and Niordson (2008)). In materials exhibiting higher initial void volume faction, the crack tip stress field will interact with a number of particles in a specific volume ahead of the defect, which will lead to void interaction (Tvergaard and Niordson (2008)). The process is a continuous one as new voids initiate as others coalesce.

Figure 20: The process of plastic damage and ductile failure [48]

Figure 21 The process zone in a) the un-cracked ligament and b) the crack propagation plane [48]
1.6. **Local Approach Methodology**

In order to adequately model the ductile fracture process, the three stages of ductile tearing presented above should be taken into account. Mechanical models for all three stages of ductile tearing exist; however, these models require large experimental data to define the material nucleation, void growth and nucleation behaviour.

For materials such as reactor pressure vessels, which have soft ductile material matrix with hard particle inclusion, the ductile process is dominated by void growth and coalescence processes. Furthermore, coalescence is a punctual process which can be associated with the end of the void growth process. The local approach methodologies described in this section and used in this study, are based on the assumption that the ductile fracture processes in the material analysed occur at the same local conditions ahead of the crack, and that this critical condition can be described by the void growth process alone.

Furthermore, two types of models exist, the coupled and the de-coupled approaches. The coupled approaches modify the material behaviour, accounting for the softening of the material matrix due to void growth and coalescence. The most well known is the Gurson model [49], which was modified by Tevergaard [50] and then by Tevergaard and Needleman [51]. The coupled methods often require the definition of large amounts of parameters in order to accurately define fracture, for example, the Gurson model requires the definition of over ten parameters to be calibrated for a single material [52]. This makes the Gurson model complicated to use and unsuitable for industrial application.

The de-coupled methodologies use the conventional definition of material behaviour, which assumes no dilatation work (i.e. no change in volume). The most successful decoupled models are based on the relationship between the stress and strain state defined according to equation (28)[52]. Such criteria have been defined by McClintock [53], Rice and Tracey [54] or Leroy et al [55]. Each method defines a different relationship between stress and strain at failure.
New insights into the competition between ductile tearing and plastic collapse

\[ \int_0^{\varepsilon_f} f(\text{stress state}) \, d\varepsilon \]  \hspace{1cm} (28)

The stress triaxiality has been shown in many studies to play an important role in the void growth process. In this study, the Rice and Tracey parameter, which is a function of stress triaxiality and plastic strain, was chosen as one of the failure criteria used to investigate the failure by ductile fracture.

1.6.1. Defining the fracture process zone:
Due to the large stress and strain gradient ahead of a sharp defect, a critical distance over which the critical damage occurs is required. This is defined as the fracture process zone [56]. Most de-coupled methodologies are sensitive to the definition of the fracture process zone. Never-the-less, no agreement exist on how to define it analytically or experimentally. Many researchers [57] argue that the distance should be proportional to the inter-particle spacing, which is typically between 100-200μm for stainless steels and structural steels [58-60]. On the other hand, Pineau & Lautridou argue that the distance itself is not important as long as it is kept constant in all analysis and that the probability of finding a single inclusion within the microstructure is non zero.

The size of the fracture process zone is important, as it defines the crack tip mesh required to define the local approach criteria. The Rice and Tracey local approach has been shown to be mesh dependent by Dutta et al [61].

1.6.2. Rice and Tracey Void growth models
The void growth and coalescence process in ductile tearing depends strongly on the stress triaxiality at the initiation site [43, 48, 62].

The Rice & Tracey model is based on the growth of a spherical void subjected to a triaxial stress field in an elastic perfectly-plastic material. The rate of void growth, defined as the rate of change of the void radius, was found to be proportional to the hydrostatic stress and plastic strain. Rice and Tracey defined two curve fits, an exponential curve fit that fits best the high triaxiality part of the void growth curve (equation (29)), and an exponential curve
New insights into the competition between ductile tearing and plastic collapse

fitted to sinh and cosh curves, which provided a better curve fit at low and medium stress triaxiality (equation (30)).

\[ GR&T = \int_0^{\varepsilon_f} 0.558 \times \sinh\left(\frac{3\sigma_m}{2\bar{\sigma}}\right) + 0.008 \times \nu \times \cosh\left(\frac{3\sigma_m}{2\bar{\sigma}}\right) \, d\varepsilon_p \quad (29) \]

\[ HR&T = \int_0^{\varepsilon_f} \frac{3\sigma_m}{\alpha e^{2\bar{\sigma}}} \, d\varepsilon_p \quad (30) \]

With \( \sigma_m \) the hydrostatic stress, \( \bar{\sigma} \) the Von-Mises equivalent stress, \( \varepsilon_p \) the plastic strain component, \( \varepsilon_f \) the strain at fracture, \( \nu \) the materials Poisson’s ratio and \( \alpha \) the fitting parameter, defined as 0.283 in the initial Rice and Tracey paper [63].

The definition of \( \alpha \) was shown to be proportional to the volume fracture of second phase particles[64]. If this value is not know, the original value of the \( \alpha \) parameter defined by Rice & Tracey (\( I\alpha=0.283 \)) or the value of \( \alpha \) modified by Huang [65] (\( \alpha=0.427 \)) can be used.

The Rice and Tracey parameter has been used successfully when assessing the failure of specimen and structures for a range of structural steels [48, 60, 66, 67]

1.6.3. Work of Fracture

The work of fracture criterion is based on the plastic work in the fracture process zone. Plastic strain work is conventionally defined using equation (31). As materials are assumed to be incompressible, the hydrostatic stress component of plastic strain work is usually defined as 0. Chaoudi states that for ductile fracture, the change in volume of the material is due to void growth, and hence can be defined using the Rice and Tracey definition for void growth (equation (32) and equation (33)).

\[ \delta W_D = \sigma_{eq} \delta \varepsilon^p_{eq} + \sigma_{hi} \delta \varepsilon^p_{ii} \quad (31) \]

\[ \delta \varepsilon^p_{ii} = \frac{\delta V}{V} = 3 \frac{\delta R}{R} \quad (32) \]
New insights into the competition between ductile tearing and plastic collapse

\[ W_f = \int_0^{\varepsilon_f} \left( 1 + 3 \alpha \frac{\sigma_m}{\bar{\sigma}} e^{\frac{3\sigma_m}{2\bar{\sigma}}} d\varepsilon_p \right) \]

(33)
New insights into the competition between ductile tearing and plastic collapse

1.7. Structural Integrity

Defect assessment procedures have been developed over the years to provide simple analytical tools to investigate the integrity of structures containing crack like flaws, in the absence of time dependant failure mechanisms such as creep and absence of environmental degradations, such as stress corrosion cracking or irradiation embrittlement. Modern assessments of structures take explicit account of possible presence of sharp defects in components, which may arise from fabrication. Such defects can be introduced during casting, welding or forming operations or in-service during the lifetime of the component.

1.7.1. The R6 Defect assessment procedure

R6 is a procedure that is used to perform structural integrity assessments of nuclear power plants. It has been developed in a consortium led by British Energy and including Rolls-Royce, Serco and TWI. It is widely used in the United Kingdom nuclear industry. The R6 Failure Assessment methodology is based on the interaction diagram analysis methodology. The principal failure criteria in fracture assessments, linear elastic fracture mechanics and plastic collapse, were defined by Dowling and Townley in 1975. These diagrams are useful for defining the limiting or failure conditions and for indicating the dominant loading or failure mechanism.

The R6 Failure assessment diagram is the interaction diagram for the two principal failure criteria of a component containing a sharp defect, linear elastic fracture mechanics and plastic collapse. The R6 procedure includes a range of provisions, allowing the modification of the conventional Failure Assessment Curve (FAC) to accounts for a range of failure mechanisms and crack tip behaviours such as ductile tearing [68], constraint effects [69, 70] and secondary stress ([71], [72], [73].

Many national structural assessment methodologies based on interaction diagrams exist, to note but a few:

- SINTAP procedure [74]
- BS9710
- JSME fitness for purpose code [75], [34]
New insights into the competition between ductile tearing and plastic collapse

1.7.2. **R6: Assessment of Structure containing defects**

1.7.2.1. **Failure Assessment Diagram**

The FAC was originally derived as the assessment of the competition between LEFM controlled fracture and plastic collapse. The proximity to fracture is defined by $K_r$, which is the ratio of the applied Stress Intensity Factor (SIF) over the SIF at fracture. The proximity to plastic collapse, defined as $S_r$ in the first editions of R6 and later changed to $L_r$ (which is the nomenclature which will be used in this work), is the ratio of the applied load over the limit load defined using the strip yield model introduced by Bilby et al [76] and used by Heald et al [77] to describe the failure of a specimen. The original FAD curve based on LEFM and Plastic collapse would not account for post yield deviation from LEFM, which lead to non-conservatism. Bloom & Malil (1982), and Bloom (1983b) showed that the FAD was dependent on material properties, component geometry, loading type and crack size.

In order to account for the deviation from LEFM with increase in plasticity, the $K_r$ parameter was redefined as a function of $J$ using the relationship between $K$ and $J$ defined in equation (34).

$$K_r = \frac{\sqrt{EJ_{0.2BL}}}{(1 - v^2)} \quad (34)$$

1.7.2.1.i. **Option 2 FAC: Reference stress based methodology**

A reference stress based methodology was developed by Kumar et al [78] and later refined by Ainsworth [79], which considers a Ramberg-Osgood material which stress-strain curve can be defined using equation (35). For these materials, the J-integral can be defined using equation (36), where $\alpha$, $\varepsilon$, $\sigma$ and $n$ are material constants defining the stress-strain curve, $c$ is characteristic dimension, $F_y$ is a normalising load (limit Load defined using the methodologies defined in section 1.4.2.), $h_1$ is a non-dimensional function of geometry, crack size, type of loading and $n$. The stress distribution is directly proportional to the Load F and the strain distribution is proportional to $F^n$.

$$\varepsilon = \alpha \varepsilon_y \left(\frac{\sigma}{\sigma_y}\right)^n \quad (35)$$
New insights into the competition between ductile tearing and plastic collapse

\[ J = \alpha \sigma_y \varepsilon_y c h_1 \left( \frac{F}{F_y} \right)^{n+1} \quad (36) \]

It was shown by Ainsworth that \( h_1 \) could be defined for elastic conditions (equation (37)), with \( n=1 \) (providing a conservative estimation of \( h_1 \)), and \( \sigma_y / \alpha \varepsilon_y = E \).

\[ h_1 = \frac{E f_e}{\sigma_y} \quad (37) \]

with

\[ \sigma_{\text{ref}} = \frac{F \sigma_y}{F_y} \quad (38) \]

This leads to a \( K_r \) definition which is conservative, and independent of components defined in equation (40). This means that equation (40) is not limited to materials which follow the behaviour defined in equation (35). Milne et al [80] introduced a correction for small scale yielding conditions, leading to the definition of the Option 2 FAD in the R6 procedure (equation (42)).

\[ \frac{J}{J_e} = \frac{E \varepsilon_{\text{ref}}}{\sigma_{\text{ref}}} \quad (39) \]

\[ K_r = \left( \frac{J}{J_e} \right)^{1/2} = \left[ \frac{E \varepsilon_{\text{ref}}}{\sigma_{\text{ref}}} + \left( \frac{1}{2} \right) \left( \frac{\sigma_{\text{ref}}}{\sigma_y} \right)^2 \right]^{1/2} \quad (40) \]

\[ L_r = \frac{F}{F_y} \quad (41) \]

\[ f_2(L_r) = \left[ \frac{E \varepsilon_{\text{ref}}}{L_r \sigma_y} + \frac{L_r^3 \sigma_y}{2E \varepsilon_{\text{ref}}} \right]^{1/2} \quad (42) \]
The accuracy of the reference stress method was investigated by Ainsworth [79] and Miller and Ainsworth [81], by comparing J estimations obtained through the reference stress method with numerically obtained J results. Reference stress methodology was found to be mainly conservative, with a typical conservatism of $\approx 5\%$. There are a few notable exceptions, for example:

- in materials with low hardening exponents ($2 < n < 3$)
- small defects under tension
- when values of $L_r$ are larger than unity.

The reference stress approach has also been validated through direct experimental validation of the FAD approach [80].

In order to apply the Option 2 FAD methodology, the following approach should be taken:

- Evaluate $K_r$ from equation (40), which requires the knowledge of $K$, which can be obtained from $K$ solution handbooks or analytically, and the knowledge of $K_{mat}$, which can be obtained experimentally.
- Evaluate $L_r$ from equation (41). This requires the knowledge of the material yield stress, which can be obtained experimentally from a tensile test, and evaluate the plastic collapse solution which can be obtained from handbooks or analytically following the methodology described in section 1.4.2.
- Evaluate the Failure Assessment Curve, which requires the knowledge of the stress-strain material behaviour, which can easily be obtained experimentally through a tensile test at the appropriate temperature of interest.
- Plot the loading point ($L_r, K_r$) on the FAD. An example of the FAD is presented in Figure 22.

1.7.2.1.ii.  Option 1 FAD: Simplified FAC

The reference stress approach has been used by Milne et al. [80] to derive Failure Assessment Curves from stress-strain data for a range of austenitic and ferritic steels. The curves were found to be fairly insensitive to material behaviour, thus making it possible to select an empirical fit to the curve (equation (43)) biased towards the lower bound. This
New insights into the competition between ductile tearing and plastic collapse

has been defined as the option 1 FAD in the latest R6 revision [80]. This equation is independent of material, geometry, type of loading and crack size, making it the ideal tool for engineers.

\[ f_1(L_r) = [1 + 0.5L_r^2]^{0.5}[0.3 + 0.7e^{-0.645}] \]  \hspace{1cm} (43)

In order to apply the Option 1 FAD methodology, the following approach should be taken:

- Evaluate \( K_r \) from equation (40), which requires the knowledge of \( K \), which can be obtained from \( K \) solution handbooks or analytically, and the knowledge of \( K_{mat} \), which can be obtained experimentally.
- Evaluate \( L_r \) from equation (41). This requires the knowledge of the material yield stress, which can be obtained experimentally from a tensile test, and evaluate the plastic collapse solution which can be obtained from handbooks or analytically following the methodology described in section 1.4.2.
- Plot the loading point \( (L_r,K_r) \) on the FAD, with the FAC defined using equation (43).

1.7.2.1.iii. Option 3 FAD: Material, geometry, loading and crack size dependent FAC.

Following the increase in computational power available to engineers and the increased capacity and simplicity of FEA software, direct investigation of the \( J_e \) and \( J \) can be done. The option 3 Failure Assessment curve is based on the relationship between elastic \( J \) integral and the elastic-plastic \( J \)-integral (equation (44))

\[ f_3(L_r) = \left(\frac{J_e}{J}\right)^\frac{1}{2} \]  \hspace{1cm} (44)

In order to apply the Option 3 FAD methodology, the following approach should be taken:

- Define the FAC using equation (44). This requires the definition of \( J_e \) and \( Jep \) for a specific geometry, material, loading condition, load, etc. This can be obtained analytically using commercially available FEA software.
New insights into the competition between ductile tearing and plastic collapse

- Evaluate $L_r$ from equation (41). This requires the knowledge of the material yield stress, which can be obtained experimentally from a tensile test, and evaluate the plastic collapse solution which can be obtained from handbooks or analytically following the methodology described in section 1.4.2.
- Plot the loading point $(L_r, K_r)$ on the FAD, with the FAC defined using equation (80).

### 1.7.3. Analysis of Margins

The major advantage of the graphical, FAD, approach is the simple analysis of margins on various inputs to the assessments of structures. As $K_r$ and $L_r$ are directly proportional to the load applied to a cracked structure a loading line can be produced readily. For constraint independent fracture toughness assessments of structures, the loading line is a straight line going through the origin. The failure load for a specific geometry and crack length can be defined as the intersection between the loading line and the FAC (Figure 22). The reserve factor on load can then be calculated using equation (45). This procedure is valid for components subjected to multiple mechanical loads. Reserve factors can be evaluated for each loading condition as well as the overall reserve factor, if the loads are assumed to be proportional.

![Figure 22: Option 1 failure assessment diagram and loading line. Load A represents an analysed load and B represents the point at fracture](image)
New insights into the competition between ductile tearing and plastic collapse

\[
F^t = \frac{\text{Failure Load for specific crack depth and geometry}}{\text{Applied load for assessed in – service conditions}}
\]  \hspace{1cm} (45)

Reserve factors can also be defined readily for defect size using equation (46), for fracture toughness using equation (47) and for material properties such as yield strength using equation (48).

\[
F^a = \frac{\text{limiting defect size for a specific geometry and load}}{\text{Defect size of interest}}
\]  \hspace{1cm} (46)

\[
F^a = \frac{\text{limiting fracture toughness for a specific geometry, crack length and load}}{\text{fracture toughness of material}}
\]  \hspace{1cm} (47)

\[
F^a = \frac{\text{limiting yield strength for specific geometry, crack length, load}}{\text{Yield strength of the material}}
\]  \hspace{1cm} (48)

The FAD allows for a simple graphical interpretation of a range of reserve factors. A more detailed discussion on the methodologies to define reserve factors using the R6 FAD can be found in [82].

1.7.4. Constraint modification

Two methodologies exist within the R6 procedure to account for constraint effects on the fracture toughness of the material and structure analysed, based on two parameter fracture mechanics defined in section 1.3.2.3.

For inclusion in the FAD methods, the T-stress is defined as a dimensionless function of the yield strength and Lr, defined by Ainsworth and O’Dowd. The T-stress is proportional to the load applied; hence a unique \( \beta T \) solution exists for a specific specimen, crack length and loading condition applied.

The first methodology retains the definition of \( K_r \), and modifies the FAC to account for the change in initiation toughness (MacLennan and Hancock, 1995). The second methodology
retains the definition of the FAC and modifies the loading point by defining a loading dependent $K_{\text{mat}}$ parameter (Ainsworth and O’Dowd, 1995, Ainsworth, 1995).

### 1.7.4.1. Procedure I: Modification of the FAC

- Define the normalised constraint parameter $\beta$
- Define the influence of constraint on the fracture toughness, in terms of $\beta$
- Modify the FAD using equation (85) and equation (45)
- Calculate $K_\tau$ using equation (85), with $K_{\text{mat}}$ the high constraint initiation toughness that is conventionally used and $K^c_{\text{mat}}$ the constraint dependent fracture toughness.

$$K_\tau = f(L_\tau) \left[ \frac{K^c_{\text{mat}}}{K_{\text{mat}}} \right]$$  \hspace{1cm} (49)

![Figure 23: Constraint corrected Failure Assessment Diagram](image)

### 1.7.4.2. Procedure II: Modification of $K_\tau$

- Define the normalised constraint parameter $\beta$
- Define the relationship between the constraint parameter $\beta$ and the loading parameter $L_\tau$
- Define the relationship between the initiation toughness and the loading parameter $L_\tau$
- Calculate $K_\tau$ by replacing the high constraint fracture toughness parameter $K_{\text{mat}}$ by the constraint dependent fracture toughness defined using equation (87), in equation(49).
New insights into the competition between ductile tearing and plastic collapse

\[
K_{mat}^c = K_{mat} \text{ when } \beta L_r \geq 0 \quad (50)
\]

\[
K_{mat}^c = K_{mat}[1 + a_1 (-\beta L_r)^n] \text{ when } \beta L_r \leq 0 \quad (51)
\]

Method 1 allows for load and crack tip margin analysis through the conventional procedure defined in section 1.7.2.1. This is not the case for method II, as the \(K_{mat}^c\) is dependent on the load [83]. These procedures have been validated for cleavage and ductile fracture conditions. MacLennan and Hancock [69] have analysed the failure points of a range of steel grades, failing under brittle and ductile conditions, using method 1. Sherry et al. validated methodology 1 using experimental validation on small and large scale tests. The procedure has also been validated for biaxial loading condition failing under brittle conditions for a A533B steel plate by Sherry and Sanderson [84]. The constraint corrected procedures were shown to reduce the conservatism of option 1 and 2, with an average benefit of 30-40% on the applied load for lower constraint geometries.

Practical applications of the methodology were explored by Tronskar et al [85] for and offshore structure, allowing for a more efficient design and a better comprehension of the conservatism of conventional failure assessment methodologies.
1.8. **Test Programmes**

1.8.1. **Transferability of fracture parameters from specimens to component level**

The lower J-Resistance obtained under high constrain crack tip conditions is conventionally used to characterise the ductile tearing resistance of a material. The original idea was that the resistance curve obtained under high constraint conditions would be unique and transferable to any geometry. However, J-Resistance curve geometrical and loading dependency was observed, with a steeper curve measured in specimen with lower specific crack tip constraint conditions. Furthermore, fracture toughness standards specify the use of high constraint specimen geometries and impose validity limits in order to ensure high constraint at fracture. This section will present the issue related to obtaining lower bound fracture toughness data. The issue of constraint difference between specimen and structure will then be presented. Finally, full scale tests performed on stainless steel materials will be presented.

1.8.2. **Obtaining high constraint toughness from 304(L) stainless steel**

There is very little fracture toughness data available in the literature for 304(L) stainless steel, and no valid fracture toughness data was found. Surprisingly, the issue of obtaining valid fracture toughness data for 304(L) is not greatly discussed in the literature. The fracture toughness properties of a 304(L) stainless steel material was investigated by Gosh et. al [86]. The specimen was design and tested according to ASTM 1820 [12], with the geometry presented in Figure 24 and the material tensile properties are presented in Table 2. The J-Resistance curve obtained is presented in Figure 25. The measured fracture toughness reported in this paper is 1186 kJm$^{-2}$. The validity limit for this geometry and material tested as defined by ASTM 1820 (equation (52)) is 359 kJm$^{-2}$. These results are representative of the issue of obtaining valid fracture toughness data for 304 stainless steel using conventional fracture toughness specimen defined by the fracture toughness test standards such as ASTM 1820.
New insights into the competition between ductile tearing and plastic collapse

\[ J_{\text{max}} = \frac{B\sigma_y}{20} \]  \hspace{1cm} (52)

**Table 2: Tensile and Hardness properties of solution annealed 304LN stainless steel** [86]

<table>
<thead>
<tr>
<th>Steel</th>
<th>Yield strength (MPa)</th>
<th>UTS (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>304L</td>
<td>352</td>
<td>687</td>
</tr>
</tbody>
</table>

**Figure 24** Fracture specimen geometry designed according to ASTM 1820 in [86]

**Figure 25:** J-Resistance curve for a solution annealed 304 stainless steel [87]

### 1.8.3. Assessment of safety margins in Assessment procedure:

Low yield high toughness materials tend to fail according to ductile fracture or plastic collapse. ASME Section XI takes advantage of highly ductile behaviour of stainless steel to define allowable flow size according to plastic collapse criteria. No knowledge of the material fracture toughness is required, and plastic collapse criteria are easy to define numerically, providing financial benefit to the industry without reducing the plant safety.

A number of test programs have investigated the failure mechanisms in pipe components manufactured from ferritic and stainless steels, commonly used in primary system piping. This section will discuss the results obtained from two test programs, the USNRC
Degraded Piping Program, led by Battelle [87] and the JAERI ductile fracture test program [88, 89]. In addition, results obtained by Shin et al [90] on stainless steel piping are discussed. The two programs investigated the failure of pipes containing circular, part through and penetrating cracks.

1.8.3.1. Degraded Piping Program

Three large scale pipe tests were conducted as part of the NRC Degraded Piping Program, and the initial results were presented by Wilkowski et al [87]. Three pipes of outer diameter 60mm, 114mm and 406mm with fully circumferential cracks with partial through wall crack, with a range of crack size (Table 3). The crack geometry is shown in Figure 26.

![Cracked geometry](image)

**Figure 26: Cracked geometry**

The net section stress at initiation obtained from the pipe test at maximum load and flow stress obtained through tensile testing were reported [87]. The flow stress was defined differently (equation (53)) from the R6 definition (equation (54)) and was corrected for in Table 30.

\[
\sigma_f = 1.15 \frac{\sigma_y + \sigma_{UTS}}{2} \quad \text{(53)}
\]

\[
\sigma_f = \frac{\sigma_y + \sigma_{UTS}}{2} \quad \text{(54)}
\]

Initiation toughness was not given but we can assume that the yield stress over the flow stress is usually about 1.61 for stainless steel. The yield stress for each pipe material was calculated using this assumption and the corresponding L values at initiation and experimental maximum load and are reported in Table 3. In all cases, the failure of the pipe occurred beyond the limit load.
New insights into the competition between ductile tearing and plastic collapse

Table 3: Four point bend pipe test results [87]

<table>
<thead>
<tr>
<th>Test</th>
<th>Battelle [87]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer Diameter (mm)</td>
<td>60.3</td>
</tr>
<tr>
<td>t (wall thickness)</td>
<td>6.02</td>
</tr>
<tr>
<td>R/t</td>
<td>4.51</td>
</tr>
<tr>
<td>defect geometry</td>
<td>Circumferential through wall crack; θ=66.7</td>
</tr>
<tr>
<td>Loading condition</td>
<td>bending</td>
</tr>
<tr>
<td>Lr at initiation</td>
<td>1.947</td>
</tr>
<tr>
<td>Lr max test</td>
<td>1.974</td>
</tr>
<tr>
<td>Lr max tensile</td>
<td>1.61</td>
</tr>
</tbody>
</table>

1.8.3.2. JAERI Program:
A series of stainless steel pipes with an outer radius of 165.2mm and thickness 10.8mm was tested, with part through circumferential crack of angle θ=0, 45 and 90 degrees with a range of crack depths tested under bending at room temperature, as part of the JAERI ductile pipe fracture program and analysed by Shibata [88]. The limit load was defined from equilibrium equations with the maximum stress set at the flow stress of the material. The results show that pipes tend to fail by plastic collapse, with the limit load analysis providing reasonable prediction for failure conditions.
New insights into the competition between ductile tearing and plastic collapse

Figure 27: JAERI experimental solutions [88]

1.8.3.3. R6 conservatism
Shin et al [90] tested a seamless 304 stainless steel pipes with outer diameter 165mm and inner diameter 143mm under four point bend. Each pipe contained a through wall crack with a range of crack length to circumference ratio. Shin et al investigated the difference between initiation load ($P_{0.2}$) measured in the experiment and the initiation load predicted by the various R6 FAC. Furthermore, a crack propagation instability analysis was performed to define the maximum load using the various R6 FAC and the measured maximum loads. The results are presented in Table 4.

Table 4: Comparison between predicted and measured maximum loads in standards specimens and pipe specimens by the R6 method

<table>
<thead>
<tr>
<th>Specimen</th>
<th>R6 curve option</th>
<th>Measured $P_{0.2}$(tonf)</th>
<th>Predicted $P_{0.2}$(tonf)</th>
<th>Error (%)</th>
<th>Measured $P_{\text{max}}$(tonf)</th>
<th>Predicted $P_{\text{max}}$(tonf)</th>
<th>Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>PIPE02</td>
<td>1</td>
<td>3-4</td>
<td>2-9</td>
<td>14-7</td>
<td>5-1</td>
<td>3-7</td>
<td>27-5</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>2-8</td>
<td>17-6</td>
<td></td>
<td>3-5</td>
<td>31-4</td>
<td></td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>2-2</td>
<td>35-3</td>
<td></td>
<td>4-1</td>
<td>19-6</td>
<td></td>
</tr>
<tr>
<td>PIPE03</td>
<td>1</td>
<td>6-5</td>
<td>5-8</td>
<td>10-8</td>
<td>11-7</td>
<td>7-4</td>
<td>36-8</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>5-8</td>
<td>10-8</td>
<td></td>
<td>7-0</td>
<td>40-2</td>
<td></td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>4-4</td>
<td>32-3</td>
<td></td>
<td>8-5</td>
<td>27-4</td>
<td></td>
</tr>
<tr>
<td>PIPE05</td>
<td>1</td>
<td>8-5</td>
<td>7-7</td>
<td>9-4</td>
<td>13-8</td>
<td>10-3</td>
<td>25-4</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>7-7</td>
<td>9-4</td>
<td></td>
<td>9-8</td>
<td>29-0</td>
<td></td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>6-3</td>
<td>25-9</td>
<td></td>
<td>11-9</td>
<td>13-8</td>
<td></td>
</tr>
</tbody>
</table>

73
New insights into the competition between ductile tearing and plastic collapse

The results show that the R6 FAD method is consistently conservative in its estimation of initiation load and instability load. It is also very interesting to note that the pipe failed in each case close to the instability load predicted by R6.
1.9. Knowledge gap

The literature review has shown that there are well developed methodologies to define fracture toughness under small scale yielding conditions. Furthermore, a range of parameters accounting for constraint, such as the J-T and J-Q two parameter fracture mechanic parameters, and a range of parameters providing constraint independent ductile fracture predictions, such as the Rice and Tracey and work of fracture local approaches. However, the application of these approaches to the characterisation of the failure mechanism in structural components is sparse. In particular

- There is little published data on the mechanical properties of austenitic stainless steels materials, in particular valid fracture toughness data and J-Q fracture locus.
- No systematic analysis of the failure of stainless steel pipes under using constraint independent fracture criteria such as local approaches.

The work undertaken for this PhD project has sought to address these issues. The aim of the research is to provide a better understanding of the fracture toughness constraint dependence under large scale yielding of austenitic stainless steels, and suggest a robust approach to investigate the failure of components manufactured from 304 stainless steel. As described in the following chapters, these knowledge gaps have been addressed through a combination of experimental and analytical approaches. The lower bound fracture toughness and the J-Q fracture locus have been defined using a combination of non-valid fracture toughness tests and numerical procedures. Furthermore, a modified, constraint independent, FAD methodology is proposed to define the failure conditions of components under large scale yielding conditions.
Chapter 2  Experimental Methodology

This chapter outlines the experimental methodologies used within this research project and describes the material characterisation, mechanical testing, specimen examination and provides a more detailed section on the Digital Image Correlation techniques used.

2.1.  Material Characterization

2.1.1.  Chemical composition

All experimental work in this project was conducted with respects to Grade 304(L) austenitic stainless steel. Two batches of material were investigated. Batch one, referred to in this work as “pipe material”, was sourced through BAE Systems. The material was in a form of a pipe of internal radius (r) 147mm and thickness (t) 33mm (Figure 28). Batch two, referred to in this work as “plate material”, was a 20% cold worked, rolled plate of dimensions 1000x80x40 mm in the L, T and S directions respectively (Figure 29).

![Pipe material coordinate system](image1)

*Figure 28: Pipe material coordinate system*

![Plate material coordinate system](image2)

*Figure 29: Plate material coordinate system*

The chemical composition of the pipe material, plate material and grade requirements defined by ASTM A250 are presented in Table 5. Both materials lie within the specified composition range defined for this alloy.
New insights into the competition between ductile tearing and plastic collapse

Table 5: Chemical composition of Plate and Pipe material and ASTM A250 grade definition

<table>
<thead>
<tr>
<th>Element (%)</th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Mo</th>
<th>Ni</th>
<th>Cu</th>
<th>Nb</th>
<th>N</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pipe Material</td>
<td>0.02</td>
<td>0.27</td>
<td>1.69</td>
<td>0.021</td>
<td>0.007</td>
<td>18.0</td>
<td>0.26</td>
<td>10.03</td>
<td>0.25</td>
<td>0.01</td>
<td>n/a</td>
</tr>
<tr>
<td>Plate Material</td>
<td>0.025</td>
<td>0.46</td>
<td>1.95</td>
<td>0.0044</td>
<td>0.025</td>
<td>18.2</td>
<td>0.38</td>
<td>8.13</td>
<td>0.23</td>
<td>0.02</td>
<td>0.10</td>
</tr>
<tr>
<td>S30403 (304L)</td>
<td>0.03</td>
<td>0.75</td>
<td>2</td>
<td>0.045</td>
<td>0.03</td>
<td>18.0-20.0</td>
<td>n/a</td>
<td>8-10.5</td>
<td>n/a</td>
<td>n/a</td>
<td>n/a</td>
</tr>
</tbody>
</table>

The low carbon content of the pipe material and the plate material provides a lower probability of carbide formations, specifically chromium carbides which can lead to material sensitization. A large difference in sulphur concentration is observed between the pipe and plate material. Sulphur is an impurity that can lead to the creation of initiation sites in the form of manganese sulphide inclusions. This suggests that the pipe material is of higher quality than the plate material. The molybdenum provides protection against pitting corrosion.

2.1.2. Metallographic:

The microstructures of the pipe and plate materials were characterised in all three orientations. Metallographic samples were mounted in Bakelite, followed by grinding and polishing. The material was polished to ¼ micron followed by electro-etching using oxalic acid at 10A for 2 minutes. The microstructure was observed using an optical microscope at a 100 times magnification. The ASTM E112 [91] methodology was used to determine the grain size of the material using the line intercept methodology. Results are shown in section 4.1.

2.1.3. Tensile test:

A series of standard tensile test specimens were produced in the orientation of the crack opening in the fracture mechanics tests, with the specimen axis in the hoop direction in the pipe material and L direction in the plate material.
Three specimens were extracted at different locations along the axial plane in the pipe material. Six tensile specimens were extracted from the plate materials in the L direction at different positions in the T-S plane. The position of extraction of the tensile specimens, defined as the distance from the bottom left corner of the plate in T and S directions, are presented in Table 6.

Table 6: Position of extraction of tensile specimen in the plate material.

<table>
<thead>
<tr>
<th>Specimen number</th>
<th>T (mm)</th>
<th>S (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>10</td>
<td>20</td>
</tr>
<tr>
<td>2</td>
<td>10</td>
<td>40</td>
</tr>
<tr>
<td>3</td>
<td>10</td>
<td>60</td>
</tr>
<tr>
<td>4</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>5</td>
<td>30</td>
<td>40</td>
</tr>
<tr>
<td>6</td>
<td>30</td>
<td>60</td>
</tr>
</tbody>
</table>

A mark indicating specimen number was engraved onto both ends of each specimen. Each specimen number was extracted from a specific location in the plate and the pipe as shown in Figure 38 and Figure 34 respectively. The specimen geometry is shown in Figure 30. Specimens had a round cross section with a nominal diameter of 5mm and a gauge length of 30mm. Specimen dimensions were precisely measured before testing using a Vernier calliper. Two pipe material and six plate material tensile specimen were tested.
New insights into the competition between ductile tearing and plastic collapse

Figure 30: Tensile specimen design

Tests were carried out in displacement control, at a displacement rate of 0.1mm/min, using a servo hydraulic MTS 100kN test machine. Tensile extension was measured using an extensometer and applied load via a load cell. Specimen extension and load were recorded at 0.1 Hz. Load and displacement measurements were used to calculate engineering stress-strain behaviour. Equation (55) and equation (56) were used to calculate true stress - true strain behaviour from engineering stress strain. In order to gain a good understanding of the material behaviour, in both material batches, 0.2% proof stress, Ultimate Tensile Strength (UTS) and percentage elongation were all calculated.

\[
\sigma_{true} = \sigma_{eng}(1 + \varepsilon_{eng}) \tag{55}
\]

\[
\varepsilon_{true} = \ln(1 + \varepsilon_{eng}) \tag{56}
\]

2.1.4. Hardness Testing:

A hardness map was performed on the T-S plane of the plate material using a slice of dimension 80x40 mm² in the T and S orientations respectively. A grid of 4 equally spaced points in the T direction of at distance of 10, 30, 50 and 70 mm for the edge and 15 equally spaced points in the S direction every 2.5mm was created. Vickers Hardness measurements were done using a 20 kg load. The Vickers Hardness was extrapolated into yield strength using equation (57) [92].
New insights into the competition between ductile tearing and plastic collapse

\[ \sigma_y = 2.7 \ HV_{20} - 125 \]  \hspace{1cm} (57)
2.2. **Fracture Toughness Testing**

2.2.1. **Pipe Material testing**

A set of 18 fracture toughness specimen with different crack lengths were tested to quantify the effect of crack depth and loading condition on the initiation toughness of 304(L) primary system piping Stainless steel (section 2.1.1.). Set 1 was composed of 7 shallow cracked three point bend specimen with thickness (B) to width (W) ratio of 0.37 length (L) of 120mm and B=10 mm (Figure 32). The specimens were fatigue pre-cracked to the required a/W=0.2 and machined down to a thickness (B) to width (W) ratio of 0.5 (Figure 33). These specimens will henceforth be referred to as SEN(B)_S. Set 2 was composed of 6 Deep cracked three point bend specimen with thickness (B) to width (W) ratio of 0.5 length (L) of 120mm and B=10 mm (Figure 31). The specimens were fatigue pre-cracked to required a/W=0.5. These specimens will henceforth be referred to as SEN(B)_D. Set 3 was composed of 5 deep cracked tension specimen with thickness (B) to width (W) ratio of 0.5 length (L) of 120mm and B=10 mm (Figure 31). The specimens were fatigue pre-cracked to required a/W=0.5. These specimens will henceforth be referred to as SEN(T)_D. Specimen sets SEN(T)_D and SEN(B)_D are the same geometry and crack depth, but are loaded differently.

*Figure 31: Single Edge Deep Cracked specimen engineering drawings*
New insights into the competition between ductile tearing and plastic collapse

Figure 32: Single edge shallow crack pre-crack geometry engineering drawings

Figure 33: Single Edge Shallow Cracked specimen engineering drawings

The three sets of specimens were extracted in the hoop-radial orientation as shown in Figure 34. Deep cracked bend specimen were designed according P2-92 [13]. The same geometry was used for the shallow crack bending and deep cracked tension specimen. Specimens were extracted in the radial direction with the crack propagating into the pipe (Figure 35). Specimen with nominal crack lengths of a/W=0.2 and 0.5 were tested in tension and in three bending (Table 7). The difference in crack length and loading conditions provides different levels of in-plane constraint conditions.
New insights into the competition between ductile tearing and plastic collapse

Table 7: Single edge cracked specimen test matrix.

<table>
<thead>
<tr>
<th></th>
<th>Tested under three point bend</th>
<th>Tested under gripped tension</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a/W = 0.2$</td>
<td>$\text{SEN(B)}_S$ (5 specimen)</td>
<td>n/a</td>
</tr>
<tr>
<td>$a/W = 0.5$</td>
<td>$\text{SEN(B)}_D$ (6 specimen)</td>
<td>$\text{SEN(T)}_D$ (7 specimen)</td>
</tr>
</tbody>
</table>

Figure 34: Specimen cutting plan from pipe section 1
An EDM notch of radius 0.1mm was introduced at the middle of each specimen followed by a minimum of 2 mm fatigue crack growth. Fatigue pre-cracking was carried out in a three point bend configuration with an R ratio of 0.1 and the final Stress intensity factor of 20 MPa√m. During fatigue cracking the tip of the crack was monitored using two digital cameras positioned on each side of the specimen.

Specimens were tested using a 100kN servo hydraulic MTS machine, in displacement control at a constant crosshead displacement rate of 0.2mm/min. The three point bend specimens were loaded at the two outer pins with a span S=5W (Figure 36). The tension specimens were loaded via friction grips (Figure 37). The applied load, cross-head displacement and Crack Mouth Opening Displacement (CMOD) were recorded at a rate of 10Hz. Specimen were tested using the multi-specimen methodology. Each specimen was tested to a different final load in order to generate different amounts of ductile crack growth and then to construct a J-Resistance curve.
Fracture toughness was calculated following the method defined in P2-92 multi-specimen testing procedure[13]. Initiation toughness was expressed in J and was calculated using equation (58) and equation (59).

\[
J = J_0 \left(1 - \frac{(0.75\eta - 1)\Delta a}{(W - a_0)}\right) \tag{58}
\]

\[
J_0 = \frac{\eta U}{B_n(W - a_0)} \tag{59}
\]

with \(W =\) specimen width, \(B_n =\) net specimen thickness, \(\Delta a =\) crack extension, \(a_0 =\) initial crack length and \(U =\) total area under the load vs. CMOD curve.

The \(\eta\) parameter represents the relationship between the area under the load vs. displacement curve and the specimen geometry with the J-integral. For deep cracked specimen, the \(\eta\) factor is independent of the crack length for limited amounts of crack growth. The parameter \(\eta\) was calculated using 2D Finite Element analyses as defined in
New insights into the competition between ductile tearing and plastic collapse

equation (60) and equation (61). The method is defined in section 3.1. After testing, all the specimens were heat tinted on a hot plate heated at 250 degrees Celsius to mark the extent of ductile crack extension and then fatigue cracked open.

\[ \eta = \frac{dJ}{dx} \]  

(60)

With

\[ x = \frac{U}{B_n(W - a_0)} \]  

(61)

Initial and final crack lengths were measured according to nine point averaging [13] after testing using a travelling optical microscope to determine the extent of ductile crack extension. The measurements were made at nine equally spaced points, where the two outer points are located at a distance 0.01B from the surface of the specimen. The two outer-most points are averaged and the value is then averaged with the seven inner points.

Two measurements of fracture initiation were extracted from the tests, \( J_{0.2} \), which is the value of \( J \) after 0.2mm of ductile crack extension and \( J_{0.2BL} \) which is the value of \( J \) after 0.2mm crack extension correcting for crack tip blunting. The crack tip blunting line was constructed according to P2-92 [13] following equation (62).

\[ J = 3.75R_m \Delta a \]  

(62)
2.2.2. **Compact Tension Specimen**

A set of 15 Compact Tension (CT) specimens covering a range of different sizes were tested to quantify the effect of specimen size on the initiation toughness of 304(L) plate material (section 2.1. ). Set 1 was composed of five Compact Tension (CT) specimens with a Width (W) to Thickness (B) ratio of 2 and a thickness (B) of 25 mm. This set will henceforth be referred to as 25mmCT. Set 2 was composed of five Compact Tension (CT) specimens also with W/B=2 and B=15mm (Figure 39). This set will henceforth be referred to as 15mmCT. Set 3 was composed of five Compact Tension (CT) specimens with a W/B=2 and B=10mm (Figure 40). This set will henceforth be referred to as 10mmCT. Specimens were extracted in the L-T orientation in the plate as close to the centre-line as material availability would permit (Figure 38). All specimens were designed according to ASTM 1820 [12] with a nominal crack length to specimen width ratio a/W=0.55 (Table 8, to obtain J-resistance curves under high constraint conditions.

<table>
<thead>
<tr>
<th>Table 8: Compact Tension Specimens dimensions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thickness (B) (mm)</td>
</tr>
<tr>
<td>-------------------</td>
</tr>
<tr>
<td>25mmCT</td>
</tr>
<tr>
<td>15mmCT</td>
</tr>
<tr>
<td>10mmCT</td>
</tr>
</tbody>
</table>

*Figure 38: Compact Tension specimen cutting plan - plate material*
New insights into the competition between ductile tearing and plastic collapse

Figure 39: 15mmCT engineering drawing

Figure 40: 10mmCT engineering drawing
New insights into the competition between ductile tearing and plastic collapse

An EDM notch of radius 0.1mm was introduced at the middle of each specimen followed by a minimum of 1 mm fatigue crack growth. Fatigue pre-cracking was carried out with an R ratio of 0.1 and an end stress intensity factor of 20 MPa√m.

Specimens were tested at ambient temperature using a 100kN servo hydraulic MTS machine, in displacement control at a constant cross-head displacement rate of 0.4mm/min. Specimens were tested using the unloading compliance methodology to obtain crack growth values. Loading cycles are summarised in Table 9. The CT specimens were loaded using flat bottomed clevises. The crack-mouth opening displacement (CMOD) was monitored using a clip gage located between knife-edges on the load line for the 25mmCT and 15mmCT specimen. CMOD was monitored on the front face of the 10mmCT specimen, which was then correlated back to the displacement on the loading line. The applied load, cross-head displacement and CMOD were recorded at a rate of 10Hz.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>cross-head displacement rate (mm/min)</th>
<th>Loading time (s)</th>
<th>Hold time (s)</th>
<th>Unload time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>all</td>
<td>0.4</td>
<td>120</td>
<td>60</td>
<td>40</td>
</tr>
</tbody>
</table>

Fracture toughness was calculated following the method defined in ASTM 1820 single specimen testing procedure.

$$ J_i = \frac{(K_i)^2(1 - v^2)}{E} + J_{p_t(i)} $$  \hspace{1cm} (63)

Where $K_i$ is the stress intensity factor at each increment of loading, $E$ the Young’s modulus, $v$ the Poisson’s ratio, and $J_{p_t(i)}$ the plastic value of J as defined in equation (64) for each increment of loading.

$$ J_{p_t(i)} = J_{p_t(i-1)} + \left( \frac{\eta_{i-1}}{B_{i-1}} \right) \left( \frac{A_{p_t(i)} - A_{p_t(i-1)}}{B_N} \right) \left[ 1 - \gamma_{i-1} \left( \frac{a_i - a_{i-1}}{b_{i-1}} \right) \right] $$  \hspace{1cm} (64)
New insights into the competition between ductile tearing and plastic collapse

where \( \eta = 2 + 0.522 \left( 1 - \frac{a_i - 1}{W} \right) \) and \( \gamma_{i-1} = 1 + 0.76 \left( 1 - \frac{a_i - 1}{W} \right) \) and \( A_{\text{pli}} \) is the increment of plastic area under the load versus plastic load-line displacement curve.

The crack length was calculated from the unloading compliance, corrected for specimen rotation, using equation (65).

\[
\frac{a}{W} = 1.00196 - 4.06319 \times U_{LL} + 11.242 \times U_{LL}^2 - 106.043 \times U_{LL}^4 - 650.677 \times U_{LL}^6
\]

Where

\[
U_{LL} = \frac{1}{\sqrt{Z_{LL} + 1}}
\]

And

\[
Z_{LL} = \frac{BE\Delta_i}{P}
\]

\[
\Delta_i = \frac{C_i}{R \sin \theta_i - \cos \theta_i} \left[ \frac{D_i}{R} \sin \theta_i - \cos \theta_i \right]
\]

\[
\theta = \left[ \frac{(d_i/2 + D_i)}{(D^2 + R^3)^{1/2}} \right] - \tan^{-1} \frac{D}{R}
\]

For side-grooved specimen, the following definition of thickness should be used:

\[
B_e = B - \frac{(B - B_N)^2}{B}
\]

Where \( B_e \) the effective thickness if specimen is side grooved, \( B_N \) the side grooved thickness, \( E \) the Young’s Modulus, \( P \) the Load applied, \( \Delta \) the unloading slope corrected for rotation, \( H \) the initial half-span of the load points, \( R \) the radius of rotation of the crack centreline \((W/a)/2\) where \( a \) is the updated crack length, \( D \) the one half of the initial distance between the displacement measurement points and \( \theta \) the angle of rotation of a rigid body element about the unbroken mid-section line.
New insights into the competition between ductile tearing and plastic collapse

2.2.3. **Validity limits**

In order to ensure small scale yielding conditions at the centre of the specimens, fracture toughness standards [[12],[6],[13]] define size limitations dependant on the material yield strength. The ESIS P2-92[13], ASTM 1820[12] and BS 7448-4[6] limitations for crack growth ($\Delta a_{\text{max}}$) and J-integral ($J_{\text{max}}$) are presented in equation (66), equation (67) and equation (68) respectively.

\[
\begin{align*}
\Delta a_{\text{max}} &= 0.1(W - a_0) \\
J_{\text{max}} &= (W - a_0)\frac{R_f}{20} \\
J_{\text{max}} &= B\frac{R_f}{20}
\end{align*}
\]

ESIS P2-92 \hspace{1cm} (66)

\[
\begin{align*}
\Delta a_{\text{max}} &= 0.25b_0 \\
J_{\text{max}} &= \frac{b_0\sigma_y}{20} \\
J_{\text{max}} &= B\frac{\sigma_y}{20}
\end{align*}
\]

ASTM 1820 \hspace{1cm} (67)

\[
\begin{align*}
\Delta a_{\text{max}} &= 0.1(W - a_0) \\
J_{\text{max}} &= (W - a_0)\frac{R_f}{40} \\
J_{\text{max}} &= B\frac{R_f}{40}
\end{align*}
\]

BS 7448-4 \hspace{1cm} (68)

\[R_f\] is the flow strength.

With $b_0$ the initial dimension of the un-cracked ligament and $b_0$ the initial dimension of the un-cracked ligament. All three fracture toughness validity limits were investigated in this work.
2.3. **Digital Image correlation**

Digital Image Correlation (DIC) was used in all fracture mechanics tests to monitor the evolution of the plastic zone ahead of the crack. Optical features were introduced on the surfaces of the SEN and 25mmCT specimen using spray paint, and using chemical etching for the 15mmCT and 10mmCT. Both methodologies enabled a clear pattern for the software to monitor. The software monitored the difference in light intensity, providing a clear digital pattern that can be monitored for deformation and movement (Figure 41). Calibration was conducted using a calibration plate. The images were recorded in combination with the load at a rate of 0.6 Hz.

The software monitored a series of interrogation windows each containing a series of features. The displacement and deformation of the interrogation window of image n is compared to that of image n-1. Displacement vectors were calculated covering the whole experiment and these were then post-processed by the software to define directional strains.

**2.3.1. Surface preparation:**
The DIC technique requires the surface of the specimen to have a set of discernible patterns on the surface analysed. The size of the analysed zone is dependent on the feature size. The feature should be sampled with at least 3x3 pixels, and the image size is 2048x2048. The methodologies used to apply the pattern are dependent on the scale of the analysis window used and the range of strains analysed.

**2.3.1.1. Digital speckle correlation (speckle size: 100μm-400μm):**
A uniform undercoat is applied to the surface of the specimen and speckles of different colour paint, is applied to obtain discernible patterns. A white undercoat with black speckle pattern is often used to obtain good contrast, and hence, better resolution. The speckle size which is achievable with a good speckle pattern can be anywhere between 100μm-300μm. If we assume that an equal volume ratio of speckled and non-speckled areas is required to obtain a good analysis, the maximum analytical area should not exceed 30x30mm for 100μm speckles and 100mm for 300μm speckles.

This methodology poses a number of difficulties. The undercoat masks the microstructure, which means that strain cannot be correlated to specific microstructures. This means that the methodology has limited value for the analysis of discontinuities in the material, such
as weld interface. Furthermore, the speckles are difficult to apply. In addition, under large strain conditions, the undercoat can delaminate from the surface of the specimen, or crack. Digital speckle correlation was used for the SEN(T)_D, SEN(B)_D, SEN(B)_S and 25mmCT specimens as they provided the best feature quality.

2.3.1.2. Micro-structural features correlation (20μm to 100μm):

The polishing and then etching of specimen provides a distribution of random patterns on the surface of the specimen. The feature size which is achievable with a good etching pattern can be anywhere between 20μm-100μm, depending on the grain size and other discernible features in the material. This leads to maximum analytical areas of 7x7mm for 20μm features and 30*30mm for 100μm features.

This methodology allows for the correlation between local strains and the micro structural features. Furthermore, although the feature might deteriorate at large strains, the displacement measured will always be that of the material investigated. The main limiting factor of this methodology is the loss of accuracy due to the lack of control over the etched patterns, as the material will preferentially etch at specific features and it is hard to obtain a uniform etching over large areas. Micro-structural features correlation was used for the SEN(T)_D, SEN(B)_D, SEN(B)_S and 25mmCT specimens as they provided the best feature quality.

2.3.2. Strain analysis methodology

The images taken were of size 2048x2048 pixels covering a square area of 30x30mm, providing a resolution of 15μm per pixel. The displacement vector calculation was performed in two passes. The first pass was performed with an interrogation window of 128x128 pixels with a square interrogation window and an interrogation window overlap of 50%. The second pass was performed with an interrogation window of 64x64 with a circular interrogation window and an overlap of 75%. This provides final resolution of 135μm per displacement vector. Four components of strains, \( \varepsilon_{xx}, \varepsilon_{yy}, \varepsilon_{xy}, \text{ and } \varepsilon_{yx} \), were post-processed from the displacement vectors in the DAVIS software and then exported into Matlab. \( \varepsilon_{zz} \) was calculated using the tensor relationship assuming plane stress conditions (equation (69)). Equivalent strains were calculated using equation (70) and equation (71).
New insights into the competition between ductile tearing and plastic collapse

\[
\varepsilon_{zx} = -\frac{\nu}{1-\nu}(\varepsilon_{xx} + \varepsilon_{yy})
\]  \hspace{1cm} (69)

\[
\overline{\varepsilon^p} = \left(\frac{2}{3}(\varepsilon'^p, \varepsilon'^p)\right)^{1/2}
\]  \hspace{1cm} (70)

with

\[
\varepsilon'_{xx} = \varepsilon_{xx} - \frac{1}{3}(\varepsilon_{xx} + \varepsilon_{yy} + \varepsilon_{zz})
\]

\[
\varepsilon'_{yy} = \varepsilon_{yy} - \frac{1}{3}(\varepsilon_{xx} + \varepsilon_{yy} + \varepsilon_{zz})
\]

\[
\varepsilon'_{zz} = \varepsilon_{zz} - \frac{1}{3}(\varepsilon_{xx} + \varepsilon_{yy} + \varepsilon_{zz})
\]

In order to investigate the development of plasticity in the un-cracked ligaments, the elastic strain must be evaluated and taken out of the DIC measured strains.

In order to do this, we assume that the stainless steels investigated behave in a homogeneous isotropic manner. The total strains are composed of elastic strain and plastic strains (equation (72)). Following Hook’s law, the linear elastic component of deformation is proportional to the applied stress and the material’s young modulus, as defined in equation (73). Hence, the plastic strain can be evaluated from total strain, the applied stress and the young’s modulus of the material (equation (74)).

\[
\varepsilon_{total} = \varepsilon_{elastic} + \varepsilon_{plastic}
\]  \hspace{1cm} (72)

\[
\varepsilon_{elastic} = \frac{\sigma}{E}
\]  \hspace{1cm} (73)

\[
\varepsilon_{plastic} = \varepsilon_{total} - \frac{\sigma}{E}
\]  \hspace{1cm} (74)

A material specific relationship, proportional to the material’s stress vs. strain curve, exists between the total strain and the plastic component of strain. This relationship was calculated for each material investigated and used to evaluate the plastic component of strain in the DIC measurements.
New insights into the competition between ductile tearing and plastic collapse

Figure 41: Example of a speckle pattern used for Digital Image Correlation on a SEN(B)$_S$ before testing
Chapter 3  Finite Element Analysis Methodology

This section describes the finite element analyses undertaken to simulate the fracture mechanics tests, a crack under Small-Scale Yielding SSY conditions, and a crack in a pipe. The material properties used to define the elastic and in-elastic behaviour are defined in section 0. Plasticity is defined using incremental plasticity, defined using the stress-strain curves obtained experimentally. All models were constructed using ABAQUS CAE [93].

3.1.  \( \eta \) factor calculation

In order to calculate the \( \eta \) factor (section 2.2.2.) for the SEN(B)_S, SEN(B)_D and SEN(T)_D specimens, 2D, plane strain, finite element models of each geometries were generated. Two dimensional finite element models were used to define the \( \eta \) factor in order to provide consistent results with \( \eta \) factors defined in test standards. In this section, pipe material properties were used to define the material plastic behaviour, using the true-stress vs. true strain curves defined in section 4.2.1.2. The onset of plasticity was defined as the deviation from the elastic line. The elastic line was defined by the Young’s modulus, obtained from BS 7443-4. All models were run once in with the elastic material definition and once with elastic-plastic material definition. Load, CMOD and K were derived from the elastic models. Load, CMOD and J were derived from the elastic plastic models.

3.1.1.  2D shallow and deep cracked bend specimen

Two small strain finite element models were built for the fracture mechanics specimens used to assess the fracture toughness properties of the pipe material, section 2.2.1. Symmetry conditions were specified along the un-cracked ligament, enabling one half of each specimen to be modelled numerically. Each model consisted of 8 noded reduced integration plane strain elements (CPE8R). The pin below the crack was modelled as an analytical rigid structure and was constrained in the x, y and rotational direction (Figure 42). Frictionless hard contact was defined between the pin and the specimen. Loading was...
achieved through the displacement of a single node in the x direction at a distance corresponding to a span (S) of 4W and a span (S) of 5W. The single node displacement methodology was chosen over the displacement of a rigid pin (with hard frictionless contact) due to convergence issues. A rigid pin displacement would require mesh refinement at the loading positions. Furthermore, at large displacements, there would be convergence issues due to the large plasticity at the pin/specimen contact point. A sensitivity analysis showed that the loading methodology did not affect the Load vs. Displacement and J vs. Load behaviour.

![Figure 42: half SEN(B) specimen, 2D FEA model, Length= 120mm, W= 20mm, S= 4W and S= 5W](image)

3.1.2. **2D deep cracked Tension specimen**

A small strain finite element models were built for the SEN(T) geometry with, W=2B and crack depth of a/W=0.5. Symmetry conditions were specified along the un-cracked ligament, enabling one half of each specimen to be modelled numerically. Each model consisted of 8 node reduced integration plane strain elements (CPE8R). The length (L) of the model was set to 150mm to accommodate the machine stiffness induced bending. The length of the model was defined by matching the experimental and analytical Load vs. CMOD responses. Loading was achieved through the displacement in the x direction of the last column of elements (Figure 43).
New insights into the competition between ductile tearing and plastic collapse

Figure 43: half SEN(T) specimen, 2D FEA model, Length= 200mm, W= 20mm, S= 4W and S= 5W

3.1.3. Validation of the models:

3.1.3.1. Shallow and deep cracked SEN(B)

Two spans (S) were defined in order to be able to validate the η factor calculation approach. A span of S=4W was used to validate the model. The stress intensity factor obtained from the elastic model was compared to K solutions provided in Murakami stress intensity factor handbook [37] (equation (75), equation (76)) and was found to be within 4% of the handbook solutions. The η factor was calculated using equation (77) and equation (78) from the values obtained in the elastic-plastic model and compared with that of standard solutions. The model solution was found to be within 1% of the standard solutions. The models were subsequently run with a Span (S) of 5, as used in the tests, and the η factor calculated for each model.

SEN(B) specimen with a span (S=4W):

\[
K_1 = \frac{3SP}{2\pi W^2} \sqrt{\pi a} \times F_1(\alpha)
\]  
(75)

\[
F_1(\alpha) = \frac{1.99 - \alpha(1 - \alpha)(2.15 - 3.93\alpha + 2.7\alpha^2)}{(1 + 2\alpha)(1 - \alpha)^{3/2}}
\]  
(76)

With S the span, t the thickness, W the width, a the crack length and \(\alpha = a/W\)
New insights into the competition between ductile tearing and plastic collapse

\[ \eta = \frac{df}{dx} \]  

(77)

With

\[ x = \frac{U}{B_n(W - a_0)} \]  

(78)

3.1.3.2. Deep cracked SEN(T)

The SEN(T)_D model defined in section 3.2. was validated by comparing the stress intensity factor obtained from the elastic model with the K solutions provided in Murakami stress intensity factor handbook [37] (equation (79), equation (80)), and was found to be within 3% of the handbook solutions. The model was subsequently run elastic-plastically and the load vs. CMOD trace was compared with to the experimental data and was found to lie within the experimental scatter, up to the point where crack extension affects the load-displacement curve.

\[ K_i = \sigma \sqrt{\pi a} \cdot F_i(\alpha) \]  

(79)

\[ F_i(\alpha) = 1.12 - 0.231\alpha + 10.55\alpha^2 - 21.72\alpha^3 + 30.39\alpha^4 \]  

(80)

With a the crack length and \( \alpha = a/W \)
3.2. **3D Single Edge Cracked Specimen**

In order to predict fracture initiation using the various local approaches for the SEN(B)_S, SEN(B)_D and SEN(T)_D specimens, three dimensional finite element models of each geometry were generated. Pipe material properties were used to define the material plastic behaviour incremental plasticity, using the true-stress vs. true strain curves defined in section 4.2.1.2. The onset of plasticity was defined as the deviation from the elastic line, defined by the Young’s modulus. All models had the same square crack tip mesh refinement, described below.

All models were run once with elastic material definitions and once with elastic-plastic material definitions. Load, CMOD and K were outputted from the elastic models. Load, CMOD and J were outputted, as well as the $\sigma_{VMIS}$ (Von Mises stress), $\sigma_H$ (Hydrostatic stress) and equivalent plastic strain in the first row of elements ahead of the crack, in the elastic-plastic models.

3.2.1. **3D Shallow and Deep Cracked Bend Specimen**

Three dimensional, small strain, elastic-plastic finite element analyses were used to simulate the shallow and deep cracked three point bend specimen. Two models were constructed, of length (L) of 120mm, W=2B, B=10mm. Crack length was defined as the average of the initiation crack length measured in specimen geometry (section 4.4.1.1), with $a/W=0.21$ and $a/W=0.52$ for the SEN(B)_S and SEN(B)_D respectively. Symmetry conditions were specified along the un-cracked ligament and the longitudinal mid-plane, enabling one-quarter of each specimen to be modelled numerically. Each model consisted of 20 node reduced integration quadratic elements arranged in constant thickness layers. Each model had a square crack tip mesh refinement of size $x=0.2$mm, $y=0.2$mm, and $z=1$mm (Figure 44). The crack tip elements are designed to be the size of the crack tip extension defined experimentally as initiation.

The pin position in alignment with the crack tip was modelled as a rigid shell tied to a single node, which was constrained in the x, y and z directions as well as rotational displacements. Hard frictionless contact was defined between the pin and the specimen.
New insights into the competition between ductile tearing and plastic collapse

Loading was simulated by the displacement in the y direction of two rows of elements extending through thickness at a distance modelling a span $S=5W$ (Figure 45). A sensitivity analysis showed that the loading methodology did not affect the Load vs. Displacement and J vs. Load behaviour.

*Figure 44: Three Dimensional Single Edge Cracked model - crack tip mesh refinement*

*Figure 45: Three Dimensional Single Edge Cracked three point bend shallow and deep crack model - symmetry conditions - loading conditions - bulk mesh refinement*
3.2.2. **3D Deep cracked tension specimens:**
Three dimensional, small strain, elastic-plastic finite element analyses is used to simulate the Deep cracked Tension specimen with a length (L) of 120mm, W=2B, B=10mm. Crack length was defined as the average of the initiation crack length measured for this specimen geometry (section 4.4.1.), as \( a/W = 0.52 \). Plasticity was modelled using the true-stress vs. true strain relationship obtained from tensile tests reported in section 4.2.1.2. Symmetry conditions were specified along the un-cracked ligament and the longitudinal mid-plane, enabling one quarter the specimen to be modelled numerically. A square crack tip mesh refinement of size \( x=0.2\text{mm}, y=0.2\text{mm} \) and \( z=1\text{mm} \). The crack tip elements are designed to be the size of the crack tip extension defined experimentally at initiation.

Loading was achieved through the displacement in the x direction of the last column of elements.

![Figure 46: Three Dimensional Single Edge Cracked Tension model - symmetry conditions - loading conditions - bulk mesh refinement](image)

3.2.3. **Validation of the models**

3.2.3.1. **3D Shallow and deep crack Bend specimen**
SEN(B)_S and SEN(B)_D models with a span of \( S=4W \) were run elastically. The stress intensity factor at the mid-thickness was compared to \( K \) solutions provided in Murakami stress intensity factor handbook (eq. (75), eq. (76)). The FEA model was found to be within 3% of the handbook solutions. Elastic-plastic models were validated using the
experimental load vs. CMOD curves and were found to follow within the experimental scatter up to initiation.

3.2.3.2. 3D Deep cracked tension specimen
The elastic-plastic model was validated using the load vs. CMOD curve. Due to bending in the loading rig, the model’s length was adjusted to fit the load vs. CMOD curve.
3.3. **Compact Tension Specimen**

In order to define fracture using local approaches in the 25mmCT, 15mmCT and 10mmCT specimens, 3D finite element models were generated of each geometry. In this section, plate material properties were used to define the models plastic behaviour, using the true-stress vs. true strain curves defined in section 0. The onset of plasticity was defined as the deviation from the elastic line, defined by the Young’s modulus.

Three models were constructed, each corresponding to a CT specimen with $W=2B$ and $B=25\text{mm}$, $15\text{mm}$ and $10\text{mm}$ respectively (Figure 47). Crack length was defined as the average of the initial crack length measured for this specimen geometry.

![Figure 47: One-quarter of CT specimen with $a/W=0.55$, $W=2B$ and $B=25\text{mm}$](image)

Symmetry conditions are specified along the un-cracked ligament and the longitudinal mid-plane, enabling one quarter of each CT specimen to be modelled numerically. Each model consisted of 20 nodded, reduced integration, quadratic elements arranged in variable thickness layers with the thickest elements defined at the longitudinal mid-plane and the
New insights into the competition between ductile tearing and plastic collapse

thinner elements defined near the free surface. Each model had a square crack tip mesh refinement of size $x = 0.2\text{mm}$ and $y = 0.2\text{mm}$ and $z$ depending on the element position through thickness.

![Figure 48: Crack tip mesh refinement of CT specimen with $a/W=0.55$, $W=2B$ and $B=25\text{mm}$](image)

Pin loading was simulated by applying a prescribed displacement in the x direction to a single node tied to a rigid body shell modelling the pin. Surface to surface contact with no friction was defined between the rigid shell and the CT. This allowed specimen rotation and measurement of reaction force through one node.

All models were run once with elastic material definitions and once with elastic-plastic material definitions. Load, CMOD and K were outputted from the elastic models. Load, CMOD and J were outputted as well as the $\sigma_{\text{VMIS}}$, $\sigma_{\text{H}}$ and equivalent plastic strain in the first and second row of elements ahead of the crack in the elastic-plastic models.

### 3.3.1. Validation

The CT models were validated using three different methodologies. The J at initial step was compared to K handbook solutions (equation (81) & (82)) for Compact Tension geometries and was found to be consistent within 2% in all models. The models were then validated for consistency by comparing the normalised load ($P/P_L$) vs. the normalised crack mouth opening displacement ($\text{CMOD}/b_0$). Due to the variation in yield strength in the specimens, the models could not be validated against experimental data. The issue is discussed in more details in section 4.4.2.1.
New insights into the competition between ductile tearing and plastic collapse

\[ K_I = \frac{P}{tW^{1/2}} \times F_I(\alpha) \quad (81) \]

\[ F_I(\alpha) = \frac{(2 + \alpha)(0.886 + 4.64\alpha - 13.32\alpha^2 + 14.72\alpha^3 - 5.6\alpha^4)}{(1 - \alpha)^{3/2}} \quad (82) \]
3.4. **Small-Scale Yielding Model**

Two 2D plane strain “boundary-layer” models (BLM) were used to simulate a crack in an infinite body under small scale yielding. The model radius $r$ is set to 200000 mm to ensure that the crack tip plastic zone did not approach the boundary of the model at the maximum load, and therefore ensured small scale yielding conditions. The crack tip of the conventional modified boundary layer model was designed using a concentrated mesh with a collapsed node at the crack tip. This model will be referred to as MBLMN from henceforth (Figure 49). The crack tip conditions of model modified for local approach analysis was designed with square crack tip elements of size $x=0.2$mm and $y=0.2$mm (Figure 50). This model will be referred to as MBLML from henceforth. Symmetry conditions were specified along the un-cracked ligament. Far field displacement in the $u$ and $v$ direction were calculated using equation (83) and (84) [94].

*Figure 49: Modified Boundary Layer Models with collapsed node mesh refinement*
New insights into the competition between ductile tearing and plastic collapse

Figure 50: Modified Boundary Layer Model with square crack tip mesh refinement

\[
\begin{align*}
  u(r, \theta) &= K_1 \left( \frac{1 + \nu}{E} \right) \sqrt{\frac{r}{2\pi}} \cos \frac{\theta}{2} (3 - 4\nu - \nu \cos \theta) + T \left( \frac{1 - \nu^2}{E} \right) r \cos \theta \\
  v(r, \theta) &= K_1 \left( \frac{1 + \nu}{E} \right) \sqrt{\frac{r}{2\pi}} \sin \frac{\theta}{2} (3 - 4\nu - \nu \cos \theta) - T \left( \frac{\nu (1 + \nu)}{E} \right) r \sin \theta
\end{align*}
\]

(83)  (84)

\[K_i = \sqrt{\frac{EJ}{(1 - \nu^2)}}\]

(85)

where \( r \) the radius of the BLM, \( \theta \) the angle of the node from the horizontal, \( K \) the stress intensity factor, \( \nu \) the Poisson’s ratio, \( E \) the Young’s Modulus and \( T \) the elastic T-stress as defined by Rice [95]

3.4.1. Validation:
Both BLM were validated through elastic analysis, comparing the \( K \) and \( T \) values obtained from the contour integral performed by ABAQUS with the \( K \) applied using equations (83)
and (84). Non-linear elastic analyses were verified using contour integrals taken only in the elastically deformed regions and by making sure that the plastic zone was small compared to the overall model size.

### 3.5. **Pipe material:**

In order to investigate the failure mechanism of a typical component in a power-plant, a pipe model was developed. A 2D axisymmetric model of a primary system pipe of internal diameter (D) of 147mm, thickness (t) of 33mm and length (L) 200mm (Figure 51) was built. Internal radial defects of normalised lengths of a/t=0.1, 0.2, 0.3, 0.4, 0.5, 0.6, 0.7, 0.8 and 0.9 were modeled. A hoop stress and axial stress was applied, with \( \lambda \) the ratio of axial over hoop stress (equation (86)). A range of operating conditions were simulated, with applied \( \lambda \) of 0.5, 0.75 and \( \infty \) (pure tension). Internal pressure was applied to the crack faces.

\[
\lambda = \frac{\sigma_{Axial}}{\sigma_{Hoop}} \tag{86}
\]

The models consisted of 8 node reduced integration quadratic elements. Each model has a square crack tip mesh refinement of size x=0.2mm and y=0.2mm. In each case, the crack tip mesh was defined to be the same size and shape as the crack tip refinement used in the SEN(B) and SEN(T) models in order to be able to consistently define failure by ductile extension using local approaches.
New insights into the competition between ductile tearing and plastic collapse

Figure 51: axi-symmetric pipe model with radius (r) = 147 mm and thickness (t) = 33 mm – square crack tip mesh of dimension 0.2 x 0.2 mm
3.6. **RIKS Analyses:**

RIKS analyses were performed on the SEN(B)_S, SEN(B)_D, SEN(T)_D, 25mmCT, 15mmCT, 10mmCT and the pipe models analysis in order to define the limit load. Elastic-perfectly-plastic material definition was used (illustrated schematically in ) with the yield strength of each material used accordingly. The elastic line was defined using the analysed material’s Young’s Modulus. The maximum load obtained was used as the Limit Load. This provides the upper bound limit load (section 1.4.2. ).

![Elastic-perfectly plastic material definition (RIKS analysis)](image-url)

*Figure 52: Elastic-perfectly plastic material definition (RIKS analysis)*
3.7. **Local approach to ductile failure**

The equivalent plastic Von Mises strain ($\varepsilon_p$), Von Mises stress ($\sigma_m$), and hydrostatic stress ($\bar{\sigma}$) were continuously monitored in the first row of elements ahead of the crack tip in the SEN(B)_D, SEN(B)_S, SEN(T)_D, 25mmCT, 15mmCT, 10mmCT and MBLML models to calculate the High constraint Rice and Tracey, generalised Rice and Tracey and work of fracture local approaches.

3.7.1. **Rice and Tracey Ductile Failure model:**

The Rice and Tracey local model describes the proximity to ductile fracture due to void growth, through a curve fit defined by Rice & Tracey [63] of the growth of a spherical void subjected to triaxial stress field in an elastic-perfectly-plastic material (section 1.6.2.).

In its high constraint form is defined using equation (87). In its more generalised form it is defined using equation (88).

\[
HR&T = \int_{0}^{\varepsilon_f} 0.283 e^{\frac{3\sigma_m}{2\bar{\sigma}}} d\varepsilon_p \tag{87}
\]

\[
GR&T_c = \int_{0}^{\varepsilon_f} 0.558 \times sinh\left(\frac{3\sigma_m}{2\bar{\sigma}}\right) + 0.008 \times v \times cosh\left(\frac{3\sigma_m}{2\bar{\sigma}}\right) d\varepsilon_p \tag{88}
\]

Both equation (87) and equation (88) describe the growth of a single void in a unit cell model. This means that the model does not account for void distribution, void growth interaction, nor coalescence. It assumes that all the voids in specific volume of material can be simplified into a single equivalent void. It is therefore important to be analysing consistantly the same volume. The specific analytical volume size can be defined using a range of methodologies (section 1.6.1.). In this work, we have chosen a crack tip mesh of 0.2mm x 0.2mm. This is consistent with the typical inter-partcile spacing for stainless steel, and the engineering definition of crack extension.
3.7.2. Work of Fracture

The Work of Fracture ($W_D$) model [96] describes the proximity to ductile fracture due to the total work done in a analytical volume ahead of the defect.

$$W_D = \int_0^{e_f} 1 + 3\alpha \frac{\sigma_m}{\sigma} e^{3\sigma_m} \, d\varepsilon_p$$  \hspace{1cm} (89)

Equation (89) defines the total work done in the region analysed. It accounts for both work done in non-recoverable deformation and work done in the creation of new surfaces (section 1.6.3. ). The methodology assumes that the work done in the creation of new surfaces is proportional to the void growth, which is defined using the high constraint Rice and Tracey void growth formulation defined in equation (87). The derivation of Equation (89) is described in more details in section 1.6. (literature review)

Failure by ductile fracture is defined when the local approach reaches a specific values defined using a combination of experimental and analytical analysis (section 3.7.3. ), in a specific volume. As in the Rice & Tracey local approach methodologies, it is important to be analysing consistently the same volume. The same crack tip mesh of 0.2mm x 0.2mm was chosen in order to consistent with the engineering definition of crack extension.

3.7.3. Calibration of the local approaches:

Reliable calibration of the Rice and Tracey parameter requires experimental fracture data from at least two different constraint levels. It is conventionally calibrated using a range of notched tensile specimen with varying notch radii providing different constraint conditions. Concerns with the validity of critical Rice and Tracey values obtained from notched specimens for application to predict the behaviour of sharp cracks has lead us to define the Rice and Tracey damage parameter at failure using a range of fracture toughness specimens that sample a range of constraint levels.

In order to calibrate the critical local approach criteria, fracture toughness must be obtained experimentally from at least two constraint conditions. In this work, the local approach
criteria were calibrated from experimental data obtained at three different constraint conditions; using SEN(B)_S, SEN(B)_D and SEN(T)_D to investigate ductile failure in the pipe material, and 25mmCT, 15mmCT and 10mmCT to investigate ductile failure in the plate material.

The local approach parameters were calculated in the first row of elements ahead of the modelled defect. The variation of parameter along the crack front, through thickness is fitted to a curve. The local approach is then averaged through thickness using equation (90) and (91). The averaging methodology is discussed in more details in section 4.6.

\[
L_{Ac} = \left( \frac{\sum_{i=0}^{4} L_{A1} \times (z/t)_i}{2 + L_{A} \times (z/t)_5} \right) / 9
\]

(90)

\[
(z/t)_1 = 0.1 \\
(z/t)_2 = 0.3 \\
(z/t)_3 = 0.5 \\
(z/t)_4 = 0.7 \\
(z/t)_5 = 0.9
\]

(91)

with z the position through thickness, t the half thickness of the specimen, and LA the local approach parameter being calibrated.

In order to define the critical parameter at fracture, the averaged parameter is plotted against the values of the J-integral at the centre of the specimen. The critical parameter was defined as the averaged local approach at the value of J_{0.2BL} defined experimentally. Small variations in the critical value of the parameter derived from individual tests can occur due to scatter in the test data and due to uncertainty in defining the J_{0.2BL} value.
Chapter 4  Material Characterisation

This section presents the characterisation of the microstructure and tensile properties of the two stainless steel batches. The tensile properties are defined and the yield strength, flow stress and plastic stress strain curves are derived for use in the Finite Element Analyses (FEA) and Failure Assessment Diagram (FAD) analyses. The variation in material properties with the plate material are assessed using the hardness mapping

4.1.  Metallographic

4.1.1.  Pipe Material

The pipe material’s microstructure is shown in Figure 53 and Figure 54 in the Axial (A) and Radial (R), and Hoop (H) and Radial (R) directions. The grain size was measured in each direction using optical images at a magnification of 100 times. The average grain size was measured to be 30±7μm, using the line-intercept method. The micrographs show that this is a fine grained material with homogenous repartition of the grains in terms of grain size and shape.

![Figure 53](image1.jpg)  Figure 53 micrograph of pipe material in the A-R orientation – magnification 100 times

![Figure 54](image2.jpg)  Figure 54: micrograph of pipe material in the H-R orientation – magnification 100 times
4.1.2. **Plate Material**

The plate material microstructure is shown in Figure 55, Figure 56 and Figure 57 in the L-T, L-S and T-S orientation respectively. The grain size was measured in each direction using optical images at a magnification of 100 times. The average grain size in the T-L, L-S and T-S orientation was found to be 150 ± 15, 100 ± 10 and 75 ± 10 μm using the line intersect methodology. The micrographs show that there is a significant anisotropy in the grain size and shape, which is consistent with a cold worked material.

![Figure 55: micrograph of plate material in the L-S orientation – magnification 100 times](image1)

![Figure 56 micrograph of plate material in the T-S orientation – magnification 100 times](image2)

![Figure 57: micrograph of plate material in the T-L orientation – magnification 100 times](image3)
4.2. Tensile testing

4.2.1. Pipe Material

4.2.1.1. Experimental results
Tensile testing was conducted as defined in section 2.1.3. The load and gauge length extension was monitored throughout the test and the engineering stress strain curves obtained in the Hoop directions and are presented in Figure 58. Yield strength, Flow strength, Ultimate Tensile Strength and strain at fracture are presented in Table 10. The tensile results show that the material has consistent behaviour through the pipe thickness, with a standard deviation smaller than 1%. The material meets the minimum material properties requirements defined by ASTM 250 for 304 Stainless Steel [97] for yield strength, strain at fracture and ultimate tensile strength (UTS) as shown in Table 10. The low yield and high toughness is typical for 304(L) material, providing high fracture toughness and work hardening capability sought for safety critical components.

![Graph: Engineering Stress vs. Strain - Pipe material - Tensile specimen tested at room temperature](image-url)

Figure 58: Engineering Stress vs. Strain - Pipe material - Tensile specimen tested at room temperature -
New insights into the competition between ductile tearing and plastic collapse

Table 10: Tensile behaviour - Pipe material tested at room temperature - Comparison with ASTM 250

<table>
<thead>
<tr>
<th>Material properties</th>
<th>$\sigma_{0.2}$ (MPa)</th>
<th>$\sigma_{\text{flow}}$ (MPa)</th>
<th>UTS (MPa)</th>
<th>$\varepsilon_{\text{failure}}$ (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASTM 250 S30403</td>
<td>$\geq 170$</td>
<td>$\geq 327$</td>
<td>$\geq 485$</td>
<td>$\geq 40$</td>
</tr>
<tr>
<td>Tensile 1</td>
<td>258</td>
<td>420</td>
<td>582</td>
<td>60</td>
</tr>
<tr>
<td>Tensile 2</td>
<td>253</td>
<td>417</td>
<td>581</td>
<td>62</td>
</tr>
</tbody>
</table>

4.2.1.2. Defining material behaviour for FEA analysis

The true stress vs. true strain behaviour of the pipe material was derived using the methodology defined in section 2.1.3. The true stress vs. true strain behaviour (Figure 59) was used to define the plastic deformation properties in the pipe material FEA analyses. The Young’s modulus was set at 200GPa [98], Poisson’s ratio ($\nu$) = 0.29 and $\sigma_y$ as the average of the tensile tests at 255MPa. The plastic deformation curve was defined using the averaged true-stress vs. true strain curve, with plasticity onset defined as the point where the curve deviated from the elastic line. This occurred at a stress of 84 MPa in the pipe material. The test data were extrapolated using a linear extrapolation methodology to define the true stress vs. true strain behaviour at large strain.

Figure 59: True stress vs. True strain behaviour, 304 SS pipe material, tensile specimen tested at room temperature
4.2.2. **Plate Material**

Tensile testing for the plate material was conducted as defined in section 2.1.3. Engineering stress strain curves obtained at the different locations in the T-S plane in the plate with the positions shown in Table 6. The engineering stress vs. strain curves obtained are presented in Figure 60.

![Engineering stress vs. strain, 304SS plate material, tested at room temperature](image)

*Figure 60: Engineering stress vs. strain, 304SS plate material, tested at room temperature*

Yield strength, flow strength, UTS and strain at fracture are presented in Table 11. The tensile results show that the material properties are dependent on the position of the specimen in the plate. The material meets the minimum yield strength, flow stress and UTS requirements defined by ASTM for 304 Stainless Steel. The strain capability requirements are marginally met at all locations analysed except for tensile specimen 4, which fails below the minimum strain capability required. The variation in material properties is typical of cold work materials.
New insights into the competition between ductile tearing and plastic collapse

Table 11: Tensile behaviour - Plate material tested at room temperature - Comparison with ASTM 250 [97]

<table>
<thead>
<tr>
<th>Material properties</th>
<th>$\sigma_{0.2}$ (MPa)</th>
<th>$\sigma_{\text{flow}}$ (MPa)</th>
<th>UTS (MPa)</th>
<th>$\varepsilon_{\text{failure}}$ (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASTM 250 S30403</td>
<td>$\geq 170$</td>
<td>$\geq 327$</td>
<td>$\geq 485$</td>
<td>$\geq 40$</td>
</tr>
<tr>
<td>Tensile 1</td>
<td>522</td>
<td>618</td>
<td>713</td>
<td>41</td>
</tr>
<tr>
<td>Tensile 2</td>
<td>424</td>
<td>553</td>
<td>681</td>
<td>45</td>
</tr>
<tr>
<td>Tensile 3</td>
<td>476</td>
<td>587</td>
<td>698</td>
<td>40</td>
</tr>
<tr>
<td>Tensile 4</td>
<td>479</td>
<td>586</td>
<td>692</td>
<td>39</td>
</tr>
<tr>
<td>Tensile 5</td>
<td>422</td>
<td>553</td>
<td>684</td>
<td>43</td>
</tr>
</tbody>
</table>

Compact Tension specimens were extracted as close to the centre of the plate as was practical with material availability. Tensile properties obtained at the centre of the plate (Tensile 2 and Tensile 4) were chosen as representative of the material properties of the Compact Tensions specimen in the Finite Element Analysis. The plastic deformation curve was defined using the averaged of tensile 2 and 5 true-stress vs. true strain curve, with plasticity onset defined as the point where the curve deviated from the elastic line.

Figure 61: True stress vs. True strain behaviour, 304 SS plate material, tensile specimen tested at room temperature
Material variability was monitored on a specimen to specimen basis using a combination of the load vs. displacement behaviour measurements and Digital Image Correlation (see section 2.3. ) results. The methodology is described in more details in section 4.4.2.2. 
4.3. **Hardness testing**

A hardness map of the plate in the T-S orientation was performed as defined in section 2.1.4. The hardness variation in the plate is presented in Vickers Hardness contour plots in Figure 62 obtained using a 20kg load. A variation of 65 HV20 was observed through the plate, representing 25% difference between the centre and the surface of the plate. The Vickers Hardness variation through the plate was extrapolated into yield strength using equation (92) and presented in Figure 63 in correlation with the positioning of the tensile specimen in the plate[92]. The results are in good agreement with the yield stress obtained through tensile testing with less than 10% deviation between the two measurements. Higher hardness and yield strength values are observed in the regions close to the surface of the plate, which is consistent with a rolled material.

\[
\sigma_y = 2.7 \times HV_{20} - 125 \quad (92)
\]

![Hardness variation in T-S orientation of the 304SS plate material - HV20](image-url)
New insights into the competition between ductile tearing and plastic collapse

Figure 63: Yield strength variation in $T$-$S$ orientation of the 304SS plate material calculated from the hardness measurements
4.4. Fracture toughness testing

This section presents the results obtained from the fracture toughness tests for the pipe and plate material.

4.4.1. Pipe Material testing

4.4.1.1. Fracture toughness testing

Figure 64 illustrates the initial crack length observed in the SEN(B) specimen. Small amounts of tunnelling of the fatigue crack was observed (Figure 64, Figure 65, Figure 66), which led to a slight deviation from nominal initial crack length, with an average deviation of 9.5% from the nominal value for the deep cracked bend specimen, 5.8% for the deep cracked tension specimen and 7.2% for the shallow cracked bend specimen. Final crack lengths profile and the nine point average are presented in Figure 67, Figure 68 and Figure 69. Uneven crack growth were observed in all SEN(B) specimen. This may be due to the loss of out-of-plane constraint due to the large plastic deformation at initiation combined to the lack of side-grooving. Average initial \( a_{\text{initial}} \) and final \( a_{\text{final}} \) crack lengths obtained using the nine point average methodology are presented in Table 12, Table 13 and Table 14. Load displacement trace are presented in Figure 70, Figure 71 and Figure 72 for the SEN(B)\_D, SEN(B)\_S and SEN(T)\_D specimen respectively. Small amounts of scatter is observed in all tests, with deviation from the mean of 5%, 7% and 9% for the SEN(B)\_D, SEN(B)\_S and SEN(T)\_D specimen respectively.

![Figure 64](image-url)
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Figure 65: initial fatigue crack length through specimen thickness - Deep cracked Single Edge Crack Tension specimen

Figure 66: initial fatigue crack length through specimen thickness - Deep cracked Single Edge Crack Bend specimen
New insights into the competition between ductile tearing and plastic collapse

Figure 67: (a) measured and (b) averaged using the 9 point average scheme, final crack length - Deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 multi-specimen fracture toughness testing methodology

Figure 68: (a) measured and (b) averaged using the 9 point average scheme, final crack length - Deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 multi-specimen fracture toughness testing methodology
Figure 69: (a) measured and (b) averaged using the 9 point average scheme, final crack length - Deep cracked Single Edge Crack Bend specimen – tested at room temperature – ESIS P2-92 multi-specimen fracture toughness testing methodology

Figure 70: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_S specimen - tested according to p2-92 – displacement controlled 0.2mm/min - room temperature
New insights into the competition between ductile tearing and plastic collapse

Figure 71: Load vs. crack mouth opening displacement (CMOD) for SEN(T)_D specimen - tested according to p2-92 – displacement controlled 0.2mm/min - room temperature

Figure 72: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_D specimen - tested according to p2-92 – displacement controlled 0.2mm/min - room temperature
New insights into the competition between ductile tearing and plastic collapse

Table 12: SEN(B)_D initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>SEN_D 1.5</th>
<th>SEN_D 2.5</th>
<th>SEN_D 1.1</th>
<th>SEN_D 2.6</th>
<th>SEN_D 2.3</th>
<th>SEN_D SG1</th>
<th>SEN_D 2.4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>0.167</td>
<td>0.166</td>
<td>0.318</td>
<td>0.453</td>
<td>0.582</td>
<td>0.613</td>
<td>1.252</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.548</td>
<td>0.536</td>
<td>0.552</td>
<td>0.551</td>
<td>0.551</td>
<td>0.527</td>
<td>0.548</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.557</td>
<td>0.559</td>
<td>0.614</td>
<td>0.551</td>
<td>0.551</td>
<td>0.558</td>
<td>0.556</td>
</tr>
</tbody>
</table>

Table 13: SEN(T)_D initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>SEN(T)_D 2.2</th>
<th>SEN(T)_D 1.3</th>
<th>SEN(T)_D 1.7</th>
<th>SEN(T)_D 2.1</th>
<th>SEN(T)_D 1.2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>0.442</td>
<td>0.611</td>
<td>0.937</td>
<td>0.806</td>
<td>1.316</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.553</td>
<td>0.540</td>
<td>0.518</td>
<td>0.518</td>
<td>0.518</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.531</td>
<td>0.571</td>
<td>0.565</td>
<td>0.559</td>
<td>0.584</td>
</tr>
</tbody>
</table>

Table 14: SEN(B)_S initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>SEN(B)_S 1.7</th>
<th>SEN(B)_S 1.4</th>
<th>SEN(B)_S SG2</th>
<th>SEN(B)_S 1.3</th>
<th>SEN(B)_S 2.1</th>
<th>SEN(B)_S 2.6</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>0.246</td>
<td>0.329</td>
<td>0.525</td>
<td>0.674</td>
<td>1.320</td>
<td>1.492</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.220</td>
<td>0.212</td>
<td>0.212</td>
<td>0.215</td>
<td>0.214</td>
<td>0.214</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.254</td>
<td>0.228</td>
<td>0.224</td>
<td>0.281</td>
<td>0.289</td>
<td>0.291</td>
</tr>
</tbody>
</table>

J was calculated from the area under the load vs. CMOD curves using the methodology described in section 2.2. The η factors calculated using FEA in section 3.1. were used to correlate the area under the curve with the J-integral. The J-Resistance curves obtained are presented in Figure 73.

Initiation toughness is defined using two definitions. J_{0.2} represents the value of the crack driving force after 0.2 mm of crack tip extension. J_{0.2BL} represents the value of the crack
New insights into the competition between ductile tearing and plastic collapse

driving force after 0.2 mm of crack extension corrected for crack tip blunting using equation (93). \( J_{0.2BL} \) represents the value of crack extension by void growth and coalescence. In ductile materials such as 304 stainless steels, large amounts of crack tip blunting are observed and the J-Resistance curves shown in Figure 73 follow the blunting line up to values of crack driving force over 1450 kJm\(^{-2}\).

\[
J = 3.75R_m\Delta a
\] (93)

In order to maintain geometrical and size independence of J, validity limits are set by testing standards [6, 12, 13] to maintain high constraint conditions. Validity limits were defined using FEA methodologies to ensure that the crack tip stress conditions do not deviate more than 10% from small scale yielding. In order to meet these validity limits, large specimen sizes are required, which are not practical due to limited material availability. Validity limits as well as initiation toughness are presented in Table 15.

![Figure 73: J-Resistance curves for SEN(B)_S, SEN(D)_D and SEN(T)_D specimen, room temperature, Displacement controlled 0.2mm/min](image)

130
New insights into the competition between ductile tearing and plastic collapse

The measured fracture toughness was found to be over five times higher than the validity limits defined by the fracture toughness test standards. Furthermore, some sensitivity of $J_{0.2BL}$ on constraint was measured. The material toughness measured in these experiments was obtained from low constraint conditions and do not represent lower bound solutions. They are therefore non-conservative and not applicable for structural integrity assessment purposes.

Table 15: $J$-Validity limits according to ASTM [12], BS7448 [6] and ESIS P2-92[13] and initiation toughness for SEN(B)$_S$, SEN(B)$_D$ and SEN(T)$_D$ as defined by the blunting line at 0.2mm crack extension, $J_{0.2}$ and $J_{0.2BL}$

<table>
<thead>
<tr>
<th></th>
<th>SEN(T) Deep crack</th>
<th>SEN(B) Shallow crack</th>
<th>SEN(B) Deep crack</th>
</tr>
</thead>
<tbody>
<tr>
<td>$J$ Validity Limit (kJm$^{-2}$) ESIS P2-92</td>
<td>n/a</td>
<td>n/a</td>
<td>210 (B)</td>
</tr>
<tr>
<td>$J$ Validity Limit (kJm$^{-2}$) ASTM 1820</td>
<td>n/a</td>
<td>n/a</td>
<td>127 (B)</td>
</tr>
<tr>
<td>$J$ Validity Limit (kJm$^{-2}$) BS</td>
<td>n/a</td>
<td>n/a</td>
<td>105 (B)</td>
</tr>
<tr>
<td>Blunting Line 0.2mm</td>
<td>435</td>
<td>435</td>
<td>435</td>
</tr>
<tr>
<td>$J_{0.2}$ (kJm$^{-2}$)</td>
<td>520</td>
<td>600</td>
<td>520</td>
</tr>
<tr>
<td>$J_{0.2BL}$ (kJm$^{-2}$)</td>
<td>2430</td>
<td>2050</td>
<td>1530</td>
</tr>
</tbody>
</table>

4.4.1.2. Digital Image Correlation

The development of plasticity within the un-cracked ligament was monitored during each test using digital image correlation (section 2.3.). The evolution of the derived equivalent plastic strains during loading is presented in Appendix 1-1, 1-2 and 1-3 for specimen SEN(B)$_S$, SEN(B)$_D$ and SEN(T)$_D$ respectively. The contours for equivalent plastic strains of 0.002 are presented in light blue and contours for equivalent plastic strains of 0.02 are presented in red.

In the SEN(B)$_D$ specimen, plasticity develops first in the tensile region ahead of the crack tip and then starts developing in the compressive region just ahead of the loading pin. Plastic strain develop in two bow shapes. The yielding of the un-cracked ligament on the surface of the specimen occurs at a value of $L_r = 0.85$, where $L_r = P/P_L$. $P$ is the load applied and $P_L$ is the limit load calculated using FEA, section 1.1.. This occurs at a $J$ integral value of 75 kJm$^{-2}$. A value of 2% strain was chosen to monitor the development of extensive plasticity. Extensive plasticity develops in a similar pattern and the un-cracked
ligament has yielded by over 2% strain before the J-Resistance curve deviates from the blunting line.

In the SEN(B)_S specimen, plasticity develops in the tensile region ahead of the crack tip develop into the un-cracked ligament as well as into the top surface. Compressive plasticity develops ahead of the loading pin into the un-cracked ligament in a similar manner as in the SEN(B)_D specimen. Complete yielding of the un-cracked ligament occurs at an $L_r = 0.9$ on the surface of the specimen. This occurs at a J around 60 kJ/m$^2$. Extensive plasticity develops in a similar pattern to yielding. At the initiation of ductile tearing, the specimen has undergone extensive plasticity.

In the SEN(T)_D, plasticity develop in a tensile manner ahead of the defect in a two stretched ovoid patterns. Plasticity develops faster on the bottom half of the specimen that the top half. This is most probably due to induced bending from the friction grip loading. The top grip is held by a less rigid component allowing it some movement. Yielding of the remaining ligament occurs at an $L_r = 0.8$. This corresponds to a J of 80kJm$^2$, i.e. well below the measured value at the initiation of ductile tearing.

The limit load was analytically defined from the maximum load bearing capability of the specimen with elastic perfectly plastic material characteristic with onset of plasticity defined from the experimentally defined yield ($\sigma_y$). Yielding of the un-cracked ligament on the surface occurs before limit load in all specimens. This is due to the fact that the DIC technique monitored the plasticity on the surface of the specimen, which is under plane stress conditions and therefore will exhibit more extensive plasticity than at the centre-line of the specimen which is under plane strain conditions, and the fact that the upper bound limit load was defined analytically.

Plasticity on the surface of the specimen is good indication of constraint loss, as the J integral definition is independent of geometry and loading conditions only for small amounts of plastic deformation through the un-cracked ligament, when the SSY solution can accurately describe the opening stress ahead of the crack tip. As plasticity develops across the un-cracked ligament, the opening stress ahead of the crack deviates from the SSY solution, and the J-integral becomes dependent on the specimen geometry and loading.
New insights into the competition between ductile tearing and plastic collapse

conditions. Hence, it can no longer be considered as a material property, and is no longer transferable to other geometries.

The approximation of the plastic zone size under contained plasticity conditions and mode I loading given in equation (94) for plane stress conditions and equation (95) for plane strain conditions can provide a general estimation of the difference between the size of the plastic zone at the centre of the specimen compared to the surface of the specimen.

\[ r_p = \frac{1}{4\pi} \left( \frac{K_I}{\sigma_y} \right) \left[ 1 + \cos(\theta) + \frac{3}{2} \sin^2(\theta) \right] \]  \hspace{1cm} (94)

\[ r_p = \frac{1}{4\pi} \left( \frac{K_I}{\sigma_y} \right) \left[ (1 - 2\nu)^2(1 + \cos(\theta)) + \frac{3}{2} \sin^2(\theta) \right] \]  \hspace{1cm} (95)

We can approximate the conditions at the centre of the specimen to be under plane strain conditions up to the point where plasticity reaches the back face of the specimens and the surface of the specimen is under plane stress conditions. At a specific loading defined by \( K \), the plane strain plastic zone with be roughly \( 1/6^{th} \) smaller than the plastic zone monitored.
4.4.2. **Plate Material**

4.4.2.1. **Fracture toughness testing**

Small amounts of tunnelling of the fatigue crack was observed in the CT specimens (Figure 74, Figure 75, Figure 76), which lead to a slight deviation from nominal initial crack length of \(a/W = 0.5\), with an average deviation 5% for the 25mmCT, 5.8% for the 15mmCT and 7.2% for the 10mmCT. Initial and final crack lengths are presented in Table 16, Table 17 and Table 18.

Crack propagation was calculated from the compliance of unloads conducted during the tests. The unloading compliance data were converted into equivalent crack lengths as described in section 2.2.2.

The load vs. CMOD curves are presented in Figure 79, Figure 78 and Figure 77 for the 25mmCT, 15mmCT and 10mmCT respectively. Small amounts of scatter are observed, with a deviation from the average of 3%, 2.5% and 2% for the 25mmCT, 15mmCT and 10mmCT respectively.

![Figure 74: initial fatigue crack length through specimen thickness – 25mmCT](image.png)

Figure 74: initial fatigue crack length through specimen thickness – 25mmCT
New insights into the competition between ductile tearing and plastic collapse

Figure 75: initial fatigue crack length through specimen thickness – 15mmCT

Figure 76: initial fatigue crack length through specimen thickness – 10mmCT

Table 16: 25mmCT initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>1_25</th>
<th>7_25</th>
<th>9_25</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>4.548</td>
<td>2.387</td>
<td>2.387</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.555</td>
<td>0.561</td>
<td>0.561</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.645</td>
<td>0.609</td>
<td>0.609</td>
</tr>
</tbody>
</table>

135
New insights into the competition between ductile tearing and plastic collapse

Table 17: 15mmCT initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>3_15</th>
<th>4_15</th>
<th>6_15</th>
<th>7_15</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>3.053</td>
<td>3.503</td>
<td>3.923</td>
<td>6.965</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.541</td>
<td>0.540</td>
<td>0.543</td>
<td>0.545</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.643</td>
<td>0.657</td>
<td>0.674</td>
<td>0.777</td>
</tr>
</tbody>
</table>

Table 18: 10mmCT initial crack length, final crack length and crack extension measured using a travelling optical microscope

<table>
<thead>
<tr>
<th></th>
<th>1_10</th>
<th>4_10</th>
<th>6_10</th>
</tr>
</thead>
<tbody>
<tr>
<td>Δa, mm</td>
<td>2.839</td>
<td>3.836</td>
<td>3.019</td>
</tr>
<tr>
<td>a/w initial</td>
<td>0.573</td>
<td>0.569</td>
<td>0.566</td>
</tr>
<tr>
<td>a/w final</td>
<td>0.715</td>
<td>0.761</td>
<td>0.666</td>
</tr>
</tbody>
</table>

Figure 77: Load vs. CMOD – Compact Tension specimen - 25mmCT – B=25mm – W=2B – tested at room temperature following ASTM 1820
New insights into the competition between ductile tearing and plastic collapse

Figure 78: Load vs. CMOD – Compact Tension specimen - 15mmCT – B=15mm – W=2B – tested at room temperature following ASTM 1820

Figure 79: Load vs. CMOD – Compact Tension specimen - 10mmCT – B=10mm – W=2B – tested at room temperature following ASTM 1820

137
4.4.2.2. Material variation through the length of the plate

Variation in yield strength was observed in the L-T plane of the plate material. Although the Compact Tension specimens were specified to be manufactured from the central section of the plate (Figure 38) the effect of the yield strength on the specimen load vs. displacement was investigated. The following analysis is based on the assumption that, as all the specimens have the same geometry, the normalised load \((L_r)\) vs. the normalised displacement \((\text{CMOD}/b_0)\) should be the same. Any deviation will pick out material anisotropy.

In order to validate this assumption and to provide a reference curve, the normalised load \((L_r)\) vs. displacement \((\text{CMOD}/b_0)\) was obtained, for each geometry (i.e. 25mmCT, 15mmCT & 10mmCT), using 3D Finite Element Analysis. The three geometries were modelled with lengths of \(a/W=0.55\) and yield strength \(\sigma_y = 424\) MPa. The Limit Load was calculated for each geometry using a RIKS analysis (section1.1.), and the results were found to be consistent with the plane strain Limit Load defined in R6. The normalised load \((L_r)\) vs. displacement \((\text{CMOD}/b_0)\) was plotted in Figure 80. The curves were found to lie on top of each other, validating our approach.

For the experimental measurements, the Limit Load was calculated using the plane strain limit loads solution defined in the R6 document [35]. The limit load solution is defined for a specific geometry (i.e. Compact Tensions) and is proportional to specimen thickness \((t)\), the specimen width \((W)\), the crack length \((a)\), and the material’s yield strength. The specimen thickness, width and initial crack length are known as they have been measured experimentally. The crack propagation was derived from the unloading curves, and can be correlated to the corresponding load. Therefore, any variation between the different specimens normalised load \((L_r)\) vs. normalised displacement \((\text{CMOD}/b_0)\) can be attributed to a variation in yield strength.

The normalised load \((L_r)\) vs. the displacement \((\text{CMOD}/b_0)\) for the 25mmCT, 15mmCT and 10mmCT are plotted in Figure 80. The crack propagation was accounted for in the Limit Load calculation. A representative sample of the specimens tested is plotted in the graph for clarity purposes.
In order to show that the variation in the normalised behaviour of the Compact Tension specimen is not due to the displacement normalisation methodology chosen, the repartition of strains on the surface of the specimen, in the un-cracked ligament, as measured using Digital Image Correlation (DIC), was investigated at a normalised displacement (CMOD/b₀) of 0.1. Figure 80 shows that, although there is a significant variation in the normalised load (Lᵣ) vs. the displacement (CMOD/b₀) behaviour between all the specimens, the strains at the normalised displacement investigated are similar (i.e. the strain measured on the surface of the 25mmCT occupies the same proportion of the un-cracked ligament as the strain on the surface of the 15mmCT and the 10mmCT). Furthermore, Figure 80 illustrates the fact that the strains measured experimentally on the surface of the specimens (i.e. Digital Image Correlation results of obtained for specimen 7-25mmCT, 3-15mmCT & 4-10mmCT and presented in Figure 80) are comparable to the strains obtained analytically on the surface of the 3D FEA models. This is a qualitative comparison and some small variation in shape of the plastic zone can be observed. This observation reinforces the argument that the variation in normalised load (Lᵣ) vs. the displacement (CMOD/b₀) can be attributed to a variation in yield strength.

Figure 80: normalised Load vs. CMOD - 25mmCT, 15mmCT, 10mmCT – Yield = 424 MPa - room temperature - Opening strain observed using DIC and FEA at a normalised distance of 0.1
New insights into the competition between ductile tearing and plastic collapse

The effective yield strength of each specimen was extrapolated using the reference curves obtained using the 3D Finite Element Analysis. The yield strength was modified in the definition of the Limit Load, so that the normalised load ($L_r$) vs. the displacement (CMOD/b₀) lies on the reference curves. No deviation from the reference curve should occur before crack initiation. It is important to note that crack propagation is accounted for in the derivation of the limit load for the experimental results. As the Finite Element Analysis conducted was for a static crack, this approach works up to initiation.

The corrected yield strengths obtained are presented in Table 19 for each specimen tested. The modified normalised loads ($L_r$) vs. the displacements (CMOD/b₀) curves are presented in Figure 81. The curves lie on top of each other up to the point of crack initiation in the 15mmCT and 10mmCT specimen. Experimental and analytical solutions are consistent for a wider range of strains for the 25mmCT.

<table>
<thead>
<tr>
<th>Specimen Identification</th>
<th>Corrected Yield strength (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1_25mmCT</td>
<td>424</td>
</tr>
<tr>
<td>7_25mmCT</td>
<td>380</td>
</tr>
<tr>
<td>9_25mmCT</td>
<td>430</td>
</tr>
<tr>
<td>3_15mmCT</td>
<td>385</td>
</tr>
<tr>
<td>4_15mmCT</td>
<td>400</td>
</tr>
<tr>
<td>5_15mmCT</td>
<td>390</td>
</tr>
<tr>
<td>6_15mmCT</td>
<td>380</td>
</tr>
<tr>
<td>2_10mmCT</td>
<td>400</td>
</tr>
<tr>
<td>4_10mmCT</td>
<td>395</td>
</tr>
<tr>
<td>6_10mmCT</td>
<td>410</td>
</tr>
</tbody>
</table>
New insights into the competition between ductile tearing and plastic collapse

**Figure 81**: normalised Load vs. CMOD - 25mmCT, 15mmCT, 10mmCT – Yield = 424 MPa - room temperature - Opening strain observed using DIC and FEA at a normalised distance of 0.1

It was assumed that the variation in yield strength has no effect on hardening exponent of stress-strain curve. This assumption is consistent with the measured stress-strain curves. The specimen specific true-stress vs. true-strain curves were therefore extrapolated by translating the reference stress vs. strain curve obtained from specimen 2 in the y-direction.
4.4.2.3. J-Resistance Curves

The J-resistance curves were calculated using the unloading compliance methodology defined in ASTM 1820 [12] and described in section 2.2.2. The experimentally derived J-integral is proportional to the specimen thickness, the crack length and the area under the plastic-load vs. plastic-displacement of the curve. The contribution of the yield strength on the J-integral is through the area under the plastic part of the load vs. displacement curve. Small amounts of variation in specimen specific J-resistance curves were observed, with a maximum deviation from the average specimen specific J-resistance curve of 1% for the 25mmCT specimens, 4% for the 15mmCT specimens and 4% for the 10mmCT specimens. The variations observed are still within acceptable experimental error. The larger deviation observed in the smaller specimen can be explained by the increased sensitivity of the J-integral to the measured load. The variation in yield strength observed in the specimen has no significantly measurable effect on the measured J-Resistance curve and the resulting initiation toughness measurements.

In the 15mmCT and 10mmCT, a negative crack growth was measured using the unloading compliance methodology. This is the product of the unloading compliance technique, and it is a phenomenon which is well documented [99]. The negative crack growth may be due to a range of effects, such as pin/specimen friction, misalignment in the loading train, balancing and physical blunting behaviour effects. No plastic deformation around the pin was observed, and no significant plastic deformation of the pin could be seen. Due to the large hardening capability of the material, and the large plastic strains observed using DIC, one of the probable cause for the negative blunting measured is the compressive residual stress caused by the plastic zone around the crack tip during unloading.

The offset technique defined by Chuang-Sung Seok [99] was used to correct for the negative crack propagation measured, where the J vs. Δa curve was translated along the Δa direction by the distance of the most negative crack propagation measurement to the
New insights into the competition between ductile tearing and plastic collapse

blunting line. The conventional power-law regression line methodology defined in ASTM 1820[12] was used to derive the curve fits for each specimen J-Resistance curve. An average of the tests was used to define the specimen specific J-resistance curves presented in Figure 82. The power-law regression line from the average J-Resistance curves can be found in equation (96), (97) and (98) for the 25mmCT, 15mmCT and 10mmCT respectively.

\[
J = 1235.2(\Delta a)^{0.7193} \tag{96}
\]
\[
J = 497.54(\Delta a)^{0.4784} \tag{97}
\]
\[
J = 350(\Delta a)^{0.6243} \tag{98}
\]

A reduction in J-resistance curve slope is observed with a reduction in specimen size. The measured initiation toughness \(J_{0.2BL}\) is 735 kJm\(^{-2}\), 284 kJm\(^{-2}\), and 151 kJm\(^{-2}\), for the 25mmCT, 15mmCT and 10mmCT respectively.

Validity limits as well as initiation toughness are presented in Table 20. The validity limits are calculated from the average initial crack lengths and the material properties measured at the centre of the plate obtained from tensile 2. The test standards provide two size limitations, the first proportional to the specimen un-cracked ligament and the second proportional to the specimen thickness. In all cases, the validity limit proportional to the specimen thickness provided the lowest validity limit, and is presented in Figure 82.
New insights into the competition between ductile tearing and plastic collapse

Figure 82: J-Resistance curves – CT specimen B=25mm, 15mm and 10mm – W=2B - Average of 3 tests per specimen size – ASTM 1820 [12]

Table 20: J-Validity limits according to ASTM [12], BS7448 [6] and ESIS P2-92 and initiation toughness for 25mmCT, 15mmCT and 10mmCT as defined by the blunting line at 0.2mm crack extension, \( J_{0.2} \) and \( J_{0.2BL} \)

<table>
<thead>
<tr>
<th>Initiation values</th>
<th>25 mm CT SG</th>
<th>25 mm CT</th>
<th>15 mm CT</th>
<th>10 mm CT</th>
</tr>
</thead>
<tbody>
<tr>
<td>J Validity Limit (kJm(^{-2})) ESIS P2-92</td>
<td>553</td>
<td>610</td>
<td>380</td>
<td>238</td>
</tr>
<tr>
<td>J Validity Limit (kJm(^{-2})) ASTM</td>
<td>424</td>
<td>468</td>
<td>291</td>
<td>183</td>
</tr>
<tr>
<td>J Validity Limit (kJm(^{-2})) BS</td>
<td>276</td>
<td>305</td>
<td>190</td>
<td>119</td>
</tr>
<tr>
<td>( \Delta a ) Validity Limit (kJm(^{-2})) ESIS P2-92</td>
<td>0.85</td>
<td>2.21</td>
<td>1.37</td>
<td>0.86</td>
</tr>
<tr>
<td>( \Delta a ) Validity Limit (kJm(^{-2})) ASTM</td>
<td>2.13</td>
<td>5.51</td>
<td>3.43</td>
<td>2.15</td>
</tr>
<tr>
<td>( \Delta a ) Validity Limit (kJm(^{-2})) BS</td>
<td>0.85</td>
<td>2.2</td>
<td>1.4</td>
<td>0.86</td>
</tr>
<tr>
<td>( J_{0.2} ) (kJm(^{-2}))</td>
<td>345</td>
<td>388</td>
<td>230</td>
<td>128</td>
</tr>
<tr>
<td>( J_{0.2BL} ) (kJm(^{-2}))</td>
<td>575</td>
<td>735</td>
<td>284</td>
<td>151</td>
</tr>
</tbody>
</table>

The 25mmCT initiation toughness measurement lies beyond the specimen J-validity limits. The 15mmCT and 10mmCT initiation toughness measurements lie within the specimen J-validity limits. Nevertheless, none of the initiation toughness measurements obtained are
valid according to ASTM 1820 methodology, as no valid J measurements exist between 0.5mm and 1.5mm of measured crack growth.

In the 25mmCT specimen, crack initiation occurs before maximum load (Figure 83). The measured load and the derived normalized load increases with crack propagation (Figure 80 & Figure 83). In the 15mmCT and 10mmCT, crack propagation coincides with maximum load and the measured load decreases with crack propagation (Figure 83). The normalized load vs. normalized displacement flattens out at a normalized load value (load vs. corrected limit load) of 1.10 and 1.12 for the 10mmCT and the 15mmCT respectively (Figure 80).

![Figure 83: Load vs. CMOD – Compact Tension specimen -25mmCT, 15mmCT and 10mmCT — tested at room temperature following ASTM 1820 – with initiation points](image)

This has a significant impact on the measured J-Resistance curve and the corresponding fracture toughness, as the J-integral is proportional to the area under the load vs. displacement curve. The evolution from stable crack growth (in the 25mmCTs), with an increase in load required to propagate the crack, to unstable crack growth (in the 15mmCTs and 10mmCTs), with a reduction in load due to crack propagation, could explain why the 25mmCT J-Resistance curve is much steeper than the 15mmCT and
10mmCT J-Resistance curves, and hence the reduction in initiation toughness with reduction in specimen size.

4.4.2.4. Digital image correlation
The evolution of equivalent plastic strains with regards to the J-Resistance curve is shown in Figure 84, Figure 85 and Figure 86 for the 25mmCT, 15mmCT and 10mmCT, respectively. The variation in yield strength observed in specimens has no apparent effect on the equivalent plastic strains measured using DIC, as it is derived from surface displacements and is, as shown in section 2.3.2., independent of the measured yield strength.

Plasticity develops in a similar manner in all specimens. Large amounts of plastic deformation occurred before initiation in all cases. Plasticity develops in a bow shape from the tensile region ahead of the crack tip and the compressive region on the back face. The level of plasticity at initiation is similar in all three specimen geometries.

Figure 84: 1_25mmCT J-resistance curve – ASTM 1820 – tested at room temperature – Equivalent plastic strain obtained from Digital Image Correlation

146
New insights into the competition between ductile tearing and plastic collapse

Figure 85: 4_15mmCT J-resistance curve – ASTM 1820 – tested at room temperature – Equivalent plastic strain obtained from Digital Image Correlation

Figure 86: 4_10mmCT J-resistance curve – ASTM 1820 – tested at room temperature – Equivalent plastic strain obtained from Digital Image Correlation

147
4.5. **Conventional fracture mechanics:**

Conventional failure assessment methodologies that account for constraint dependence of fracture toughness require the definition of constraint effects using two parameter fracture mechanics. The J-T two parameter approaches is adequate for small amounts of plastic deformation in the crack propagation plane. The T-stress approach extends the validity of the J-integral by evaluating the second order term of the William expansion [95]. This approach is no longer accurate close and beyond the limit load.

The J-Q two parameter fracture mechanics approach is adequate for larger amounts of strains. It corrects for the deviation of the stress field from the SSY solution, at a specific normalized distance ahead of the crack tip, and hence is not limited by theoretical limitations to the crack tip stress field definition.

4.5.1. **J-Q locus**

The J-Q approach is based on the fact that in an infinite body, the variation in the normalised opening stress field from the SSY solution, due to constraint effects, is independent of the normalised distance ahead of the defect; as long as you lie beyond the finite strain region just ahead of the defect. This means that, as long as this assumption holds true, an analysis of the stress field far from the crack tip can still adequately define the crack tip stress field.

This assumption will hold true if, 1) the normalised distance of rσy/J=2 is still contained within the un-cracked ligament and 2) the region analysed is far enough from any specimen boundary or discontinuity, for the stress field to behave as if it was in an infinite body. As the normalised distance is proportional to the J-integral, large initiation toughness values can lead to issues regarding the validity of the above assumptions.

The normalised distance of rσy/J=2 was chosen by O'Dowd and Shih to lie beyond the regions of finite strains [21, 30] and within a region where the deviation from small scale yielding is independent of the normalised distance. For the material and fracture toughness values considered at the time, it was adequate and would still lie within a region proportional to the SSY stress field. Hence, the methodology could adequately describe the
stress field governing the fracture process. When analysing specimens and components that have failed under large strains conditions, the validity of the conventional definition of the normalised distance should be investigated.

4.5.2. **SEN specimen manufactured from the pipe material**

The fracture toughness specimen validity limits ensure conditions close to the SSY ahead of the defect remain close to SSY conditions. When non-valid fracture toughness measurements are analysed using the J-Q methodology, care should be taken to ensure that the normalised distance at which Q is defined, is still valid.

In the SEN fracture specimen, initiation toughness values of over five times the validity limit were measured. In the SEN(B)_S specimen, with a measured initiation toughness of \( J_{0.2BL} = 2050 \text{kJm}^{-2} \), the normalised distance \( r\sigma_0/J = 2 \) defined by O’Dowd and Shi represents a physical distance \( r = 16.4 \text{mm} \), which lies within the specimen compressive plastic zone close to the back face of the specimen. Similarly, in the SEN(B)_D specimen, with a measured initiation toughness of \( J_{0.2BL} = 1530 \text{kJm}^{-2} \), the normalized distance \( r\sigma_y/J = 2 \) represents a distance \( r = 12.2 \text{ mm} \), which lies beyond the back face of the specimen. Likewise, in the SEN(T)_D specimen, with a measured initiation toughness of \( J_{0.2BL} = 2430 \text{kJm}^{-2} \), the normalised distance \( r\sigma_y/J = 2 \) represents a physical distance \( r = 19.4 \text{mm} \), which also lies beyond the back face of the specimen. Hence, for this austenitic stainless steel, the normalised distance, \( r\sigma_y/J = 2 \), does not lie within the J-Q annulus and the J-Q approach fails to accurately describe the deviation between the stress field ahead of the crack from the reference stress distribution ahead of the crack tip.

The reference SSY solution for the pipe material was defined using the MBLMN model described in section 3.4. The evolution of normalised opening stress \( (\bar{\sigma}) \) against normalised distance ahead of the crack tip \( (\bar{F}) \) at initiation in the SEN(B)_S, SEN(B)_D and SEN(T)_D was calculated using the 3D models described in section 4.2.1.2. The values of \( J_{0.2BL} \) obtained experimentally for each specimen were correlated to the value of the analytically obtained J-integral at the centre plane of each model. Furthermore, the normalised opening stress was defined at the centre of the model. The variation of Q through thickness was not accounted for.
New insights into the competition between ductile tearing and plastic collapse

Figure 87 shows the crack tip stress field at the values of J that correspond with the initiation of ductile tearing. The results suggest that the finite strain region lies between the normalized distance of $0 < \sigma_y/J < 0.075$ in all specimen. Furthermore, Q becomes dependent on the normalised distance at a distance greater than $\sigma_y/J > 0.15$. In these cases, the J-Q annulus lies between $0.075 < \sigma_y/J < 0.15$.

A more appropriate distance at which to calculate the Q parameter at such large J conditions was therefore chosen to be $\sigma_y/J=0.1$.

![Graph](image)

**Figure 87:** Crack tip Stress field at the centre of the SEN(B)_S, SEN(B)_D and SEN(T)_D specimen at a value of J corresponding to initiation

Figure 88 shows the J-Q locus defined for this material at the centre of the specimens at initiation. The ductile initiation occurred at values of the Q parameter of -1.1, -1.6 and -2.3 for the SEN(B)_D, SEN(B)_S and SEN(T)_D respectively.

Although this provides an indication of the variation of fracture toughness under low constraint conditions, it does not provide any clear insight into high constraint fracture toughness behaviour.
New insights into the competition between ductile tearing and plastic collapse

Figure 88: Variation in constraint, as defined by a modified $Q$ parameter, at initiation of ductile tearing, for 304 Stainless Steel pipe material tested at ambient temperature

4.5.3. **CT specimen manufactured from the plate material**

In order to understand the effect of specimen size on fracture toughness, the J-Q locus was calculated for the CT specimen. The SSY solution for the plate material was defined using the MBLMN model described in section 3.4.

The evolution of normalized opening stress ($\overline{\sigma}$) against normalised distance ahead of the crack tip ($\overline{R}$) at initiation in the CT specimens was defined using the 3D models defined in section 3.3. The values of $J_{0.2BL}$ obtained experimentally for each specimen were compared to the value of $J$ at the centre of the specimen to define the initiation toughness conditions in all cases. Furthermore, the normalised opening stress at initiation was defined at the centre of the specimen. The variation in $Q$ through thickness was not accounted for. As the initiation toughness ($J_{0.2BL}$) measured in the plate material is lower than that observed in the SEN specimen, the conventional definition of the normalised value at which $Q$ is calculated could therefore be used. The relationship between initiation measured experimentally and the $Q$ parameter is presented in Figure 89.
New insights into the competition between ductile tearing and plastic collapse

Figure 89: Variation in constraint, as defined by the conventional $Q$ parameter, at initiation of ductile tearing, for 304 Stainless Steel plate material tested at ambient temperature

The reduction in specimen size results in an increase in constraint as defined by $Q$ at a specific value of $J$. The increase in initiation toughness with increase in specimen size observed in the tests, lead to a decreased constraint condition at initiation. This is consistent with the conventional observation that a material will initiate at a higher value of $J$ when under lower constraint condition, due to the reduction in opening stress for a specific value of $J$.

4.5.3.1. Interim conclusions:
The normalised distance $r\sigma_y/J=2$ defined by O’Dowd and Shi is no longer valid when analysing fracture toughness measurements obtained far beyond the specimen validity limit.
In low yield, high toughness specimen, the J-Q analysis defines the initiation toughness constraint dependence for low constraint conditions. As we cannot obtain high constraint failure conditions, it does not provide any insight into the high constraint initiation toughness. Large specimens are still required in order to obtain valid toughness measurements.
4.6. Rice & Tracey and Work of fracture Local approaches

Initiation toughness was shown to be constraint sensitive in section 4.5. for both pipe and plate materials. This can lead to:

- An over-conservative failure prediction when analysing a structure under lower constraint condition. This occurs when applying the specimen validity limit as measured fracture toughness, as structural integrity assessment best practices would require.
- A non-conservative failure prediction when analysing a structure under higher constraint condition, when applying the non-valid, measured fracture toughness.
- A non-conservative failure prediction when applying the \( J \)–\( Q \) constraint correction, with the constraint sensitivity of the initiation toughness calculated at a non-valid normalised distance.

Two parameter fracture mechanics are conventionally used to define the constraint dependence of initiation toughness. The \( J \)–\( Q \) locus obtained experimentally, using the modified methodology defined in this work, for the pipe material in section 4.5.2., describes the constraint dependence of initiation toughness accurately for \( Q < -1 \). In order to define the constraint dependence of initiation toughness under lower constraint conditions, a large volume of material would be required to machine sufficiently large fracture mechanics specimen, to obtain valid measurements of initiation toughness.

In this section, constraint independent failure criteria are explored. Three local approaches were selected to define failure by ductile tearing, the high constraint Rice & Tracey criterion (equation (29)), the generalised Rice and Tracey criterion (equation (30)) and the work of fracture criterion (equation (33)) defined in sections 1.6.2. and 1.6.3.

The through thickness averaging methodology developed in this work is presented in section 3.7.3. .
4.6.1. **SEN specimen**
The SEN(B)_S, SEN(B)_D and SEN(T)_D specimens were modelled using 3D FEA using the approach described in section 3.2.2. Figure 90, Figure 91 and Figure 92 show the evolution of averaged damage through thickness against the applied J integral at the centre of the specimen. Damage was monitored using the Rice and Tracey local approach in Figure 90, the Generalised Rice and Tracey local approach in Figure 91 and the work of fracture in Figure 92. The values of the Local approach criteria at initiation defined experimentally are shown in Table 21.

<table>
<thead>
<tr>
<th></th>
<th>SEN(T) Deep crack</th>
<th>SEN(B) Shallow Crack</th>
<th>SEN(B) Deep Crack</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>$J_{0.2BL}$ experimental</td>
<td>2430</td>
<td>2100</td>
<td>1530</td>
<td>n/a</td>
</tr>
<tr>
<td>HC Rice &amp; Tracey value</td>
<td>2.44</td>
<td>2.44</td>
<td>2.44</td>
<td>2.44</td>
</tr>
<tr>
<td>G Rice &amp; Tracey value</td>
<td>2.398</td>
<td>2.404</td>
<td>2.409</td>
<td>2.40</td>
</tr>
<tr>
<td>Work of Fracture value</td>
<td>10,344</td>
<td>10,289</td>
<td>9,710</td>
<td>10,114</td>
</tr>
</tbody>
</table>

Table 21: Local approach criteria averaged through thickness at initiation defined using $J_{0.2BL}$ in SEN(B)_S, SEN(B)_D and SEN(T)_D specimen
New insights into the competition between ductile tearing and plastic collapse

Figure 90: Evolution of damage averaged through thickness defined using the High Constraint Rice & Tracey local approach vs. J-integral averaged through thickness in the SEN(B)_S, SEN(B)_D and SEN(T)_D geometries.

Figure 91: Evolution of damage averaged through thickness defined using the Generalised Rice & Tracey local approach vs. J-integral averaged through thickness in the SEN(B)_S, SEN(B)_D and SEN(T)_D geometries.
New insights into the competition between ductile tearing and plastic collapse

Figure 92: Evolution of damage averaged through thickness defined using the work of fracture local approach vs. J-integral averaged through thickness in the SEN(B)_S, SEN(B)_D and SEN(T)_D geometries

All three Local Approach methodologies provided constraint independent failure criteria for the pipe material. Deviation of critical local approach criteria are 2%, 4% and 1.5% of High Constraint Rice & Tracey criteria, generalised Rice and Tracey criteria and work of fracture criteria respectively. This deviation can be explained by:

1. The assumption that the failure process can be defined using void growth theory alone
2. Experimental scatter in the fracture toughness measurements.
4.6.2. **Plate Material:**

4.6.2.1. *Damage in the fracture process zone*

The SEN(B)_S, SEN(B)_D and SEN(T)_D specimens were modelled using three dimensional Finite Element methodologies using the approach described in section 3.2.2. Figure 93, Figure 94 and Figure 95 show the evolution of averaged damage through thickness against contour J integral at the centre of the specimen. Damage is monitored using the Rice & Tracey local approach in Figure 93, the Generalised Rice & Tracey local approach in Figure 94 and the work of fracture in Figure 95. The values of the Local Approach criteria at initiation defined experimentally are shown in Table 22.

**Table 22: Local approach criteria averaged through thickness at initiation defined using J0.2BL in 25mmCT, 15mmCT and 10mmCT specimen**

<table>
<thead>
<tr>
<th></th>
<th>25mmCT</th>
<th>15mmCT</th>
<th>10mmCT</th>
</tr>
</thead>
<tbody>
<tr>
<td>J_{0.2BL} _experimental</td>
<td>700</td>
<td>300</td>
<td>170</td>
</tr>
<tr>
<td>HC Rice &amp; Tracey value</td>
<td>1.193</td>
<td>0.684</td>
<td>0.492</td>
</tr>
<tr>
<td>G Rice &amp; Tracey value</td>
<td>1.190</td>
<td>0.682</td>
<td>0.486</td>
</tr>
<tr>
<td>Work of Fracture value</td>
<td>12,290</td>
<td>2,590</td>
<td>1,711</td>
</tr>
</tbody>
</table>

**Figure 93:** Evolution of damage averaged through thickness defined using the Generalised Rice & Tracey local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT
New insights into the competition between ductile tearing and plastic collapse

Figure 94: Evolution of damage averaged through thickness defined using the high constraint Rice & Tracey local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT

Figure 95: Evolution of damage averaged through thickness defined using the work of fracture local approach vs. J-integral averaged through thickness in the 25mmCT, 15mmCT and 10mmCT
Figure 93, Figure 94 and Figure 95 show that at a given value of J, a reduction in specimen size leads to an increase in damage, as defined by the local approach methodologies, in the first row of elements ahead of the crack tip. This is consistent with the J-Q measurements, which show increase in constraint conditions as defined by J-Q with a reduction in specimen size.

This suggests that the local approach criteria effectively rank the constraint conditions as measured experimentally. Nevertheless, although the variation for yield strength within the plate was corrected for, no local approach could provide a consistent prediction of initiation toughness. The inadequacy of the local approach definition used to define initiation could be due to:

1. material variation observed throughout the plate
2. the failure process is no longer dominated by void growth only and void nucleation and coalescence should be considered
4.7. **Prediction of valid initiation toughness using a Modified Boundary Layer model:**

In order to obtain the constraint independent fracture toughness properties experimentally, a large volume of material is required to manufacture specimen failing under a range of constraint conditions. Boundary Layer models provide a simple methodology to apply a range of constraint conditions at the crack tip. The displacements applied at the boundary of the model are proportional to, $J$ and $T$ applied at the crack tip, and to the radius of the model (equation (83),(84)) . As the displacement field is defined using Linear Elastic Fracture Mechanics (LEFM) theory, the displacement are proportional to the crack tip conditions only when the plastic zone is small compared to the size of the model. The plastic zone should be contained within $1/5^{th}$ of the radius. [94, 100]

4.7.1. **Definition of the Plastic zone size and MBLM methodology validity:**

The modified boundary layer model material properties are defined from the true stress vs. strain material behaviour derived experimentally (section 4.2.), and applied using incremental plasticity. The onset of plasticity is defined in the model as the point of deviation from the elastic loading line defined using the Young’s modulus.

Due to the characteristic deformation behaviour of austenitic stainless steels, the deviation from the elastic loading line occurs at an early stage of stress strain curve and often at levels of stresses many times lower that the material’s proof stress. In this work, the onset of plasticity as defined by the deviation from the elastic loading line occurs at a stress of 80MPa, whilst the material exhibits a proof stress of 255MPa. This creates and issue regarding the definition of plasticity in this work. This has a significant impact on the boundary layer model validity limits.
It also affects the definition of constraint applied to the crack tip through the T-stress. Constraint is conventionally applied as a function of $T/\sigma_0$, with values of encompassed between -0.1 and -0.8 expected to lead to significant deviation in the crack tip stress field, as shown in Figure 3 on page 35. This observation is only true for materials which exhibit a sharp transition between the linear elastic behaviour and the elastic plastic behaviour, and hence, when $\sigma_0$ is close to $\sigma_y$. In austenitic stainless steels, the large difference between the deviation from the elastic line ($\sigma_0$) and the proof stress leads to an issue regarding the definition of the normalising stress. If the deviation of from the elastic loading line is used, very low effective levels of T-stress compared to the material elastic-plastic deformation behaviour are applied to the crack tip; and hence the applied T-stresses have a very limited impact on the crack tip constraint. In order to obtain levels of constraint which are comparable to those obtained in the literature for ferritic and austenitic steels, the 0.2% proof stress should be used.

As the displacements applied at the boundary of the model are derived from Linear Elastic Fracture Mechanics theory, in order to apply the appropriate crack tip stress field, the plastic zone size must be small compared to the over-all size of the model. In effect, this requires the plastic zone to be contained within $1/5$th of the model’s radius in order to maintain SSY conditions.

Furthermore, the applied T-stress has a significant impact on the plastic zone size, with an increase in the plastic zone size with the application of a negative T-stress (Figure 12, p45), and the application of a T-stress larger than yield leads to the plastic zone encroaching on the boundary of the MBLM model independently of the radius of the model. Hence, care should be taken when defining the onset of plasticity.

In order to assess the effect of constraint on initiation toughness in stainless steel, we need to investigate constraint conditions encompassed between $T/\sigma_y=0$ and $T/\sigma_y=-0.7$. This is equivalent to applying constraint conditions of $0>T/\sigma_0>-2.18$. This has a significant impact on the definition of the validity limits of the MBLM model.
New insights into the competition between ductile tearing and plastic collapse

Figure 96 shows that, when we consider yield as the deviation from the elastic loading line ($\sigma_0$), the plasticity will develop through the MBLM model and encroach on the model’s outer boundary at levels of $T/\sigma_y < -0.32$. Furthermore, the plastic zone will be larger than $1/5$th of the model’s radius for values of $T/\sigma_y < -0.2$. Hence, theoretically the assumptions used to derive the displacements applied to the MBLM are no longer valid.

![Plastic zone size](image)

**Figure 96**: Plastic zone size with yield defined as the deviation from the elastic loading line, in a MBLM model of $r = 200000$ mm for an applied $J = 1000$kJm$^{-2}$, and range of elastic T-stress conditions

Never-the-less, when we investigate the size of the plastic zone using the proof stress to define yield, the plastic zone is well contained within the MBLM model. Figure 97 shows that the plastic zone defined using the proof stress is contained within the model for levels of constraint up to $T/\sigma_y = -1$. 

163
New insights into the competition between ductile tearing and plastic collapse

Figure 97: Plastic zone size with yield defined as the proof stress, in a MBLM model of \( r = 200000 \) mm for an applied \( J = 1000 \text{kJm}^{-2} \), and range of elastic T-stress conditions

The plastic deformation applied to the material between the yielding point as defined by \( \sigma_0 \) and the proof stress is very small. This is due to the very progressive deviation of the stress-strain deformation from the elastic loading line. This never-the-less invalidates the assumption used to derive the displacement, as LEFM theory can no longer be applied when the plastic zone is larger than 1/5\(^{th}\) of the model’s radius.

Due to this issue, we differentiate in this work the values that are obtained in valid conditions, where the plastic zone size defined by \( \sigma_0 \) is less that 1/5\(^{th}\) of the radius of the model, and the values obtained where the plastic zone size, again defined by \( \sigma_0 \) is large.

In order to obtain valid estimations of the constraint applied ahead of the crack tip, the effect of constraint on the initiation toughness will be investigated using the elastic-plastic constraint parameter, Q.
4.7.2. Methodology and Results:

In this section, the Local Approach criteria defined for the pipe material in section 3.4. was combined with a Local Approach based modified boundary layer model (MBLML) defined in section 3.4., in order to obtain analytically 1) the high constraint fracture toughness and 2) the constraint dependent initiation toughness. Seven different constraint conditions were applied to the MBLML model, of normalised $T/\sigma_y=0$, -0.2, -0.3, -0.4, -0.5, -0.6 and -0.7, to sample a range of constraint conditions.

As all local approach have provided valid constraint independent initiation toughness estimation, only one LA criteria was used for this analysis. The High Constraint Rice & Tracey parameter was chosen for this analysis for its ease of use. The damage in the first element ahead of the crack tip is calculated for each loading increment and plotted as a function of $J$ in Figure 98.

The evolution of the high constraint Rice and Tracey parameter (HR&T) vs. $J$ for the range of $T/\sigma_y$ investigated is shown in Figure 98. The horizontal line plotted in Figure 98 represents the calibrated failure criterion for the pipe material.

*Figure 98: High Constraint Rice & Tracey vs. $J$ obtained from a Modified Boundary layer Model for a range of Biaxiality ratios - pipe material*
The value of J initiation at different constraint conditions can be defined from the intersection of the constraint specific HR&T vs. J curve with the critical HR&T curve. As the T-stress is defined for the displacement applied at the final increment and the displacement is applied incrementally, the T-stress at initiation is proportional to the increment at which initiation is defined. The J at the critical Local approach values and corresponding T-stresses are presented in Table 23.

In order to define the variation of J initiation with Q, the Q parameter is calculated at each initiation conditions, at a distance $r_\sigma y/J=2$. The J at the critical local approach value and the corresponding Q parameter values are presented in Table 23.

The value of J at initiation under high constraint conditions ($T/\sigma_y=0$) defines the lower bound, high constraint value of J at the onset of crack growth. This will be used as our high constraint fracture toughness parameter in the subsequent sections of this thesis, and will be referred to as $J_{MBLM}$.

<table>
<thead>
<tr>
<th>J</th>
<th>T/σy</th>
<th>Q</th>
</tr>
</thead>
<tbody>
<tr>
<td>485</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>505</td>
<td>-0.15</td>
<td>-0.05</td>
</tr>
<tr>
<td>530</td>
<td>-0.231</td>
<td>-0.12</td>
</tr>
<tr>
<td>561</td>
<td>-0.32</td>
<td>-0.2</td>
</tr>
<tr>
<td>584</td>
<td>-0.415</td>
<td>-0.25</td>
</tr>
<tr>
<td>599</td>
<td>-0.456</td>
<td>-0.3</td>
</tr>
<tr>
<td>650</td>
<td>-0.581</td>
<td>-0.38</td>
</tr>
</tbody>
</table>

The fracture toughness constraint dependence should be defined as a function of $L_r$ following equation (99) in order to construct the constraint corrected FAD. The constraint dependent J values defined in Table 23 were converted to K following equation (100),
assuming plane strain conditions. The $\alpha$ and $k$ parameters defined in equation (5) were
derived for constraint dependence defined as a function of $T$ and $Q$.

$$K_{mat}^c = K_{mat}[1 + \alpha(-\beta L_r)^k]$$

With $\beta_T L_r = T/\sigma_y$

Or $\beta_Q L_r = Q$

$$K = \frac{EJ}{\sqrt{(1 - \nu^2)}}$$

(99)

(100)

Figure 99 shows the J-T failure locus defined using the critical HCR&T value of 2.44
obtained in section 4.6. for the pipe material. The values of $\alpha$ and $k$ were derived through a
curve fitting of the $K_{mat}^c/K_{mat}$ vs. $T$ data with $\alpha=0.3$ and $k=1.25$.

Figure 99: $K_{mat}^c/K_{mat}$ as defined using the High Constraint Rice & Tracey parameter vs. $T$
- pipe material

The values of $\alpha$ and $k$ were derived through a curve fitting of the $K_{mat}^c/K_{mat}$ vs. $Q$ data with
$\alpha=0.55$ and $k=1.25$. The values obtained from the curve fit are compared with the
experimental measurement of J-Q locus and the MBLM calculation of the J-Q locus in
Figure 100. Experimental and analytical data follow the same trend with a maximum deviation of 5\% of the experimental data from the analytical solutions.

![Graph](image)

**Figure 100:** $K_{\text{mat}}^c / K_{\text{mat}}$ vs. $Q$ – results obtained from the MBLM model and experimental.

The definition of the initiation toughness constraint dependence and calibration of the $\alpha$ and $k$ parameters allow for the correction of the FAD curves following the procedure defined in section 1.7.4.1.
4.8. **Prediction of engineering failure in a pipe containing a circumferential defect:**

The methodologies described in section 3.5. to section 4.7. provide a reliable way to define ductile initiation fracture toughness under a range of constraint conditions. These methodologies are of particular interest when assessing engineering failure in structural components. One such component, where a good understanding of engineering failure mechanisms is of vital importance, is the primary system piping of a Pressurised Water Reactor. The following section assesses the integrity of a pipe containing an internal circumferential defect. The pipe geometry and the corresponding FEA model have been described in section 3.5.

The model geometry and loading conditions considered are defined in section 3.5. The conventional FAD, the constraint dependant FAD and the Local Approach methodology for ductile initiation are used in this section to assess the engineering failure conditions of the cracked pipe for different crack depths.

Section 4.8.1. describes the pipe model and loading conditions in more details. The R6 constraint modified FAD and the modified FAD for ductile tearing conditions developed in this work are described in section 4.8.7. and section 4.8.6. respectively.

### 4.8.1. Limit Load Analysis:

The limit load was evaluated using a RIKS analysis of an elastic perfectly plastic material definition as defined in sections 1.1. Table 24 and Figure 101 presents the limiting internal pressure solutions for the different crack depths and loading conditions analysed in this study. The loading condition analysed is defined using $\lambda$, with $\lambda=\sigma_{axial}/\sigma_{hoop}$. 


New insights into the competition between ductile tearing and plastic collapse

Table 24: Internal limit pressure as a function of crack depth and $\lambda$ for the circumferentially cracked pipe model

<table>
<thead>
<tr>
<th>Limit internal Pressure (MPa)</th>
<th>(a/t)</th>
<th>0.1</th>
<th>0.2</th>
<th>0.3</th>
<th>0.4</th>
<th>0.5</th>
<th>0.6</th>
<th>0.7</th>
<th>0.8</th>
<th>0.9</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\lambda = 0.5$</td>
<td>59.12</td>
<td>58.98</td>
<td>58.66</td>
<td>58.08</td>
<td>56.56</td>
<td>44.8</td>
<td>32.94</td>
<td>21.62</td>
<td>11.46</td>
<td></td>
</tr>
<tr>
<td>$\lambda = 0.75$</td>
<td>56.54</td>
<td>56.22</td>
<td>55.28</td>
<td>49.16</td>
<td>40.49</td>
<td>32.03</td>
<td>23.79</td>
<td>15.77</td>
<td>7.95</td>
<td></td>
</tr>
<tr>
<td>Tension</td>
<td>252.4</td>
<td>236.55</td>
<td>209.9</td>
<td>182.3</td>
<td>152.6</td>
<td>125.15</td>
<td>95.45</td>
<td>64.90</td>
<td>33.57</td>
<td></td>
</tr>
</tbody>
</table>

The limit load decreases linearly with increasing normalised crack depth $a/t$ for the geometry in pure far field tension. The limit load is almost constant for $0.1 < a/t < 0.3$ and then decreases linearly for $\lambda = 0.75$. The limit load is almost constant for $0.1 < a/t < 0.5$ and then decreases linearly for $\lambda = 0.5$

Figure 101: Variation in Limit Load with crack depth for loading conditions $\lambda = 0.5, 0.75$ and $\infty$
4.8.2. Conventional Failure Assessment Diagram

Option 1 and Option 3 FADs were constructed for the pipe material using equation (101) and equation (102) respectively.

\[ f_1(L_r) = [1 + 0.5L_r^2]^{-0.5}[0.3 + 0.7e^{-0.6L_r}] \quad (101) \]

\[ f_3(L_r) = \left( \frac{f_e}{f} \right) \frac{1}{2} \quad (102) \]

With \( f_1 \) defining the option 1 failure curve, \( f_3 \) defining the option 3 failure curve, \( L_r \) defined in equation (103), \( J_e \) the elastic J-integral and \( J \) the elastic-plastic J-integral.

\[ L_r = \frac{P}{P_L} \quad (103) \]

with \( P \) the load or stress applied and \( P_L \) the limit load or limiting stress defined analytically from a elastic-perfectly plastic material model defined in section 1.1. .

In all these cases, failure by plastic collapse is defined using the R6 definition of \( L_{r_{\text{max}}} \) defined in equation (104).

\[ L_{r_{\text{max}}} = \frac{\sigma_f}{\sigma_y} = \frac{417}{255} \approx 1.64 \quad (104) \]

The loading line was constructed using equation (105), from analytically derived values of \( K_I \) and experimentally characterised values of fracture toughness (\( K_{\text{mat}} \)). The limit of plasticity \( L_{r_{\text{max}}} \) was defined using the R6 definition shown in equation (104). \( K_{\text{mat}} \) values are calculated from the J integral measurement at initiation converted to \( K_{\text{mat}} \) using plane strain relationship defined in equation (106).

\[ K_r = \frac{K_I}{K_{\text{mat}}} \quad (105) \]
New insights into the competition between ductile tearing and plastic collapse

\[ K_{\text{mat}} = \sqrt{\frac{E J_{0.2BL}}{(1 - \nu^2)}} \]  \hspace{1cm} (106)

4.8.3. Effect of initiation toughness measurements:

Figure 102 show the Option 1 and Option 3 FAD for the circumferentially cracked pipe with a crack depth of \( a/t = 0.5 \) and loading condition \( \lambda = 0.5 \). For this particular material, the option 1 FAD is slightly non-conservative. The option 1 curve consistently lies above the option 3 curve, leading to non-conservative estimation of fracture using option 1 curve for that specific geometry, defect and loading condition. As Option 3 is material, geometry and loading dependant, Figure 102 is only a representative presentation of the difference between the two approaches.

The sensitivity of failure analysis to the definition of initiation toughness is shown in Figure 102. Three definitions of fracture toughness were chosen to illustrate the sensitivity of the FAD failure assessment methodologies to the definition of initiation toughness:

- the \( J \) validity for the SEN(B)\_D specimen defined using ESIS P2-92 (Table 25),
- the \( J_{0.2} \) value obtained from the SEN specimen obtained from the blunting line defined in ESIS P2-92 (Table 25),
- the high constraint initiation toughness derived analytically using the Boundary layer model defined in section 4.7. (Table 25)

<table>
<thead>
<tr>
<th>Fracture Toughness (kJ m(^{-2}))</th>
<th>( J_{\text{validity}} ) ESIS P2-92</th>
<th>( J_{0.2} )</th>
<th>( J_{\text{MBLM}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>210</td>
<td>410</td>
<td>485</td>
</tr>
</tbody>
</table>

\( J_{\text{validity}} \): defined using ESIS P2-92, \( J_{0.2} \): defined using ESIS P2-92 blunting line and \( J_{\text{MBLM}} \): Fracture toughness measurements for the pipe material.

It is important to note that the values obtained from the SEN specimen are geometrically dependent.
New insights into the competition between ductile tearing and plastic collapse

Figure 102: R6 Option 1 & 3 Failure Assessment Diagram – Failure prediction of circumferentially cracked pipe with fracture toughness defined using specimen validity limit, $J_{0.2}$ and high constraint fracture toughness defined analytically using local approach criteria combined to MBLM.

Figure 102 shows that the definition of the material fracture toughness has a significant impact on the prediction of failure. Furthermore, the use of the $J_{\text{validity}}$ initiation toughness leads to 10% conservatism in the assessment of the $L_r$ at fracture and 30% in the assessment of $K_r$ at fracture compared to the analysis using the high constraint fracture toughness derived analytically using the MBLM model.

The sensitivity of the FAD failure analysis to the definition of fracture toughness is investigated in more details in the following sections. The prediction of failure using the ESIS P2-92 validity limits, the analytically derived high constraint fracture toughness and the constraint corrected fracture toughness (using the local approach methodologies to define failure) are investigated in section 4.8.4., section 4.8.5. and section 4.8.6. respectively.

All definitions of fracture toughness initiation value predict ductile crack initiation prior to reaching the limit plasticity condition. Large fracture toughness constraint dependence was observed experimentally in section 4.5. and analytically in section 4.7., which means that even with the correct initiation toughness, this analysis can be assumed to be conservative.
4.8.4. **Analysis of pipe failure using Option 1 FAD and J\text{validity}**:

Figure 103 summarises the results obtained in FAD Option 1 failure assessment of the internally circumferentially cracked pipe for $\lambda=0.5$, $\lambda=0.75$ and $\lambda=\infty$ analysed using $J\text{validity}$ defined as the SEN(B)_D specimen J validity limit. Failure is predicted to occur by ductile initiation for all conditions except $a/t=0.1$ with $\lambda=0.5$ and $a/t\geq0.8 \lambda=0.75$.

![Figure 103: Engineering prediction of $L_r$ at failure using R6 FAD option 1 & J specimen validity limit vs. crack depth for a circumferentially cracked pipe loaded under a range of $\lambda$ conditions](image)

4.8.5. **Analysis of pipe failure using Option 1 FAD and $J_{\text{MBLM}}$**:

The R6 Option 1 FAD approach was used to predict the failure of the pipe under loading conditions $\lambda=0.5$, 0.75 and $\infty$ (pure tension). The high constraint initiation toughness of 485 kJ m$^{-2}$, defined using the Boundary Layer Model methodology defined in section 3.4.
was used in all the FAD analysis. Failure by ductile tearing is predicted when the loading line intersects the failure curve, whilst failure by plastic collapse is predicted when the loading line intersects with the limit load conditions.

Figure 104 and Figure 105 show the prediction of failure using the R6 FAD approach for the pipe analysed for a normalised crack depth of 0.1 to 0.5 and a normalised crack depth of 0.6 to 0.9 respectively with a loading condition \( \lambda = 0.5 \). Failure by ductile tearing is predicted for crack lengths of \( 0.3 \geq a/t \geq 0.8 \). A reduction in \( L_r \) at fracture is predicted for \( 0.1 \geq a/t \geq 0.5 \), followed by an increase in \( L_r \) at fracture for \( 0.5 \geq a/t \geq 0.9 \). The most severe crack depth for a loading condition of \( \lambda = 0.5 \) is \( a/t = 0.5 \). A large change in predicted failure occurs between \( a/t = 0.5 \) and \( a/t = 0.6 \). This is due to the large reduction in limit load, as shown in Figure 101.

![Figure 104: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with \( \lambda = 0.5 \) and \( a/W = 0.1, 0.2, 0.3, 0.4 \) and 0.5](image-url)
New insights into the competition between ductile tearing and plastic collapse

Figure 105: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with λ=0.5 and a/W=0.6, 0.7, 0.8 and 0.9

Figure 106 and Figure 107 show the prediction of failure using the R6 FAD approach for the pipe analysed for a normalised crack depth of 0.1 to 0.5 and a normalised crack depth of 0.5 to 0.9 respectively with a loading condition λ=0.75.

Failure by ductile tearing is predicted for crack lengths of $0.2 \geq a/t \geq 0.6$. A reduction in $L_r$ at fracture is predicted for $0.1 \geq a/t \geq 0.5$, followed by an increase in $L_r$ at fracture for $0.5 > a/t > 0.9$. Similarly to the $\lambda=0.75$ loading case, the most severe crack depth for a loading condition of $\lambda=0.5$ is $a/t=0.5$. 

176
New insights into the competition between ductile tearing and plastic collapse

Figure 106: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 107: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 108 and Figure 109 show the prediction of failure using the R6 FAD approach for the pipe analysed for a normalised crack depth of 0.1 to 0.5 and a normalised crack depth of 0.6 to 0.9 respectively with a loading condition $\lambda=\infty$.

Failure by ductile tearing is predicted for crack lengths of $0.2 \geq a/t \geq 0.6$. A reduction in $L_r$ at fracture is predicted for $0.1 \geq a/t \geq 0.5$, followed by an increase in $L_r$ at fracture for $0.5 \geq a/t \geq 0.9$. Similarly to the $\lambda=0.75$ loading case, the most severe crack depth for a loading condition of $\lambda=0.5$ is $a/t=0.5$.

![Figure 108: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=\infty$ and $a/W=0.1, 0.2, 0.3, 0.4$ and 0.5](image-url)
New insights into the competition between ductile tearing and plastic collapse

![Graph](image)

*Figure 109: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=\infty$ and $a/W=0.6, 0.7, 0.8$ and $0.9$*

Figure 110 summarises the results obtained in FAD Option 1 failure assessment of the internally circumferentially cracked pipe for $\lambda=0.5$, $\lambda=0.75$ and $\lambda=\infty$ analysed using $J_{\text{MBLM}}$ obtained from the MBLM model.

![Graph](image)

*Figure 110: Engineering prediction of $L_r$ at failure using R6 FAD option 1 & $J$ specimen validity limit vs. crack depth for a circumferentially cracked pipe loaded under a range of $\lambda$ conditions*
New insights into the competition between ductile tearing and plastic collapse

Table 26 presents the $L_r$ at ductile fracture initiation predicted using the fracture toughness specimen ESIS P2-92 validity limit ($J_{\text{validity}}$) and the high constraint initiation toughness defined using the MBLM approach ($J_{\text{MBLM}}$). Significant conservatism arises from the inadequate derivation of the high constraint fracture toughness, due to experimental specimen limitations. The highest amount of conservatism is observed at small and large crack lengths.

**Table 26: $L_r$ at ductile fracture predicted by the Option 1 FAD using $J_{\text{validity}}$ and $J_{\text{MBLM}}$ to define fracture toughness**

<table>
<thead>
<tr>
<th>$\lambda$</th>
<th>$a/t$</th>
<th>0.1</th>
<th>0.2</th>
<th>0.3</th>
<th>0.4</th>
<th>0.5</th>
<th>0.6</th>
<th>0.7</th>
<th>0.8</th>
<th>0.9</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>R6 Prediction (MBLM)</td>
<td>2.3</td>
<td>1.831</td>
<td>1.53</td>
<td>1.377</td>
<td>1.29</td>
<td>1.302</td>
<td>1.366</td>
<td>1.5</td>
<td>1.85</td>
</tr>
<tr>
<td>$\lambda=0.5$</td>
<td>R6 Prediction ($J_{\text{valid}}$)</td>
<td>1.78</td>
<td>1.46</td>
<td>1.32</td>
<td>1.28</td>
<td>1.2</td>
<td>1.24</td>
<td>1.28</td>
<td>1.33</td>
<td>1.44</td>
</tr>
<tr>
<td>% Difference</td>
<td>22.6</td>
<td>20.3</td>
<td>13.7</td>
<td>7.0</td>
<td>7.0</td>
<td>4.8</td>
<td>6.3</td>
<td>11.3</td>
<td>22.2</td>
<td></td>
</tr>
<tr>
<td>$\lambda=0.75$</td>
<td>R6 Prediction (MBLM)</td>
<td>1.98</td>
<td>1.51</td>
<td>1.37</td>
<td>1.30</td>
<td>1.29</td>
<td>1.61</td>
<td>1.75</td>
<td>2.45</td>
<td>3.02</td>
</tr>
<tr>
<td>% Difference</td>
<td>22.2</td>
<td>12.3</td>
<td>8.5</td>
<td>5.5</td>
<td>6.2</td>
<td>14.4</td>
<td>14.6</td>
<td>20.4</td>
<td>22.2</td>
<td></td>
</tr>
<tr>
<td>$\lambda=\infty$</td>
<td>R6 Prediction (MBLM)</td>
<td>1.96</td>
<td>1.5</td>
<td>1.41</td>
<td>1.38</td>
<td>1.37</td>
<td>1.36</td>
<td>1.44</td>
<td>1.58</td>
<td>1.95</td>
</tr>
<tr>
<td>% Difference</td>
<td>22.4</td>
<td>14.0</td>
<td>10.6</td>
<td>10.9</td>
<td>9.5</td>
<td>8.1</td>
<td>11.8</td>
<td>15.2</td>
<td>20.0</td>
<td></td>
</tr>
</tbody>
</table>

### 4.8.6. Modified FAD for ductile tearing

The evolution of ductile damage ahead of the crack is analysed using the three local approaches validated for this material in section 4.6. Failure due to ductile tearing initiation is defined when the critical parameters of the local approach models are attained in material ahead of the crack-tip. Failure by plastic collapse is defined using the R6 Failure assessment definition of $L_r$ max.

In this section, the results are presented using a Local Approach defined Failure Assessment Curve for ductile tearing. The normalised local approach ($LA/LA_c$) is plotted against $L_r$. LA is the specific value of the local approach parameter at a specific loading condition. $LA_c$ is the value of the local approach at initiation. The horizontal line
represents the limiting condition characterised using the normalised local approach parameter. The vertical line represents the plastic collapse condition defined using the maximum load bearing capacity of the component or structure analysed, with the material idealised to an elastic perfectly plastic stress-strain response. The intersection between the loading line and LA/LA_c or the L_{r_{max}} limiting conditions defines failure by ductile initiation or by plastic collapse, respectively. The methodology is defined schematically in Figure 111.

![Figure 111: Local Approach Modified Failure Assessment Diagram](image)

4.8.6.1. Loading condition \( \lambda = 0.5 \)

Figure 112 and Figure 113 present the results obtained using the Local Approach (LA) modified Failure Assessment Diagram (FAD) for ductile tearing condition, using the high constraint Rice and Tracey local approaches for a loading condition \( \lambda = 0.5 \) and crack depths of \( 0.1 < a/t < 0.5 \) and \( 0.5 < a/t < 0.8 \) respectively.

Figure 114 and Figure 115 present the results obtained using the LA FAD for ductile tearing condition, using the Generalised Rice and Tracey local approaches for a loading condition \( \lambda = 0.5 \) and crack depths of \( 0.1 < a/t < 0.5 \) and \( 0.5 < a/t < 0.8 \) respectively.

Figure 116 and Figure 117 present the results obtained using the LA FAD for ductile tearing condition, using the Work of fracture local approaches for a loading condition \( \lambda = 0.5 \) and crack depths of \( 0.1 < a/t < 0.5 \) and \( 0.5 < a/t < 0.8 \) respectively.
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**Figure 112**: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

**Figure 113**: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 114: Modified Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 115: Modified Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 116: Modified Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 117: Modified Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.5$ and $a/W=0.6$, 0.7, 0.8 and 0.9

184
All three approaches predict similar failure behaviour. An increase in the severity of damage in relation to \(L_r\) is observed for crack depths of \(0.1 \leq a/t \leq 0.5\) with a linear relationship between \(LA\) and \(L_r\) for the two Rice & Tracey local approach. The most severe condition occurs at \(a/t=0.5\), with the component failing just beyond the plastic collapse limiting condition at an \(L_r \approx 1.7\). A rapid decrease in damage in relation to \(L_r\) is observed for crack depths of \(0.6 \geq a/t \geq 0.9\). This can be explained by the reduction in limit load solutions for \(a/t>0.5\) (section 4.8.1).

4.8.6.2. Loading condition \(\lambda=0.75\)

Figure 118 and Figure 119 present the results obtained using the LA Failure assessment diagram for ductile tearing condition, using the high constraint Rice and Tracey local approaches for a loading condition \(\lambda=0.75\) and crack depths of \(0.1<a/t<0.5\) and \(0.5<a/t<0.8\) respectively.

Figure 120 and Figure 121 present the results obtained using the LA Failure assessment diagram for ductile tearing condition, using the Generalised Rice and Tracey local approaches for a loading condition \(\lambda=0.75\) and crack depths of \(0.1<a/t<0.5\) and \(0.5<a/t<0.8\) respectively.

Figure 122 and Figure 123 present the results obtained using the LA Failure assessment diagram for ductile tearing condition, using the work of fracture local approaches for a loading condition \(\lambda=0.75\) and crack depths of \(0.1<a/t<0.5\) and \(0.5<a/t<0.8\) respectively.
New insights into the competition between ductile tearing and plastic collapse

Figure 118: LA Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 119: LA Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 120: LA Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 121: LA Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 122: LA Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 123: LA Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
All three Local Approach models predict similar failure behaviour. An increase in the severity of damage in relation to $L_\tau$ is observed for crack depths of $0.1 \leq a/t \leq 0.5$ with a linear relationship between LA and $L_\tau$ for the two Rice & Tracey local approach. The most severe condition occurs at $a/t=0.5$, with the component failing just beyond the plastic collapse limiting condition at an $L_\tau \approx 1.7$. A rapid decrease in damage in relation to $L_\tau$ is observed for crack depths of $0.6 \geq a/t \geq 0.9$. This can be explained by the reduction in limit load solutions for $a/t>0.5$ (section 4.8.1.).

4.8.6.3. **Loading condition $\lambda=\infty$**

Figure 124 and Figure 125 present the results obtained using the LA FAD for ductile tearing condition, using the high constraint Rice and Tracey local approaches for a loading condition $\lambda=0.75$ and crack depths of $0.1<a/t<0.5$ and $0.5<a/t<0.8$ respectively.

Figure 126 and Figure 127 present the results obtained using the LA FAD for ductile tearing condition, using the Generalised Rice and Tracey local approaches for a loading condition $\lambda=0.75$ and crack depths of $0.1<a/t<0.5$ and $0.5<a/t<0.8$ respectively.

Figure 128 and Figure 129 present the results obtained using the FAD for ductile tearing condition, using the work of fracture local approaches for a loading condition $\lambda=0.75$ and crack depths of $0.1<a/t<0.5$ and $0.5<a/t<0.8$ respectively.
New insights into the competition between ductile tearing and plastic collapse

Figure 124: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda = 0.75$ and $a/W = 0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 125: Modified Failure Assessment Diagram using High Constraint Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda = 0.75$ and $a/W = 0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

**Figure 126**: Modified Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

**Figure 127**: Modified Failure Assessment Diagram using General Rice & Tracey local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Figure 128: Modified Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.1$, 0.2, 0.3, 0.4 and 0.5

Figure 129: Modified Failure Assessment Diagram using Work of Fracture local approach methodology – Pipe material – PWR primary system piping analysis with $\lambda=0.75$ and $a/W=0.6$, 0.7, 0.8 and 0.9
New insights into the competition between ductile tearing and plastic collapse

Three approaches predict similar failure behaviour. An increase in the severity of damage in relation to $L_r$ is observed for crack depths of $0.1 \geq a/t \geq 0.3$ with a linear relationship between $L_A$ and $L_r$ for the two Rice & Tracey local approach. The failure conditions predicted at $a/t=0.3$ and 0.4 are similar, at an $L_r$ of 1.905 and 1.912 respectively according to the Rice and Tracey criterion and at the same $L_r$ of 1.91 for the Work of Fracture criteria. A rapid decrease in damage in relation to $L_r$ is observed for crack depths of $0.5 \geq a/t \geq 0.9$.

Table 27 presents the $L_r$ at ductile fracture initiation predicted using the three local approach methods.

<table>
<thead>
<tr>
<th>$a/t$</th>
<th>0.1</th>
<th>0.2</th>
<th>0.3</th>
<th>0.4</th>
<th>0.5</th>
<th>0.6</th>
<th>0.7</th>
<th>0.8</th>
<th>0.9</th>
</tr>
</thead>
<tbody>
<tr>
<td>$L_r$ at failure defined by the critical local approach</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$\lambda=0.5$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>H R&amp;T</td>
<td>n/a</td>
<td>2.645</td>
<td>2.125</td>
<td>1.799</td>
<td>1.670</td>
<td>1.963</td>
<td>2.533</td>
<td>3.473</td>
<td>4.919</td>
</tr>
<tr>
<td>GR&amp;T</td>
<td>n/a</td>
<td>2.645</td>
<td>2.123</td>
<td>1.797</td>
<td>1.669</td>
<td>1.964</td>
<td>2.538</td>
<td>3.473</td>
<td>4.919</td>
</tr>
<tr>
<td>W_D</td>
<td>n/a</td>
<td>2.70</td>
<td>2.154</td>
<td>1.821</td>
<td>1.674</td>
<td>1.938</td>
<td>2.460</td>
<td>3.314</td>
<td>4.465</td>
</tr>
<tr>
<td>R6 Prediction</td>
<td>2.30</td>
<td>1.831</td>
<td>1.500</td>
<td>1.377</td>
<td>1.323</td>
<td>1.362</td>
<td>1.366</td>
<td>1.40</td>
<td>1.525</td>
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<tr>
<td>$\lambda=0.75$</td>
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<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>H R&amp;T</td>
<td>n/a</td>
<td>2.232</td>
<td>1.85</td>
<td>1.806</td>
<td>2.004</td>
<td>3.487</td>
<td>4.301</td>
<td>7.916</td>
<td>11.221</td>
</tr>
<tr>
<td>GR&amp;T</td>
<td>n/a</td>
<td>2.232</td>
<td>1.849</td>
<td>1.806</td>
<td>2.004</td>
<td>3.492</td>
<td>4.314</td>
<td>7.967</td>
<td>11.395</td>
</tr>
<tr>
<td>W_D</td>
<td>n/a</td>
<td>2.657</td>
<td>2.216</td>
<td>2.111</td>
<td>2.249</td>
<td>3.787</td>
<td>4.541</td>
<td>8.112</td>
<td>10.818</td>
</tr>
<tr>
<td>R6 Prediction</td>
<td>1.98</td>
<td>1.505</td>
<td>1.366</td>
<td>1.302</td>
<td>1.290</td>
<td>1.612</td>
<td>1.745</td>
<td>2.45</td>
<td>3.02</td>
</tr>
<tr>
<td>$\lambda=\infty$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>H R&amp;T</td>
<td>n/a</td>
<td>2.071</td>
<td>1.906</td>
<td>1.912</td>
<td>2.074</td>
<td>2.307</td>
<td>n/a</td>
<td>3.515</td>
<td>5.162</td>
</tr>
<tr>
<td>GR&amp;T</td>
<td>n/a</td>
<td>2.071</td>
<td>1.905</td>
<td>1.912</td>
<td>2.075</td>
<td>2.309</td>
<td>n/a</td>
<td>3.532</td>
<td>5.234</td>
</tr>
<tr>
<td>W_D</td>
<td>n/a</td>
<td>2.067</td>
<td>1.912</td>
<td>1.911</td>
<td>2.055</td>
<td>2.262</td>
<td>n/a</td>
<td>3.344</td>
<td>4.703</td>
</tr>
<tr>
<td>R6 Prediction</td>
<td>1.96</td>
<td>1.5</td>
<td>1.41</td>
<td>1.38</td>
<td>1.37</td>
<td>1.36</td>
<td>1.44</td>
<td>1.58</td>
<td>1.95</td>
</tr>
</tbody>
</table>

Good agreement exists between the HR&T and GR&T local approaches in the predictions of $L_r$ at fracture with a average deviation of 0.2 %. Good agreement exists between the HR&T and W_D for loading conditions $\lambda=0.5$ and $\lambda=\infty$, with average deviations of 2%. Larger amounts of deviation between HR&T and W_D is observed for $\lambda=0.75$, with a average deviation of 10%, with W_D providing values of $L_r$ at failure up to 20% higher than
the Rice & Tracey local approaches. The R6 approach consistently provides conservative
failure predictions.

4.8.6.4. Comparison with Option 1 FAD analysis:
Figure 130, Figure 131 and Figure 132 present the variation of $L_r$ at ductile initiation
defined using: the HR&T model, GR&T model, work of fracture model, R6 Option 1 using
specimen J validity to define initiation toughness, and R6 Option 1 using lower bound
$J_{0.2BL}$ obtained from the MLBML model for $\lambda=0.5$, 0.75 and $\infty$ respectively. The pipe is
assessed to fail by plastic collapse before ductile initiation in all cases according to the LA
analysis. Compared with the LA results, the R6 methodology is conservative in all
assessments.

Figure 130: Prediction of $L_r$ at ductile failure against crack depth – Internally circumferentially cracked pipe
$-\lambda=0.5$
New insights into the competition between ductile tearing and plastic collapse

Figure 131: Prediction of \( L_r \) at ductile failure against crack depth – Internally circumferentially cracked pipe – \( \lambda = 0.75 \)

Figure 132: Prediction of \( L_r \) at ductile failure against crack depth – Internally circumferentially cracked pipe – \( \lambda = \infty \)

Figure 133 presents the percentage difference between the average \( L_r \) at ductile initiation predictions using the local approaches and the \( L_r \) at ductile initiation prediction using the R6 Option 1 using lower bound \( J_{0.2BL} \) obtained from the MLBML model for \( \lambda = 0.5, 0.75 \) and \( \infty \)
New insights into the competition between ductile tearing and plastic collapse

Figure 133: Percentage difference between the average $L_r$ at ductile initiation predictions using the local approaches and the $L_r$ at ductile initiation prediction using the R6 Option 1 using lower bound $J_{0.2BL}$ obtained from the MLBML model for $\lambda = 0.5, 0.75$ and $\infty$ for a range of crack depths

The R6 methodology is over 20% conservative for all cases analysed. It tends to be more conservative for larger defects ($a/t>0.5$).

4.8.7. Constraint corrected Failure Assessment Diagram

The constraint sensitivity of the material can be accounted for using the constraint corrected FAD defined in R6 [35]. The Failure Assessment Curve (FAC) can be modified using equation (107) and (108) to correct for constraint dependant fracture toughness.

$$K_r = f(L_r)[1 + \alpha(-\beta L_r)^k]$$  \hspace{1cm} (107)

With

$$\beta_T = \frac{T/\sigma_T}{L_r}; \quad \beta_Q = \frac{Q}{L_r};$$  \hspace{1cm} (108)

The fracture locus fitting parameters $\alpha$ and $k$ were defined in section 4.7 for $\beta_T$ and $\beta_Q$. For the T-stress correction scheme, curve fit parameters of $\alpha=0.25$ and $k=1.25$ were defined. For the J-Q correction scheme, curve fit parameters of $\alpha=0.55$ and $k=1.25$ were defined in section 4.7.
The effect of geometry and loading condition on crack tip constraint is accounted by $\beta_T$, but does not take into account constraint loss due to extensive plasticity. The T-stress for a specific geometry and loading condition can be defined from an linear elastic finite element analysis. Defining $\beta_T$ is straightforward as $T/\sigma_y$ is proportional to the load applied, which is proportional to $L_r$. Figure 134 presents the derivation of $\beta_T$ for an internally circumferentially cracked pipe with a loading condition $\lambda=0.5$.

The effect of material plasticity, geometry and loading condition on the crack tip constraint is accounted by $\beta_Q$. The $Q$ parameter has to be defined for a specific geometry at a range of $L_r$ in order to define $\beta_Q$. Figure 135 presents the derivation of $\beta_Q$ for an internally circumferentially cracked pipe with a loading condition $\lambda=0.5$.

The $Q$ vs. $L_r$ relationship for this particular geometry, loading conditions and crack depth was obtained from the FEA analysis. The stress field ahead of the defect was exported at values of $L_r$ of 1.02, 1.22 1.43 and 1.9 representing $J$ values of 33kJm$^{-2}$, 157 kJm$^{-2}$, 458 kJm$^{-2}$ and 2534 kJm$^{-2}$ respectively. The issue of a physically meaningful normalised distance $r_0/J$ occurred. The conventional distance of 2 was used for the values of $J$ lower than 500kJm$^{-2}$, at which point distance dependant $Q$ was observed. For values of $J$ higher than 1500 kJm$^{-2}$, a normalised distance of 0.1 was used, enabling the calculation to be three or more elements ahead of the crack tip and beyond the finite strain condition for this material.

Figure 134: Evolution of $T/\sigma_y$ against $L_r$ in the pipe geometry - $\lambda=0.5 - a/t=0.5$

Figure 135: Evolution of $Q$ against $L_r$ in the pipe geometry - $\lambda=0.5 - a/t=0.5$
New insights into the competition between ductile tearing and plastic collapse

The Option 3 Failure Assessment curve for the internally circumferentially cracked pipe with λ=0.5 was modified using equation (107). For the T-stress FAC correction scheme, the following parameters defined above were used: β_T=-0.4289, α=0.25 and k=1.25. For the Q parameter FAC correction scheme, the following parameters defined above were used: β_Q=-0.8002, α=0.55 and k=1.25. Figure 136 presents the Option 3 FAC, the β_T corrected FAC and the β_Q corrected FAC.

![Figure 136: Q parameter constraint corrected Failure Assessment Diagram – pipe geometry – λ=0.5 – a/t=0.5](image)

The constraint corrected FAD methodology using β_T predicts failure at L_r=1.42 and the β_Q methodology predicts failure at L_r=1.66. This compares to an L_r=1.27 predicted by the conventional R6 FAD and a prediction of L_r=1.67 for the Local Approach methodologies. A slight variation exists between the β_Q failure prediction and the Local Approach methodologies failure predictions. This can be explained by the errors introduced in the definition of α and k fitting parameters and the definition of the β_Q parameter.
Chapter 5  Discussion

This chapter discusses the main results presented in the previous section with respect to the aims of the project, which was to investigate the competition between the ductile tearing and plastic collapse failure mechanisms in austenitic stainless steel. In the process, a methodology to define failure of components where no valid fracture toughness measurements are available was defined. The following discussion addresses five key issues:

- The difficulty in obtaining valid fracture toughness data for low yield high toughness materials
- The calibration of Local Approach models for ductile initiation using 3D FEA
- The analytical derivation of valid fracture toughness and constraint corrected fracture toughness.
- Simplified constraint corrected Failure Assessment methodology
- Failure of a primary system pipe under a range of loading conditions.

Multiple methodologies exist to perform assessments of structures containing a defect. In order to emphasis the results obtained in this work, a decision map (Figure 137) has been defined. This is aimed at providing the structural integrity engineer with the most appropriate methodology for failure assessment depending on the level of analysis required. Each section is discussed in relation to the results obtained in Chapter 4.

![Figure 137: Failure assessment of structure methodology decision map](image-url)
New insights into the competition between ductile tearing and plastic collapse

As the local approach criteria investigated in this study predict ductile tearing conditions, the current methodology is specifically for materials failing in a ductile manner. This methodology is particularly appropriate for low yield high toughness materials failing under large plasticity conditions.

The discussion itself is divided in two main sections. The first section discusses in detail the novel methodologies defined in this work: the limitation of experimental determination of fracture toughness for low yield high toughness materials, and the use of local approaches to define crack tip initiation. Section 5.3. describes in detail the Local Approach Failure Assessment Diagram defined in this work and discusses the results obtained in section 4.8.6. Section 5.4. describes the use of local approach methodologies to obtain valid fracture toughness and constraint dependant fracture toughness, and its application to the constraint corrected FAD methodology. In this section, only the results obtained from the pipe material tests will be discussed.

The second section discusses the practical applications of the methodologies discussed in section 5.1. in relation to defining the failure mechanisms of a primary system pipe manufactured from 304(L) stainless steel. The first sub-section discusses the results obtained analytically in relation to experimental results of pipe tests. The second sub-section discusses the limitations of local approach methodologies to define fracture, specifically the use of miniature specimen.
Section 1: Defining Failure Assessment methodology

5.1. **Defining Fracture properties for use in a structural integrity assessment**

The first step in any structural integrity assessment of components of structures is the definition of fracture toughness properties. This section discusses the limitations of fracture toughness definition using test standards, and the experimental definition of the constraint dependent fracture locus and its limitations (Figure 138).

![Figure 138: Failure assessment of structure methodology decision map – Experimental definition of fracture toughness valid according to test standards](image)

5.1.1. **Defining initiation toughness experimentally:**

One of the principle issues with performing the assessment of low yield high toughness material such as the 304(L) stainless steel analysed in this study, is obtaining lower bound fracture toughness data, valid according to major fracture toughness testing standards, which is required by structural assessments methodologies such as R6 [35].

The validity limits set in the test standards were set to ensure that the experimental J-integral obtained was close to Small Scale Yielding solutions. This is essential due to the
fact that, in small scale yielding conditions, the J-integral is proportional to the crack tip stress state and independent of the specimen geometry. In fracture toughness specimen, the crack tip condition can deviate from small scale yielding conditions due to extensive plasticity in the plane of crack propagation (in-plane constraint loss), or due to deviation from plane strain conditions at the centre of the specimen due to insufficient specimen or structure thickness (out-of-plane constraint loss), leading to loss of proportionality between the J-integral and the crack tip stress field. In the specimens tested in this study, both extensive in-plane and out-of-plane constraint loss was observed, leading to large initiation toughness constraint dependence. Large amounts of plastic deformation was observed in the crack propagation plane experimentally using Digital Image Correlation measurements with over 2% plastic strain extending throughout the un-cracked ligament (section 2.3.) at initiation. Furthermore, analytically defined J-integral show extensive variation through thickness with very little plane strain conditions (Figure 144). The fracture toughness measured in the SEN(B)_D, SEN(B)_S and SEN(T)_D were 313%, 419% and 497% higher than the high constraint fracture toughness defined analytically using the local approach methodology. Furthermore, the deviation loss of proportionality defined by the Q parameter was -1.1, -1.6 and -2.3 for the SEN(B)_D, SEN(B)_S and SEN(T)_D respectively. These measurements cannot be used for assessment purposes and no methodology exists in the literature to predict high constraint fracture toughness from non-valid tests. Furthermore, a large Resistance curve dependence on geometry was observed, making these unsuitable to predict crack instability in components.

In order to obtain lower bound toughness measurements, specimen needs to satisfy the size requirements defined by fracture toughness testing standards. These size requirements are a function of the measured initiation toughness (section 2.2.3., equation (66), (67), (68)). We can therefore use the experimentally derived fracture toughness and the high constraint fracture toughness value obtained using the Boundary Layer Model to bind the specimen size requirement.

From experimental data available, we would have to consider the lower bound initiation toughness obtained from the SEN(B)_D specimens. If we consider the Single Edge Cracked specimen geometry, we can define the minimum size dependent on the specimen thickness. The minimum specimen thickness required to obtain valid fracture toughness
data for toughness up to the SEN(B)_D initiation toughness, the specimen thickness would have to be 71 mm, 117 mm and 142 mm for the requirements defined in ESIS P2-92, ASTM 1820 and BS 7448-4 respectively (Figure 139). The experimentally derived fracture toughness measurements were obtained under low constraint conditions, leading to a higher bound value of fracture toughness and a conservative estimate of size requirement in order to obtain SSY solution.

If we consider the high constraint initiation toughness measurement obtained analytically from the MBLML model, the minimum specimen thickness required to obtain valid fracture toughness would be 23.5 mm, 38.5 mm and 46.8 mm for the requirements defined in ESIS P2-92, ASTM 1820 and BS 7448-4 respectively (Figure 140). Due to the requirement of have valid crack growth data, these requirements are a lower bound and will not provide valid J-resistance curves.

Furthermore, due to possible variations of material toughness depending on the crack propagation orientation, the specimen should be extracted from the appropriate orientation [35]. In the pipe material, the critical crack propagates through thickness, leading to a loss of integrity of the component. It is geometrically impossible to obtain specimen of the required size to obtain valid fracture toughness measurements for the 304(L) stainless steel pipe.

![Figure 139: J validity limit as a function of thickness with initiation toughness defined as the lower bound toughness obtained from the MBLML model](image1)

![Figure 140: J validity limit as a function of thickness with initiation toughness defined as J0.2BL obtained from the SEN(B)_D specimen](image2)
New insights into the competition between ductile tearing and plastic collapse

5.1.2. *Initiation toughness effect of Failure Assessment Diagram:*
When valid toughness data cannot be obtained experimentally, the specimen validity limit is used in a structural integrity assessment. The R6 loading line used to define the structures failure mechanism and failure load is proportional to the material toughness and the use of specimen validity limits can lead to over-conservative estimates of failure. Figure 141 shows the failure points for the internally circumferentially cracked pipe analysed in sections 4.8.3, with the failure points defined using the specimen validity limit, the $J_{0.2}$ initiation point measured in the SEN(B)_D specimen and the lower bound high constraint initiation toughness predicted using the MBLM model.

![Figure 141: R6 Option 1 & 3 FAD with loading line defined using $K_{0.2}$, $K_{\text{validity}}$ and $K_{\text{MBLM}}$](image)

In this work, we have shown that the three Local Approach methodologies adequately predicted failure by ductile tearing in specimen with varying constraint conditions at initiation. In order to investigate the conservatism provided by the R6 FAD, we assume that this methodology is transferable from experimental specimen to structural components.

Table 28 provides the percentage difference between the failure conditions predicted using the R6 option 1 curve with fracture toughness defined as the $J_{\text{validity}}$ or the $J_{\text{MBLM}}$, compared to the failure conditions predicted using the local approach FAD methodology; for the circumferentially cracked pipe investigated, for all loading conditions and crack depths. The R6 failure prediction using the lower bound fracture toughness provides a conservative
New insights into the competition between ductile tearing and plastic collapse

failure prediction, with a minimum of 23% conservatism in terms of $L_r$. Using lower bound fracture toughness provides even large margins, providing in most cases prediction as much as 40% lower than the failure defined by the Local Approach methodology.

<table>
<thead>
<tr>
<th>Conservation (%)</th>
<th>$\lambda=0.5$</th>
<th>$\lambda=0.75$</th>
<th>$\lambda=\infty$</th>
</tr>
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<tbody>
<tr>
<td></td>
<td>MBLM</td>
<td>$J_{\text{validity}}$</td>
<td>MBLM</td>
</tr>
<tr>
<td>a/t 0.2</td>
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<tr>
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<tr>
<td>a/t 0.5</td>
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</tr>
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<td>a/t 0.8</td>
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<td>69</td>
</tr>
<tr>
<td>a/t 0.9</td>
<td>61</td>
<td>70</td>
<td>73</td>
</tr>
</tbody>
</table>

Table 28: Percentage difference between $L_r$ at initiation defined using the MBLM or $J_{\text{validity}}$ limit and the local approach defined failure.

5.1.3. **Comparison with fracture data obtained in the literature:**
Mills produced a study on the variation in initiation toughness in different heats of 304 stainless steel [101]. He compared fracture toughness from previous studies to the tests performed on various heats of stainless steel. At room temperature, large variations in initiation toughness were measured, ranging from 460 to 1600 kJm$^{-2}$. This variation can be attributed to variations in material properties from heat to heat. None of his tests yielded valid initiation toughness measurements and the variation should also be attributed to variations in constraint conditions at initiation.

5.1.4. **Defining Constraint dependant fracture toughness experimentally**
In order to make sound assessment of the structures containing a sharp defect’s capacity to withstand a load, the constraint effect on initiation toughness should be assessed. R6 provides a methodology to correct the failure assessment diagram for fracture toughness constraint dependence based on two parameter fracture mechanics (see literature review section 1.3.2. ). For large scale yielding conditions, O’Dowd and Shih J-Q parameter [21,
30] should be used to correct the FAD [29, 35]. The constraint dependent fracture toughness must be defined experimentally from fracture toughness specimens that fail under a range of constraint conditions. Standards for low constraint fracture toughness specimen are in the process of being development and recommended guidelines such as DNV-RP-F108 [102] can be used to test Single Edge Cracked specimen in tension.

5.1.4.1. Defining J-Q fracture locus:
The two-parameter methodology corrects for deviation of the stress state ahead of the defect away from SSY solution. This deviation is conventionally defined at a normalised distance of 2 [29]. This distance was designed to be out-side the finite strain region but still within the J-Q annulus.

5.1.4.1.i. Initiation toughness constraint dependence:
The conventional two parameter fracture mechanics definition is limited by the size of the J-Q annulus. The J-Q annulus is defined by O’Dowd and Shi as the region where the opening stress vs. normalised distance (\( r \sigma_y / J \)) obtained under SSY conditions is proportional to the stress distribution obtained ahead of the specimen’s crack tip. Loss of proportionality conventionally occurs at low (\( r \sigma_y / J \)) values in the large strain region ahead of the crack tip and at large (\( r \sigma_y / J \)) values due to boundary effects. A normalised distance of 2 was recommended by O’Dowd and Shi to ensure that the region investigated lies within the J-Q annulus.

The non-proportionality effect encountered outside the J-Q annulus was identified by Joyce and Link [32], where they showed the dependence of Q on the normalised distance chosen ahead of the crack tip. The evolution of Q with normalised distance under a range of J conditions was investigated in shallow and deep cracked SEN(B) specimen with a material with a hardening exponent of n=10. For the lower values of J analysed, Q was found to be independent to the measurement point for shallow cracked geometries (Figure 143: a, b & c). Furthermore, in the shallow cracked specimen, Q was found to be independent on the distance for all loadings considered (Figure 142: a). For deep cracked specimens, Q was found to be increasingly dependent to the normalised distance with increase in the values of J (Figure 142: b, c). In all cases, higher bound values of Q were obtained within the proportionality region.
New insights into the competition between ductile tearing and plastic collapse

Figure 142: $Q$ vs. distance ahead of the crack tip for SEN(B) specimens, from Joyce & Link [32], analysing results from O’Dowd & Shi [21, 30]

In bending conditions, the $J$-$Q$ annulus is mainly limited by the effect of the far field compression on the tensile stress field in uncontained plasticity conditions. In tensile loading conditions, the main limiting factor is the physical size of the specimen.

In our SEN(B)$_S$, SEN(B)$_D$ and SEN(T)$_D$ specimen, at failure load, the normalised distance of 2 lied outside the $J$-$Q$ annulus (Section 4.5.1. ). The region of proportionality needs to be defined in this case in order to be able to apply the $J$-$Q$ two parameter constraint correction methodologies.

Figure 143 presents the $Q$ parameter normalised by the $Q$ parameter obtained at a normalised distance of 0.1 as a function of the normalised distance ahead of the crack tip in the three SEN specimen at initiation. The value of the $Q$ parameter is shown to be dependent on the normalised distance ahead of the crack tip in all specimens. A small region which provides a higher bound value of $Q$, and in which the proportionality conditions should exist can be found in all three specimen at normalised distances comprised between 0.075 and 0.125.
New insights into the competition between ductile tearing and plastic collapse

Beyond this region, the deviation between the stress field under small scale yielding conditions and the specimen crack tip stress field increases. This leads to an over-estimation of the constraint loss at initiation. The Q parameters defined in this region will provide non-accurate but conservative estimates of initiation toughness constraint dependence. The consequences of not applying the methodology correctly are therefore small when defining the J-Q fracture locus.

5.1.4.1.ii. Component constraint loss with increase in load:
The variation of constraint conditions ahead of a defect in a component as a function of Lr should be defined in order to correct the FAC for constraint effects on initiation toughness. When constraint is defined using Q parameter, and applied to cases beyond the limit load, the standard normalised distance may no longer be valid. For the pipe analysis, the normalised distance $r\sigma_y/J=2$ was found to be valid for $J<500$ kJm$^{-2}$. At higher values of J, the normalised distance had to be changed in order to lie within the J-Q annulus. The failure predictions obtained from this methodology were consistent with results obtained from the Local Approach analysis, validating the approach.
Beyond the Q-annulus, the deviation between the stress field under small scale yielding conditions and the specimen crack tip stress field increases. This leads to an over-estimation of the constraint loss in the component. This will provide non-accurate and non-conservative estimates of initiation toughness constraint dependence. Not defining Q correctly can therefore lead to an over-estimation of constraint loss, and hence would not predict the failure of the component.

5.1.4.1.iii. Defining the J-Q fracture locus:
In order to define the constraint dependant fracture toughness of a material, large amounts of test data is required. Using multi-specimen testing, a minimum of 6 valid test data is required to define initiation toughness for one constraint conditions. A range of constraint conditions is required in order to have an accurate estimation of the constraint dependant fracture locus. Furthermore, in order calibrate the curve fit for constraint dependent fracture toughness, high constraint fracture toughness measurement is required. It is therefore very expensive to define experimentally the constraint dependant fracture locus, especially in low yield high toughness materials which exhibit large amounts of constraint loss.

The analytical methodology to define constraint dependant fracture toughness and how to apply it to a constraint corrected FAD is discussed in more details in section 5.4.

5.1.5. Interim conclusions:
Obtaining valid fracture toughness measurements of low yield high toughness materials require very large fracture toughness specimen, which often cannot be obtained from the geometry analysed. The resultant use of the validity limits to define initiation toughness yield very conservative estimations of limiting conditions, which can lead to uneconomical requirements NDT analysis or replacement of the component.

When constraint corrected failure assessment methodologies are used, care should be taken when defining the Q parameter in the specimen at fracture. Definition of Q outside the J-Q annulus will yield non-transferable, but conservative definition of the J-Q fracture locus. Furthermore, definition of Q outside the J-Q annulus will over-estimate the constraint loss in a specimen, with potential non-conservative estimation of failure loads.
5.2. Implementation of Local Approach Methodologies

Conventional fracture mechanics relies on the continuum mechanic analysis through K, J-integral, J-T or J-Q approaches discussed in 5.1. The following section discusses the use of mechanistic approaches such as local approach criteria to define ductile failure in stainless steels.

5.2.1. Calibration of the critical damage criteria:

The damage criteria have to be calibrated using experimental data obtained from a range of constraint conditions. Most of the calibration methodologies in the literature rely on fracture data obtained from a range of notched tensile specimen with a range of notch root radii. This approach is valid if it is assumed that the void growth mechanism in a notched tensile specimen is the same as the void growth mechanism ahead of a sharp defect. The variation the stress field between a notched and a cracked specimen was argued to lead to non-transferable derivation of the critical local approach criteria by Sherry et al [56].

Here, the Local Approach parameters have been calibrated using experimental data from cracked specimen with a range of constraint conditions, which provides a more representative failure criterion. Due to the large stress and strain gradient ahead of a sharp defect, a critical distance over which the critical damage occurs is required. Many researchers [57] argue that the distance should be proportional to the inter-particle spacing,
which is typically 200μm for stainless steels and structural steels [58-60]. On the other hand, Pineau & Lautridou argue that the distance itself is not important as long as it is kept constant in all analysis and that the probability of finding a single inclusion within the microstructure is non-zero.

In this work, a critical area of 0.2*0.2mm$^2$ ahead of the defect was defined. This distance is representative of the area of damage at initiation defined experimentally through $J_{0.2BL}$. This area would contain an average of 10 gains (see Figure 53 & Figure 54), leading to a high probability of including a void initiation site. This is also the same area as used in the literature for this material, but defined as a typical spacing between inclusions in structural steels.

### 5.2.2. Averaging methodology

Local approaches are typically calibrated using 2D plane strain models in the case of fracture specimens [60, 103] or axis-symmetric models in the case of notched tensile specimens. Nevertheless, the 2D plane strain assumption is no longer fully valid when analysing large strain conditions, where out of plane constraint loss occurs through the specimen thickness. The samples used to calibrate the local approach criteria were non-side grooved, leading to non-negligible variations in the Local Approach parameter through thickness (see Figure 144, Figure 145 and Figure 146).
Figure 146: Variation of the Work of Fracture local approach parameter through thickness at initiation

The critical local approach criteria are calibrated against experimentally derived $J_{0.2BL}$ initiation toughness values. The experimental methodology as defined by ESIS P2-92, equates the load displacement curve obtained experimentally to the $J$-integral of a 2 dimensional plane strain Finite Element Model using the $\eta$ factor (section 3.1 ). At the same time, the crack growth measurement is an average through thickness using the 9 point average methodology. In order to be consistent with the experimentally derived measurements, a specific averaging scheme was defined. The $J$-integral values at the centreline of the specimen, where conditions are closest to plane strain conditions, were used.

Furthermore, the averaging methodology defined in section 3.7.3. reduces the scatter between critical Local Approach for SEN(B)_D, SEN(B)_D and SEN(T)_S compared to simply taking the value at the centre element of the specimen. Table 29 presents the local approach criteria values, at initiation, for all three specimen geometry using the averaging methodology and at the centre of the specimen.
New insights into the competition between ductile tearing and plastic collapse

Table 29: Critical Local Approach criterion calculated for the SEN(B)_D, SEN(B)_S and SEN(T)_D calculated using the through thickness averaging methodology compared to critical local approach at the centre of the specimen

<table>
<thead>
<tr>
<th></th>
<th>SEN(T) Deep Crack</th>
<th>SEN(B) Shallow Crack</th>
<th>SEN(B) Deep Crack</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>J_{0.2BL} experimental</td>
<td>2430</td>
<td>2100</td>
<td>1530</td>
<td>n/a</td>
</tr>
<tr>
<td>HC Rice &amp; Tracey value (Centre)</td>
<td>3.04</td>
<td>3.12</td>
<td>3.34</td>
<td>3.17</td>
</tr>
<tr>
<td>HC Rice &amp; Tracey value (average)</td>
<td>2.44</td>
<td>2.44</td>
<td>2.44</td>
<td>2.44</td>
</tr>
<tr>
<td>G Rice &amp; Tracey value (centre)</td>
<td>3.0</td>
<td>3.08</td>
<td>3.30</td>
<td>3.13</td>
</tr>
<tr>
<td>G Rice &amp; Tracey value (average)</td>
<td>2.398</td>
<td>2.404</td>
<td>2.409</td>
<td>2.40</td>
</tr>
<tr>
<td>Work of Fracture value (centre)</td>
<td>13491</td>
<td>13832</td>
<td>14147</td>
<td>13823</td>
</tr>
<tr>
<td>Work of Fracture value (average)</td>
<td>10344</td>
<td>10289</td>
<td>9710</td>
<td>10114</td>
</tr>
</tbody>
</table>

5.2.3. Local Approach criterion as a constraint independent fracture toughness measurement

There are a number of advantages to using Local Approaches to predict initiation toughness. The first advantage is that the value of the local approach parameter at initiation is independent of $L_r$ and constraint conditions. This means that the local approach model can be calibrated against non-valid fracture toughness data. In this work, the local approach models were calibrated against fracture toughness data which exhibited constraint conditions of $Q=-1.1,-1.6$ and $-2.1$ in the SEN(B)_D, SEN(B)_S and SEN(T)_D respectively, leading to large J constraint dependence. Yet, local approach criteria were successfully calibrated using these specimens. It is interesting to note that the critical high constraint Rice & Tracey parameter obtained at the centre of the specimens are closest to the critical parameter defined in the literature [60] as 2.9 for a stainless steel in a critical area of $0.2 \times 0.2 \text{mm}^2$. 

213
The second advantage is that, as local approaches are constraint independent, they can be applied to a range of geometries and loading conditions. This means that one can obtain accurate predictions of initiation toughness in a components subjected to different loading conditions. The conventional high constraint J estimation approach can lead to over-conservatism in the assessment of components exhibiting low crack tip constraint conditions. Furthermore, the amount of conservatism that arises from the J estimation scheme is hard to define without performing a constraint corrected assessment such as the Constraint corrected FAD method.

The third advantage is the relative simplicity of calibration compared to other mechanistic approaches. More complex and accurate approaches, predicting ductile initiation as well as ductile crack extension have been developed in recent years. For example, the Gurson model [49] accounts for void initiation and void coalescence, but still assumes a spherical void growth definition. More complex models such as the Benzerga model [104], accounts for material anisotropy and inclusion shape. These models are complex, requiring six and seven parameters to be calibrated for the Gurson model and the Benzerga model respectively.

5.2.4. *Interim conclusion:*
Adequately calibrated local approach methodologies provide a robust parameter to define initiation toughness, which is independent of loading conditions and the geometry of the structure being analysed. The local approach should be calibrated from at least three experimental measurements of fracture toughness from at least three different constraint conditions. When constraint loss through thickness needs to be taken account of, an averaging methodology based on the 9 point average defined in BS -7442-4 for crack front profile measurements is recommended.
5.3. **Local approach based Failure assessment methodology:**

Many of the research efforts in defining constraint effects on the Failure Assessment Diagram are based on two parameter fracture parameter analysis, J-T or J-Q [21, 30]. These approaches are appropriate when there is good knowledge of constraint dependant fracture behaviour, which requires access to a substantial amount of material testing of a range of constraint conditions. The proposed engineering methodology to define the ductile failure of components here is based on a two parameter analysis and requires considerably less test data. Similar to the R6 Failure Assessment Diagram, proximity to the plastic collapse limiting condition is predicted by the $L_r$ parameter defined using the R6, with the limiting condition defined by $L_{rmax}$. Proximity to fracture is defined by an appropriate local approach parameter. The fracture limiting condition is defined using the critical local approach criteria calibrated against experimental data to predict initiation toughness, here related to 0.2mm of crack propagation through void growth and coalescence (section 4.2).

This leads to a failure assessment diagram which is similar in concept to the R6 FAD. The abscissa is defined by the $L_r$ parameter in the normal way. The ordinate is defined by the evolution of the local approach parameter normalised by its critical value, i.e. $LA_r =$
New insights into the competition between ductile tearing and plastic collapse

$LA/\frac{LA_c}{c}$, where $LA$ is the value of the local approach at a specific loading condition and $LA_c$ the critical value of the local approach defining initiation toughness. A failure assessment boundary is then defined by $L_{\text{max}}$ and $LA=1$. For geometry and loading condition a failure assessment point can be plotted on the diagram and be assessed against this boundary.

A variation of the failure assessment point as a function of the applied load can be explored numerically so that a loading line can be plotted and the critical load to failure predicted.

Note that in the conventional R6 approach the loading line is linear as both $K_r$ and $L_r$ are proportional to Load. This is not the case here.

This approach is applicable to each of the three LA models explored in this research, i.e. GR&T, HR&T and W_D and results obtained from this methodology are presented in section 4.8.6.

5.3.1. Application of the methodology

Analysis of components for defect tolerance requires an understanding of the failure mechanisms under a range of conditions. For example, if a defect is found during an NDE test, the structural integrity engineer must take into account uncertainties in defect length and do the analysis for a range of loading conditions. The Local approach methodology allows for a simple crack depth and loading condition sensitivity analysis, following the approach defined in Figure 147.

Figure 148 defines the approach required for the Constraint Independent R6 FAD approach.
New insights into the competition between ductile tearing and plastic collapse

Local Approach Methodology

1. Define LA_{critical}
2. Create Model
3. Calculate PL for specific crack depth and loading condition
4. Calculate Loading Line for specific crack depth and loading condition
5. Sensitivity analysis required
   - Yes
   - No: Plot LA vs. L_r
6. Modify crack tip or loading condition

R6 Constraint Independent FAD

1. Define High constraint fracture toughness
2. Define constraint dependent fracture locus
3. Create Finite Element Model
4. Calculate PL for specific crack depth and loading condition
5. Define \( f_1 \) or \( f_2 \) for specific crack depth and loading condition
6. Modify FAD failure curve for specific crack depth and loading condition
7. Modify crack tip or loading condition
8. Calculate Loading line for specific crack depth and loading condition
9. Sensitivity analysis required
   - Yes
   - No: Plot Kr vs. L_r

Figure 147: Local Approach to defining the failure mechanism of a component containing a defect

Figure 148: R6 constraint corrected FAD approach to defining the failure mechanism of a component containing a defect
New insights into the competition between ductile tearing and plastic collapse

The local approach methodology was defined to provide a complimentary approach to analysis requiring accurate failure analysis combined with sensitivity analysis. There are three main advantages to the local approach methodology.

- LA provides a constraint independent definition of the critical failure condition by ductile initiation. This means that only one diagram is required for the assessment of a range of constraint and crack length for a given material. The calibration of the Local Approach criteria should be done using fracture toughness specimens with a range of constraint conditions at fracture. Conversely, the R6 constraint dependent FAD requires one failure assessment curve per geometry and loading condition. Multiple cases can be presented on a single graph with the LA method, making the analysis easier to interpret, particularly for a range of crack depths.
- The analysis technique is versatile and is able to address primary and primary + secondary loading conditions. For example, the high Constraint Rice & Tracey local approach has been shown to be adequate for analysis of weld materials including cases with residual stress [105, 106].
- Finally, the analysis of multiple loading conditions and crack depths requires fewer analytical steps, making the approach more economic and efficient than the constraint corrected FAD based on $\beta_Q$ correction.

The three main disadvantages of constraint corrected methodologies are:

- The Local Approach Based FAD is specific to a single mechanical failure.
- The methodology requires extensive use of Finite Element Analyses.
- Sufficient material should be available in order to calibrate the local approach criteria.

5.3.2. Local Approach Methodology example case: Primary system piping with circumferential defect

The prediction of failure of a component is sensitive to the analytical methodology employed. Conventionally, an R6 Failure Assessment methodology would be used to define the failure of a component containing a defect real or postulated. The initiation
New insights into the competition between ductile tearing and plastic collapse

toughness obtained from experimental measurements would conventionally be used and if no valid fracture toughness measurements could be obtained, the specimen validity limit would be used.

Figure 149 shows that conventional R6 failure assessment methodologies would not be adequate for the analysis of the failure of the pipe system defined in section 4.8. It is interesting to note that the conservatism in the R6 assessment procedure is dependent on the geometry and crack depth.

![Graph showing Lr at ductile initiation for a circumferentially cracked pipe laded under internal pressure and tension (λ=0.5) for a range of crack depths](image)

**Figure 149: Lr at ductile initiation for a circumferentially cracked pipe laded under internal pressure and tension (λ=0.5) for a range of crack depths**

According to these results, more complex fracture analysis or component replacement would be mandatory. Experimental and analytical results have shown that these results are over-conservative.

5.3.3. **Interim Conclusions:**
The LA based FAD methodology provides a novel methodology to predict the failure mechanism for a component failing in a ductile manner. It allows a constraint independent analysis, with graphical interpretation of the results, but requiring advanced analytical methodologies.
5.4. **Defining fracture toughness locus analytically:**

As discussed in section 5.1, it is often impractical or impossible to obtain valid fracture toughness data from fracture toughness specimen obtained from a component. The fracture toughness measurements obtained from the SEN(B)\_D were 300% above validity limits and the crack tip stress field deviated from SSY condition by a Q of -1.6.

Sherry and Wilkes showed that constraint dependant fracture toughness in the brittle regime could be predicted using a combination of local approach and boundary layer model [ref]. A similar approach was used to predict high constraint fracture toughness and constraint dependant fracture toughness under ductile conditions. The methodology uses the unique relationship between the local approach criteria and constraint condition. At each initiation condition, constraint is defined using the J-Q two parameter local approach.

5.4.1. **Defining the J-Q constraint correction method:**

The methodology to define the constraint dependant fracture locus is defined using a flow diagram (Figure 150). The methodology comprises of seven distinctive steps:

- **Step 1:** *Measure initiation toughness experimentally using recognised standards*:
  
  If non-valid results are obtained, test specimen of a different constraint condition in order to calibrate local approach. It is recommended to test shallow cracked
specimen using the same geometry as defined in the test standards. If a wider spread of constraint conditions are required, Single Edge Notched tensile specimen can be tested according to DNV-RP-F108[102]. Unless additional surface information is required, such as plastic zone evolution ahead of the crack tip, it is recommended to test side-grooved specimen following the recommendation in BS-7448-4[6]. This will reduce the variation of constraint conditions through thickness.

- **Step 2: Calibrate LA parameter:**

  Adequate local approach should be selected to describe the fracture process examined. In the case of ductile fracture, the High Constraint Rice & Tracey, Generalised Rice & Tracey and the Work of Fracture local approach criteria are recommended.

  The critical local approach parameter should be calibrated using the methodology defined in section 3.7.3. in order to account for the variation in the value of the damage parameter through thickness.

  The mesh can be defined using two methodologies. In the literature, the mesh size is recommended to be proportional to the distance between the two inclusions ahead of the crack tip [57]. This approach was shown to be adequate for a range of materials.

  This study uses a crack tip mesh the same size as the crack extension defining initiation conditions. For J initiation defined by ASTM, BS and ESIS standard, the crack tip mesh would be x=0.2 and y=0.2, with x the direction of crack propagation, and y the normal to the crack propagation plane. When performing three dimensional analyses, the element thickness z should be adequately refined to capture the variation of damage through thickness. The element thickness can affect the averaging methodology if the mesh is too coarse.

- **Step 3: Define Boundary Layer Model with adequate crack tip mesh:**

  A boundary layer model with adequate crack tip mesh should be constructed using the approach described in section 3.4. The crack tip mesh should be the same length and height as the one used in the 3D model used to calibrate the local approach.

- **Step 4: Apply a range of constraint conditions between 0>T>-1, whilst ensuring that the contained plasticity requirements are met. This is discussed in more details below.

- **Step 5: Define J at initiation for each applied constraint conditions**
Plot the J vs. Local Approach criteria parameter. The J-integral at which the loading line intersects the critical LAc is the initiation toughness for that specific constraint condition. Relating the values of J-integral with the T/σy applied the J vs. T/σy fracture locus can be constructed. In order to define the α and k parameters, the high constraint toughness must be defined from the loading case T/σy=0. α and k can be defined from the K/K_{mat} vs. T/σy relationship to be used with the βT constraint correction parameter.

**Step 6: Define Q at initiation for each applied constraint conditions**

The Q parameter at each initiation condition for different constraint conditions can be defined. The Q parameter can be defined at the normalised distance rσy/J=2. The only limitation to the Q analysis in Boundary Layer Models is that the analysed distance should be ahead of the finite strain region ahead of the crack tip. The α and k parameters can be related to the K/K_{mat} vs. Q fracture locus to be used with the βQ constraint correction parameter.

**Step 7: Validate α and k parameters**

Plot the (K/K_{mat})-T and (K/K_{mat})-Q fracture locus obtained analytically. Extend the fracture locus obtained experimentally using the values of α and k defined analytically to validate the results with the experimental results.
5.4.2. Defining material properties in relation to T/σy

Low yield high toughness materials such as the 304(L) stainless steel tested in the work exhibit an early deviation from the elastic line define by the Young’s modulus. In our material, deviation from the loading line occurred at a stress of 80 MPa, and the measured 0.2% proof stress was 250 MPa. In the material definition used in the model, the non-linear behaviour is defined from the deviation from the elastic loading line. On the other hand, the T/σy value is defined from σ₀.2, which is consistent with the definition used by Sherry and Wilkes[107]. This means that although you are applying a T/σy=-0.5, the effective T/σy applies is -1.5. This means that at high T/σy, the plasticity condition expands beyond the boundary of the model. When non-contained plasticity conditions are reached the T-stress at the crack tip will no longer be the same as the one applied through the
displacement of the boundary nodes using equation (83) and equation (84). Nevertheless, since the plastic strains are small, the effect on the effective T-stress applied is negligible for T/σy ≥ 0.6. However, J-integral path dependence was experienced for T/σy ≤ -0.6. Results in the literature has not revealed this issue and the relationship between T and Q has be defined for conditions up to T/σy = -1.

The Q parameter is dependent on the deviation of the crack tip stress field from SSY solution. This means that the analysis is independent of the plastic zone size ahead of the defect. Nevertheless, the relationship between T and Q is no longer valid in fully plastic conditions. This is due to the fact that the applied T is no longer the same as the constraint condition ahead of the defect defined by T.

The values of α calibrated from the (K/K_{mat})-T and (K/K_{mat})-Q were 0.3 and 0.5 respectively, with k is the same for both analysis. The deviation can be explained by the effect of uncontained plasticity conditions, with the T-stress applied through far field displacement no longer being the same as the T-stress at the crack tip.

5.4.3. **Comparison with existing results**

This methodology can be applied to define lower bound initiation toughness and the J-T and J-Q fracture locus. The high constraint fracture toughness for this material was estimated to be 485 kJm$^{-2}$. Although no valid lower bound initiation toughness measurements of 304(L) stainless steel could be found in the literature, the initiation toughness estimated using this procedure is consistent with the lower bound fracture toughness obtained by Mills [101].

Furthermore, this methodology was validated using the experimental J_{initiation}-Q values obtained experimentally. The J-Q fracture toughness was calculated using the proposed methodology and the R6 curve fit approach was applied. The lower constraint fracture toughness prediction obtained from the curve fit was consistent with the measured fracture toughness values obtained experimentally. This validates the methodology for this material. Analysis of other materials would be required to validate adequately the methodology proposed.

This methodology can be applied for a range of materials as long as you have an adequate, experimentally calibrated local approach criteria and the material stress-strain behaviour
5.4.4. Application of the methodology to define R6 FAD and constraint corrected FAD

5.4.4.1. Conventional FAD approach
In order to ensure the validity of the conventional R6 Failure Assessment procedure, valid, estimations of lower bound fracture toughness are required. The analytical methodology provides the engineer an approach to determine lower bound fracture toughness as an input to option 1, 2 and 3[35]. This leads to a more accurate, if still conservative, assessment of the structure. The pipe analysis performed in this study shows that the reduction in conservatism is dependent of the crack depth analysed (Figure 149).

5.4.4.2. Constraint corrected FAD
Once the lower bound fracture toughness and the constraint dependant fracture toughness has be determined using the methodology defined in section 4.7., the constraint corrected FAD defined in R6 can be constructed.
New insights into the competition between ductile tearing and plastic collapse

In order to correct the FAD, the relationship between the loading condition (Lr) and the constraint parameter must be defined for each geometry and loading conditions. The T-stress corrected failure assessment diagram allows for a correction of geometrical effects on the crack tip stress field. The relationship between Lr and T/σy is linear and can be defined using simple LEFM analysis. Nevertheless, the relationship between J and T as defined by the methodology in section 4.8.7. is less accurate, as discussed in section 5.4.2. In order to maintain the relationship between the applied displacement and the crack tip constraint conditions, contained plasticity conditions need to be maintained in the MBLML model. This leads to a limitation in the number of constraint conditions at initiation that can be defined as a function of T-stress. This in turns affects the accuracy of

Figure 151: Methodology to define the constraint corrected R6 FAD using the constraint dependant fracture locus obtained analytically
the calibration of $\alpha$ and $k$ parameters, as the parameters are calibrated over a shorter range of constraint conditions. Furthermore, as the $T$-stress does not account for plasticity induced constraint loss, ill-defined levels of conservatisms still exist at $L_r>0.9$.

The Q parameter corrected failure assessment diagram requires the derivation of the evolution of Q with increase in $L_r$. When assessing crack tip conditions under large plasticity conditions in low yield high toughness materials, assessment of Q is required at very large values of J. This leads to non-validity of the conventional distance at which Q is usually defined. Furthermore, Q should be defined from the same normalised distance. This can lead to issues when defining large ranges of constraint conditions, as no one distance will fall within the J-Q annulus for $0.2>L_r>2$. 


Section 2: Application of the failure assessment methodology

5.5. Failure of a pipe under internal pressure and remote tension

Local approach analysis has shown that under all the conditions analysed, the pipe will fail by plastic collapse prior to initiation by ductile tearing (section 5.3.2. & Figure 149). This is consistent with the results of a number of full scale tests described in the literature.

Wilkowski [87] analysed the results obtained from the BINP run by the USNRC. Three 304 stainless steel full scale pipes of different radius and wall thickness with circumferential through wall cracks (Figure 152) were tested under bending at room temperature.

![Cracked geometry](image)

*Figure 152: Cracked geometry*

The net section stress at initiation, Net section stress obtained from the pipe test at maximum load and flow stress obtained through tensile testing were reported [ref]. The flow stress was defined differently (equation (1)) from the R6 definition (equation (2)) and was corrected for in Table 30.

\[ \sigma_f = 1.15 \frac{\sigma_y + \sigma_{UTS}}{2} \quad (109) \]

\[ \sigma_f = \frac{\sigma_y + \sigma_{UTS}}{2} \quad (110) \]

Initiation toughness was not given but we can assume that the yield stress over the flow stress is usually about 1.61 for stainless steel. The yield stress for each pipe material was calculated using this assumption and the corresponding \( L_t \) values at initiation and experimental maximum load and are reported in Table 30. In all cases, the failure of the pipe occurred beyond the limit load.
New insights into the competition between ductile tearing and plastic collapse

Table 30: Analytical and experimental pipe test results

<table>
<thead>
<tr>
<th>Test</th>
<th>Wasylyk</th>
<th>Battelle [ref]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer Diameter (mm)</td>
<td>358</td>
<td>358</td>
</tr>
<tr>
<td>t (wall thickness)</td>
<td>33</td>
<td>33</td>
</tr>
<tr>
<td>R/t</td>
<td>5.42</td>
<td>5.42</td>
</tr>
<tr>
<td>defect geometry</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Circumferential internal crack a/t=0.5</td>
<td>1.62</td>
<td>1.75</td>
</tr>
<tr>
<td>Circumferential internal crack a/t=0.2</td>
<td>2.04</td>
<td>1.947</td>
</tr>
<tr>
<td>Circumferential internal crack a/t=0.5</td>
<td>1.61</td>
<td>1.61</td>
</tr>
<tr>
<td>Circumferential through wall crack; θ=66.7</td>
<td>1.61</td>
<td>1.61</td>
</tr>
<tr>
<td>Circumferential through wall crack; θ=66.7</td>
<td>1.61</td>
<td>1.61</td>
</tr>
<tr>
<td>Loading condition</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Lr at initiation</td>
<td>1.61</td>
<td>1.61</td>
</tr>
<tr>
<td>Lr max tensile</td>
<td>1.61</td>
<td>1.61</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The experimental results reported from the Battelle report are theoretically more severe loading than the case analysed in this study. Nevertheless, the experimental results reported are in agreement with the analytical results obtained. The loading condition analysed (λ=0.5) is more realistic of a pipe in normal operation, being the equivalent of a closed looped pressurised system.

Shibata’s [88] analysis of the JAERI ductile pipe fracture test shows similar results to the ones predicted in this study. A series of stainless steel pipes with an outer radius of 165.2mm and thickness 10.8mm was tested with part through circumferential crack of angle 0=0, 45 and 90 degrees with a range of crack depths tested under bending at room temperature. The Limit load was defined from equilibrium equations with the maximum stress set at the flow stress of the material. As discussed in the literature review, this provides upper bound predictions of plastic collapse conditions. The results are in broad
agreement with the results obtained in this study (Figure 153). Disagreement exists at the higher crack depths, which can be explained by the difference in definition of the limit load.

Currently, under ASME BPVC Section XI IWB 3640, acceptable defect size in stainless steel components is defined using limit load solutions. This is currently not the case when following the R6 procedure, where the fracture mechanics analysis is required to define the failure condition using the FAD procedure. The pipe analysis has shown that for straight pipe manufactured from primary system 304(L) stainless steel grade materials, the failure will always occur after plastic collapse. This result is consistent with a large range of experimental results on pipe materials containing more severe defects than the ones defined in the analysis.

**Interim conclusion**

In this work, a methodology has been defined to predict accurately the failure of a component under a range of conditions. An initial investigation of the failure mode of components with low yield high toughness material characteristics was performed, and it was shown that for a straight pipe manufactured from 304 stainless steel, containing a
New insights into the competition between ductile tearing and plastic collapse

circumferential defect, loaded under tension and internal pressure, plastic collapse is the prevalent failure conditions. The results obtained are consistent with large scale experimental results. This methodology can be used to validate the ASME BPVC Section XI IWB 3640 acceptable defect size criteria for stainless steel. This would require the analysis of a range of geometries and a range of material behaviours.
5.6. **Limitations of Local Approach Methodologies**

The methodologies defined above are only valid when the failure mechanism can adequately be predicted using a local approach methodology. This requires confidence in the understanding of the failure mechanisms and confidence in the capability of the local approach to predict failure. Three main limitations identified are discussed in this subsection, the geometrical limitation of the meshing method, material variation effect on local approach methodology and the specimen size effect.

5.7. **Geometrical limitations of the approach**

In this work, a square crack tip mesh of dimensions x (crack propagation plane) and y (normal to the crack propagation plane) the same size as the 0.2mm of crack propagation considered to define the initiation toughness measurements, with adequate refinement through thickness was used. The mesh should be positioned in such a way that x direction is on the crack propagation plane. This can be problematic when the crack propagation plane is not known. Furthermore, local discontinuities in the geometry such as pipe nozzles can be incompatible with this meshing methodology. It may be possible to perform a mesh independent local approach analysis, where the local approach is defined from results derived from multiple elements over a critical area defined through x and y. Further work is required to develop and validate such methodologies.

5.7.1. **Size effect on initiation toughness:**

In this study, the variation in initiation toughness with reduction in specimen size was studied in a cold worked 304(L) stainless steel plate. A reduction in initiation toughness with a reduction in specimen size was observed. Furthermore, a higher constraint condition, as defined by Q, was shown to be prevalent in the smaller specimens. The local approach methodologies adequately predicted the trend in measured toughness with reduction in specimen size. Nevertheless, the mechanism that leads to high constraint conditions in the smaller specimen is not fully understood; and the local approach methodologies were unable to predict the actual initiation toughness values.

The reduction in fracture toughness with the reduction in specimen size is inconsistent with a large number of experimental results presented in the literature [58], where a reduction in specimen size tends to lead to an increase in measured fracture toughness, due to a loss of
New insights into the competition between ductile tearing and plastic collapse

in-plane and out of plane constraint through plastic deformation. The larger specimen is more likely to experience contained plasticity conditions, hence be closer to SSY conditions, and hence initiate ductile tearing at a lower-bound value of J.

5.7.1.1. Reduction in toughness with reduction in specimen size
Examples of a reduction in measured initiation toughness with reduction in specimen can also be found in the literature however. In low yield high toughness materials such as 304 stainless steel, a reduction in initiation toughness and toughness slope with reduction in specimen size was observed by Lucon et al, Scibetta et al, Link et. al and Zhao et al [20, 103, 108, 109]. Lucon has reported a reduction of toughness and R-curve slope in CT specimen with W/B=2 with B of 25mm and 5mm at room temperature and elevated temperature. Link has observed a reduction in toughness and slope in CT specimen when reducing the specimen thickness from 2B to 1/2B with a constant W of 50. Zhao et al proposed a schematic representation of variation of fracture toughness with specimen size presented in Figure 154, where an increase in toughness with reduction in specimen size is observed up to a critical specimen thickness, after which a reduction in measured toughness with reduction in specimen thickness is measured.

Three explanations for the observed reduction in toughness can be put forward:

- The non-validity of experimental J calculation when crack growth occurs under widespread plasticity [103, 108]
- The transition from tensile ductile fracture to shear ductile fracture [110-113]
- The variation in material properties within the plate [101]
5.7.1.1.i. Transition from tensile ductile fracture to shear ductile fracture

Boa et al[110], Barsoum et al [111], Gao et al [112] and Zang et al [113] have argued that the reduction in toughness with a reduction in triaxiality can be explained by a transition from tensile controlled void growth to shear controlled void growth.

Barsoum et al. investigated the failure of Weldox steel under a range of constraint conditions using notched specimens. Figure 155 illustrates the fracture surface under high triaxiality, Figure 157 presents the fracture under low triaxiality, and Figure 156 presents the fracture surface for a specimen that failed under a transition between the two. Figure 158, Figure 159 and Figure 160 present the fracture surface of the 25mmCT, 15mmCT and 10mmCT respectively.

Figure 157 clearly shows failure by shear ductile rupture, with shallow, small elongate shear dimples. Barsoum states that the high plastic strains promote void nucleation. Void growth is then impeded by low triaxiality, with substantial shearing occurring. Void coalescence occurs through void shearing. No void shearing can be observed in the fracture surfaces of the CT specimens; hence we can conclude that failure did not occur by shear ductile rupture.

Figure 155 and Figure 158 clearly shows failure by tensile ductile fracture. The fracture surface exhibits large deep dimples, with large void growth promoted by high triaxiality factors. Void growth dominates the failure process until the deformation localises and void coalescence occurs by necking of the inter-void ligament.

Figure 156 was characterised in [111]as representative of the transition region between tensile ductile fracture and shear ductile fracture. The fracture surface exhibits clear void growth and coalescence by necking of the inter-void ligament, yet the voids are small, shallow and elongated, similarly to the void in Figure 157. Similar features are observed on the fracture surface of the 15mmCT and the 10mmCT specimens presented in Figure 159 and Figure 160 respectively.
Barsoum [111] shows that the strain at failure will first increase with reduction in triaxiality, up to a transition region, after which strain at failure will reduce with triaxiality (Figure 161). Similar behaviour was shown by Goa et al [112] In the current work, a reduction in fracture toughness was observed with specimen size (Figure 82). Nevertheless the constraint was measured analytically to increase at fracture initiation with reduction in specimen size (Figure 88). Furthermore, stress triaxiality is higher at failure in the smaller specimens. It is not clear how a tensile/shear transition region could occur under higher triaxiality and constraint conditions (i.e. 15mmCT & 10mmCT) than a specimen that fails by tensile ductile fracture (i.e. 25mmCT).
New insights into the competition between ductile tearing and plastic collapse

Figure 161: The effective plastic strain at failure vs. stress triaxiality, where solid circle and round circle denote different measures of plastic strain and the open square present results from smooth round bars.[111]

5.7.1.1.ii. Variation in material throughout the plate

Large material variability was observed in the L-T plane of the 304(L) stainless steel plate (Section 4.3, Figure 62). The specimens were extracted as close to the central region of the plate as possible, were the average yield strength was approximately 424MPa. Nevertheless, the extracted specimens still incorporated regions with higher than average hardness and yield strength (see section 4.2.2.).

The variation in yield strength was assessed for all the specimen geometries that were tested. Furthermore, it was shown in section 4.4.2.2. and 4.4.2.3. that the variation in yield strength had little effect on the measured J-Resistance curves, and that despite the variation measured, scatter between J-Resistance curves of a same geometry was not larger than that expected due to experimental scatter.

5.7.1.2. Interim conclusion:

The reduction in measured fracture toughness can be explained by one of two theories:

1) The transition from crack growth under an increasing load to a crack growth under a reducing load has lead to non-transferable behaviour at initiation.
2) A reduction in specimen size can lead to a transition from tensile ductile failure to shear ductile failure in low yield high toughness materials. The smaller specimens are likely to lie in the tensile/shear ductile failure transition conditions.
Chapter 6 Concluding remarks

This chapter summarises the contribution of this project to advance the understanding of failure conditions in components manufactured from ductile materials with low yield strength and high fracture toughness.

The following methodologies have been developed:

- A refined methodology to calculate the local approach criteria at ductile initiation from 3D FEA was devised. This methodology has been shown to be capable of providing more consistent failure criteria under conditions of large out of plane constraint loss.
- A refined method for deriving the Q parameter close to the crack-tip under conditions of widespread plasticity when the conventional normalised distance of $r\sigma_y/J$ is located well beyond the fracture process zone.
- A numerical methodology for defining both high constraint fracture toughness and the J-Q fracture locus from non-valid fracture toughness measurements. This has been shown to provide J-Q fracture locus which are in agreement with experimental results.
- A refined failure assessment methodology based on Local Approach parameters. This was shown to be consistent with conventional constraint corrected failure assessment methodologies, whilst requiring fewer calibrated parameters.

The main conclusions for this work are divided into two section. The first section will present the conclusions from the results obtained from the pipe material and the local approach based Failure Assessment Diagram. The second section will present the conclusions from the results obtained from the plate material.

The main conclusions from this work are as follows:

- Obtaining valid fracture toughness measurements of low yield high toughness materials require very large fracture toughness specimen, which often cannot be obtained from the geometry analysed. The resultant use of the validity limits to
define initiation toughness yield very conservative estimations of limiting conditions, which can lead to uneconomical requirements NDT analysis or replacement of the component.

- When constraint corrected failure assessment methodologies are used, care should be taken when defining the Q parameter in the specimen at fracture. Definition of Q outside the J-Q annulus will yield non-transferable, but conservative definition of the J-Q fracture locus. Furthermore, definition of Q outside the J-Q annulus will over-estimate the constraint loss in a specimen, with potential non-conservative estimation of failure loads.

- Adequately calibrated local approach methodologies provide a robust parameter to define initiation toughness, which is independent of loading conditions and the geometry of the structure analysed. The local approach should be calibrated from at least three experimental measurements of fracture toughness from at least three different constraint conditions. When constraint loss through thickness needs to be taken account of, an averaging methodology based on the 9 point average defined in BS -7442-4 for crack front profile measurements is recommended.

- Lower bound fracture toughness and J-Q fracture locus can be derived using the MBLM methodology defined in this work. Lower bound fracture toughness for 304(L) stainless steel has been defined numerically, with J_{0.2BL} = 485 kJm^{-2}. The J-Q constraint dependent fracture locus has been defined numerically for the 304(L) stainless steel investigated following the R6 formulation, with α=0.5 and k=1.25.

- The LA based Failure Assessment diagram methodology provides a novel methodology to predict the failure mechanism for a material failing in the upper shelf regime. It allows a constraint independent analysis, with graphical interpretation of the results, but requiring advanced analytical methodologies.

- In this work, a methodology has been defined to predict accurately the failure of a component under a range of conditions. An initial investigation of the failure mode of components with low yield high toughness material characteristics was performed, and it was shown that for a straight pipe manufactured from 304 stainless steel, containing a circumferential defect, loaded under tension and
New insights into the competition between ductile tearing and plastic collapse

internal pressure, plastic collapse is the prevalent failure conditions. The results obtained are consistent with large scale experimental results. This methodology can be used to validate the ASME BPVC Section XI IWB 3640 acceptable defect size criteria for stainless steel. This would require the analysis of a range of geometries and a range of material behaviours.

The reduction in measured fracture toughness observed with reduction in specimen size can be explained by one of two theories:

1) The transition from crack growth under an increasing load to a crack growth under a reducing load has lead to non-transferable behaviour at initiation.
2) A reduction in specimen size may lead to a transition from tensile ductile failure to shear ductile failure in low yield high toughness materials. The smaller specimens are likely to lie in the tensile/shear ductile failure transition conditions.
Chapter 7  Future Work

This research was aimed at addressing the issue associated with the definition of constraint dependent fracture toughness parameter under large scale yielding conditions. Based on the discussion in Chapter 5, a number of areas have been highlighted as having potential scope for further work.

7.1.  Experimental validation of the Local approach modified FAD

The local approaches used in this work were calibrated against three specimen geometry, sampling a range of constraint conditions. In order to validate this approach, the results obtained in this study should be complemented by a full scale test. Validation of the methodology can also be achieved through the analysis of existing test using the LA modified FAD.

Furthermore, additional validation of the local approach criteria should be done on sets of specimens with different thicknesses, in order to confirm that the Local Approach and Q parameters are sufficiently independent of the thickness of the test geometry to be transferable to a range of engineering applications.

7.2.  Derivation of the J-Q fracture locus for a range of material properties

The J-Q fracture locus has been defined using the modified MBLM approach defined in sections 4.7. for the 304(L) “pipe” stainless steel. This methodology can be used to derive the evolution of damage ahead of a defect as a function of J for a range of constraint condition and a range of material properties. The resulting damage curves can be fitted to exponential curves and provided in a database. This database could provide an engineering methodology to define the J-T and J-Q fracture locus, enable a more straightforward use of the constraint corrected FAD.
7.3. **Analysis of failure in a range of components and materials**

This study has provided a methodology to define valid failure criteria under large scale plasticity. Initial results suggest that failure in a 304(L) stainless steel pipe will be by plastic collapse prior to ductile initiation for a limited range of defects and loading conditions. Further analytical studies and experimental work will be required to demonstrate whether this observation is general for a wider range of defects and loading conditions.
New insights into the competition between ductile tearing and plastic collapse

References

New insights into the competition between ductile tearing and plastic collapse

New insights into the competition between ductile tearing and plastic collapse


New insights into the competition between ductile tearing and plastic collapse


New insights into the competition between ductile tearing and plastic collapse

New insights into the competition between ductile tearing and plastic collapse


Appendix 1-1

Figure 162: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_D specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse

Figure 163: J-Resistance curve for SEN(B)_D specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled 0.2mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse
New insights into the competition between ductile tearing and plastic collapse
New insights into the competition between ductile tearing and plastic collapse

Appendix 1-2

Figure 164: Load vs. crack mouth opening displacement (CMOD) for SEN(B)_S specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse

Figure 165: J-Resistance curve for SEN(B).S specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled 0.2 mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse
New insights into the competition between ductile tearing and plastic collapse.

\[ g \quad h \quad i \quad j \quad k \]
Appendix 1-3

Figure 166: Load vs. crack mouth opening displacement (CMOD) for SEN(T)_D specimen with DIC strain analysis points – tested according to P2-92 – displacement controlled 0.2mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse

Figure 167: J-Resistance curve for SEN(T) D specimen with DIC strain analysis points – tested according to P2-92 multi-specimen testing methodology – displacement controlled 0.2mm/min – room temperature
New insights into the competition between ductile tearing and plastic collapse
New insights into the competition between ductile tearing and plastic collapse
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