5 Pipe crack growth and stability behaviour under load controlled cyclic loading

5.1 Introduction

The crack growth assessment of a cracked pipe is done when it is subjected to cyclic loads. The crack size at the end of cyclic loading event is evaluated considering all participating crack growth mechanisms such as fatigue, corrosion etc. With this final crack size, the stability analysis is carried out to demonstrate required safety margins between the anticipated overload (as per design basis) and the critical load (which can trigger unstable tearing or plastic collapse failure). This methodology [38] is followed during fitness for service assessment of a cracked component when a crack is detected during an in-service inspection as well as in level-2 of LBB. In the level-2 LBB assessment, the number of cycles and cyclic loading is considered in the crack growth assessment due to fatigue. However, in level-3 LBB analysis, crack growth assessment of postulated through-wall crack (called as Leakage Size Crack LSC) is not performed. The stability of pipe, with LSC crack (a maximum credible size through-wall crack) is demonstrated under postulated design basis accident event loading which in several countries, as in India also, is Safe Shutdown Earthquake (SSE) event. Here the crack growth owing to cyclic nature of SSE load is neither evaluated nor accounted in assessment of critical load in any of the present LBB practices [1-19]. In reality, the cracked pipe is subjected to large magnitude reversible cyclic load which, in turn, causes significant cyclic tearing damage [46-61,69-74]. In the piping design the earthquake induced inertial loads are conservatively considered as load controlled. In IPIRG program, [13, 56-58], the behaviour of cracked piping under inertial loading was found closer to load controlled. The nuclear piping design considers 10 cycles of equivalent maximum stress per earthquake [20, 21], in assessment of margins against
fatigue and ratcheting modes of failure which are considered under cyclic loading.

In view of above, majority tests in current experimental programme (see Chapter 3) have been conducted under load controlled reversible cyclic loading. These tests have generated large data for investigation of the stability and crack growth behaviour under load controlled reversible cyclic loading. The test specimens covered wide range of parameters representative of piping used in NPPs. This chapter presents details of investigations carried out on load controlled cyclic tearing tests. These studies are carried out in relation with corresponding monotonic fracture.

*The work reported in this chapter is:*

(i) *Pipe fracture behaviour under load controlled cyclic loading:* Here the importance of the number of loading cycles (i.e. associated with an earthquake) in instability assessment; the moment-rotation (*M-Φ*) and the crack growth behaviour under load controlled cyclic loading is studied. The impact of load amplitude, load ratio and mean load on stability is also investigated.

(ii) *The crack growth evaluated using the methodology available in literature is compared to experimentally measured values.* Finite element study on CT specimen is carried out to understand the crack tip plasticity and applicability of envelope curve methods under reversible cyclic loading.

(iii) *Development a procedure for crack growth and instability assessment using a cyclic J and Dowling’s ΔJ-Integral based on each cycle loading branch rather than the envelope curve*
5.2 Pipe fracture behaviour under load controlled cyclic loading

Load controlled cyclic tearing test (see Table 3.7) have been conducted to understand the impact of number of loading cycles on crack growth and stability behaviour of a through wall cracked pipe. The tests in this category are conducted as per the loading scheme given in section 3.4.2. A monotonic fracture tests has been conducted (see Table 3.10) on identical pipe to general base line data corresponding to each of the cyclic tearing test. The results of both load controlled cyclic and monotonic fracture tests have been studied and following observations have been made:

![Figure 5.1](image.png)

Figure 5.1: Typical Test Results for Load Controlled Cyclic Tearing Test on 6” NB SS304LN pipe with through wall circumferential crack at weld centre (Narrow Groove hot wire GTAW) (a) Moment M & crack growth Δa vs. Cycles N, (b) Moment M & Total Rotation ϕ vs. Cycles N

Figure 5.1 shows typical test results for a load controlled cyclic tearing test on 6” NB SS304LN NGW welded pipe. This figure shows the plot of applied load, crack extension (obtained from average of growth at two crack tip fronts), CMOD and rotation response
versus number of cycles. The moment (M) versus CMOD and moment (M) versus rotation ($\phi$), response are shown in Figure 5.2 (a) and (b) respectively. Figure 5.3 shows similar test results obtained from two typical load controlled cyclic tearing tests on 12" NB SS 304LN NGW welded pipes.

Figure 5.2: (a) Moment Vs. CMOD and (b) Moment Vs. Total Rotation plots for Load Controlled Cyclic Tearing Test 6" QCSP-6-60-L2-NGW

Figure 5.1 (b) and Figure 5.3 (c and d) show that the maximum values of rotation / CMOD corresponding to maximum moment (when crack opens) remains nearly constant (or in some cases increase slightly) during initial cycles, while they increase rapidly in later cycles leading to instability. This shows that depending on the load amplitude, the crack growth in initial cycles is dominated by fatigue alone where ductile tearing contribution is insignificant. However, in the later cycles near to instability, in addition to fatigue crack growth, the ductile tearing also becomes significant and in fact ductile tearing-fatigue
synergy governs. This can further be observed from cycle by cycle hysteresis loop evolution of moment versus rotation and moment versus CMOD, as shown in Figure 5.2(a and b) and Figure 5.3 (a and b).

Figure 5.3: Tests results for Load controlled tests on 12” NB SS304LN NGW pipe with circumferential TWC at weld centre

Figure 5.4 shows typical fracture surface of a load controlled cyclic tearing test conducted on SS304LN NGW 12” NB pipe. The test pipe has stable cyclic crack growth up to 40 cycles and had unstable failure in 41st cycle when the crack size become large enough or the remaining ligament was not able to sustain the applied load. The sable crack growth, (crack growth, Δa, up to a cycle prior to instability) has been recorded in all the load controlled cyclic tearing tests. It can be clearly seen that there is significant amount of cycle by cycle stable ductile tearing before the instability cycle (Ni).
5.2.1 Stability and Crack growth behaviour

The number of cycle to instability (i.e. $N_t$), the stable crack growth (i.e. $\Delta a_t$) and the applied load magnitudes (M) have been studied in relation to those observed in corresponding monotonic fracture test on identical pipe.

Figure 5.5 plots a bar chart showing the magnitudes of applied moment, M, the number of cycles to instability, $N_t$ and the stable crack growth, $\Delta a_t$, for both carbon steel base (CSB)
and weld (CSW) category test on 8” size pipes. Figure 5.6 plots a bar chart showing the magnitudes of applied moment, $M$, the number of cycles to instability, $N_f$ and the stable crack growth, $\Delta a$, for cyclic tearing tests on both 12” and 6” size narrow gap welded stainless steel (SSW) pipe tests. Both these figures also plot corresponding monotonic fracture tests data. These figures clearly show:

(i) Under reversible cyclic loading, the unstable tearing leading to DEGB like failure of pipe, takes place in very few cycles (10-20) when the applied moment magnitude are 80 to 90% of the critical moment of the pipe obtained from monotonic fracture tests on identical pipe.

(ii) Significant stable crack growth ($\Delta a$) takes place before the unstable tearing. This crack growth increases with each loading cycle, which in turn reduces the size of healthy ligament (load bearing cross section). At certain stage, the remaining ligament becomes critical leading to unstable tearing failure.

(iii) The number of cycles, $N_f$, to unstable failure of pipe, increases with decrease in the applied moment magnitude.

(iv) The stable crack growth, $\Delta a$, increase with increase in $N_f$ which in turn depend on applied moment magnitude. Smaller the applied moment; large would be the stable crack growth, $\Delta a$, prior to unstable tearing takes place.
Figure 5.5. Comparison of 8” size carbon steel base (CSB) and weld (CSW) pipe tested under monotonic and load controlled cyclic tearing (load ratio=-1) conditions.
Figure 5.6. Comparison of 12” and 6” size stainless steel narrow gap welded (NGW) pipe tested under monotonic and load controlled cyclic tearing (load ratio=-1) conditions.
Figure 5.7 shows the crack growth behaviour of different tests on pipe having same material, size and crack geometry but different crack locations, namely in base metal, in centre line of SSW and centre line of NGW. Figure 5.7(a), clearly shows that under reversible cyclic loading the crack located in SSW undergoes larger tearing/ crack growth compared to when same crack is located in base metal. Likewise, Figure 5.7(b) compares the crack growth behaviour of cases in which crack is located in NGW and SSW. The figure clearly shows that NGW has superior resistance to cyclic crack growth in ductile tearing-fatigue synergic regime. In addition, the number of cycles to instability is higher in case of NGW welded pipes as compared to not only in SSW welded pipes but base metal also. This is highlighted by comparing the results of test nos. QSSP-6-60-L1-NGW, QSSP-6-60-L3-SSB, QCSP-6-60-L3-SSW, QCSP-6-60-L4-SSW and QCSP-6-60-L1-SSB shown in Table 3.7.

Figure 5.7: Crack growth vs. cycle’s plots for Stainless steel pipes having TWC crack in Base metal, SMAW and NGW centre line. (a) SSB vs. SSW, (b) SSW vs. NGW
Figure 5.8, shows the crack growth behaviour of different tests on carbon steel pipes having same material, size and crack geometry but different loading histories in term of amplitude and mean. These results show the influence of the load ratio R, mean load and compressive plasticity on the crack growth behaviour. The observations are explained in subsequent sub-sub-sections.

![Graph showing crack growth behaviour](image)

Figure 5.8: Comparison of crack growth behaviour of carbon steel pipe for same maximum load but different load ratio and for same amplitude but different load ratio

5.2.2 Effect of mean stress and stress/load ratio on cyclic fracture

Consider the two tests on carbon steel pipes (test nos. QCSP-8-60-L1-CSB and QCSP-8-60-L4-CSB, see Table 5.1), which have been conducted with same load amplitude but different load ratios. All other parameters such as crack size, pipe size, crack location and mode of loading are same in both the tests (see Table 5.1). In test no. QCSP-8-60-L1-CSB the load ratio is -0.5 (that is mean load is non-zero) whereas, in test no. QCSP-8-60-L4-
CSB the load ratio is -1.0 (that is zero mean load). The number of cycles to unstable failure was recorded as 45 (load ratio = -0.5) and 157 (load ratio = -1.0) respectively, see Figure 5.8. It shows that mean stress reduces the number of cycles to failure in comparison to zero mean stress but with same amplitude test. This is due to larger contribution of ductile tearing in crack growth owing to larger value of the maximum load (i.e. mean + amplitude; see Table 5.1) in case of non-zero mean load tests. The ductile tearing depends on the maximum load and not on the load amplitude.

Table 5.1: CSB and CSW Load controlled cyclic tearing tests on identical pipes but with different road ratio

<table>
<thead>
<tr>
<th>Parameter Name</th>
<th>QCSP-8-60-L1-CSB</th>
<th>QCSP-8-60-L4-CSB</th>
<th>QCSP-8-60-L3-CSW</th>
<th>QCSP-8-60-L3-CSW</th>
<th>QCSP-8-60-L2-CSW</th>
</tr>
</thead>
<tbody>
<tr>
<td>D0, mm</td>
<td>219.5</td>
<td>219</td>
<td>219</td>
<td>219</td>
<td>219</td>
</tr>
<tr>
<td>t, (mm)</td>
<td>15.5</td>
<td>15.66</td>
<td>16.09</td>
<td>15.4</td>
<td>16.08</td>
</tr>
<tr>
<td>θ (Degrees)</td>
<td>33.24</td>
<td>33.73</td>
<td>33.69</td>
<td>33.75</td>
<td>33.1</td>
</tr>
<tr>
<td>M_max, kNm</td>
<td>141.1</td>
<td>107.4</td>
<td>142.6</td>
<td>141</td>
<td>142.7</td>
</tr>
<tr>
<td>M_amp, kNm</td>
<td>105.8</td>
<td>107.4</td>
<td>107.0</td>
<td>141</td>
<td>142.7</td>
</tr>
<tr>
<td>M_mean, kNm</td>
<td>35.3</td>
<td>0</td>
<td>35.7</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Load Ratio, R=Mmin/Mmax</td>
<td>-0.5</td>
<td>-1</td>
<td>-0.5</td>
<td>-1</td>
<td>-1</td>
</tr>
<tr>
<td>Cycles to instability, Nf</td>
<td>44</td>
<td>157</td>
<td>88</td>
<td>5</td>
<td>12</td>
</tr>
<tr>
<td>Tests: Same M_amp, Different M_max</td>
<td>☓</td>
<td>☓</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tests: Same M_max, Different M_amp</td>
<td>☞</td>
<td>☞</td>
<td>☞</td>
<td>☞</td>
<td>☞</td>
</tr>
<tr>
<td>Base vs Weld: Same absolute Loading</td>
<td>♣</td>
<td>♣</td>
<td>♣</td>
<td>♣</td>
<td>♣</td>
</tr>
</tbody>
</table>

5.2.3 Effect of compressive plasticity and fatigue synergy

Consider the two typical tests on carbon steel pipes (test nos. QCSP-8-60-L1-CSB and QCSP-8-60-L3-CSB, see Table 5.1), which have been conducted with same maximum load but different load ratios. All other parameters such as crack size, pipe size, crack location
and mode of loading are same in both the tests (see Table 5.1). In test no. QCSP-8-60-L1-CSB the load ratio is -0.5 whereas, in QCSP-8-60-L3-CSB the load ratio is -1.0. The number of cycles to unstable failure is recorded as 45 (load ratio = -0.5) and 5 (load ratio = -1.0) respectively, see Figure 5.8.

Similar observation is made for carbon steel weld (CSW) category pipe tests (test nos. QCSP-8-60-L3-CSW and QCSP-8-60-L2-CSW, see Table 5.1). These are conducted with same maximum load but different load ratios that is -0.5 and -1.0 respectively. All other parameters such as crack size, pipe size, crack location and mode of loading are same in both the tests (see Table 5.1). The number of cycles to unstable failure was recorded as 88 (load ratio = -0.5) and 12 (load ratio = -1.0) respectively, see Table 5.1.

The significant reduction in number of cycles in the QCSP-8-60-L3-CSB (w.r.t. QCSP-8-60-L1-CSB) and QCSP-8-60-L2-CSW (w.r.t. QCSP-8-60-L3-CSW) tests are due to:

(a) Significant reversible/compressive plasticity because of larger compressive loads, leading to significant damage in fracture process zone ahead of crack. The compressive stresses /plasticity ahead of crack tip causes:

(i) Crack tip sharpening: After each compressive unloading, crack tip re-sharpening has been observed in these tests. The sharp crack tips are known to increase the crack-tip stress intensity and promote crack extension, thus lowering the apparent fracture resistance

(ii) Void flattening/crushing: In ductile fracture, the fracture process zone consists near spherical shape voids. These voids gets elongated and flattened or crushed under reverse loading (compressive load). The elongated flat and sharp voids tend
to enhance void coalescence and hence lower the apparent fracture toughness.

Both, the crack tip sharpening and the void flattening are important mechanism in the cyclic degradation process causing accelerated tearing in subsequent load cycle application. These apparently accelerate fracture process in subsequent tensile loading leading to additional crack growth. Similar observations have also been reported in IPIRG programme [13, 53] and by other investigators [73]

b) Increase in the load amplitude because of larger compressive loads, leading to increased fatigue crack growth (increased range of stress intensity factor, $\Delta K$, or range of $\Delta J$-integral). The contribution of fatigue crack growth is larger in the QCSP-8-60-L3-CSB w.r.t. QCSP-8-60-L1-CSB test and QCSP-8-60-L2-CSW w.r.t. QCSP-8-60-L3-CSW tests. The larger FCG would in turn further reduce the number of cycles to unstable failure.

Under large compressive loads, due to combined contribution of above both the reasons, i.e. the compressive plasticity induced damages and increased fatigue crack growth, the number of cycles to unstable failure reduce significantly.

5.3 Crack growth and stability assessment using the CRIEPI $J_{\text{max}}$ and $\Delta J$ integral method

In the past, fatigue crack growth in elastic plastic regions has been investigated by several researchers [69, 75-85]. In these studies, the Dowling’s [76] $\Delta J$-integral is used to model the fatigue crack growth rates in the elastic plastic region. These studies have shown good simulation of the crack growth behaviour over wide interval of crack growth for different sized cracks. C.W. Marchall and G. Wilkowski, [69] have reviewed several experimental
and analytical studies carried out to understand effect of cyclic loading on ductile fracture resistance. It is shown that the crack tearing under cyclic loading with load ratio, R, greater than 0, the total crack extension is just summation of crack extension due to monotonic ductile tearing, $\Delta a_{\text{mono}}$ (obtained from a monotonic J-R curve test) and fatigue crack growth, $\Delta a_{\text{cyc}}$ (estimated using fatigue crack growth analysis). While for negative R, the total crack extension exceeded above evaluated $\Delta a_{\text{mono}} + \Delta a_{\text{cyc}}$. The additional tearing/degradation (that is difference in measured tearing with that of estimated tearing) is attributed to damage owing to compressive loading.

NUREG/CR-6440 [53], Rahman [85], have presented basic experimental analysis approach for low cycle fatigue crack growth assessment under reversible cyclic loading conditions that is with negative load ratio (R<0). These have evaluated the low cycle fatigue crack growth of the cyclic pipe experiments conducted under simulated earthquake loading. Here The Dowling’s $\Delta J$-integral is used along with the extrapolated Paris law. Here the $i^{th}$ cycle $\Delta J$-integral is evaluated as discussed in section 5.3.2. Then, the equivalent $\Delta K_i$ is evaluated using following equation.

$$\Delta K_i = \sqrt{\Delta J_i E} \quad (5.1)$$

The crack growth (da) in the $i^{th}$ cycle then is evaluated using the effective $\Delta K_i$ and fatigue crack growth law (Paris Law).

$$\left( \frac{da}{dN} \right)_{i} = C (\Delta K_i)^m \quad (5.2)$$

Where, the constant C and m are generally determined from the standard fatigue crack growth tests.
During the same time as part of Japanese CRIEPI programme, Miura [62, 64-66] have proposed a cyclic $J_{\text{max}}$ and $\Delta J$ integral which was used in the crack growth and stability assessment of the load controlled cyclic tearing tests carried out in CRIEPI programme (similar to the load controlled cyclic tearing tests of current programme). Here the fatigue crack growth, $\Delta a_{\text{cyc}}$, is evaluated using the $\Delta J$-integral as described above. The static ductile tearing is evaluated using monotonic $J$-$R$ curve and a cyclic $J_{\text{max}}$ integral. It is shown that the procedure has reasonably simulated the CRIEPI programme load controlled tests carried out on 4" size STS410 carbon steel pipes.

In view of above, the crack growth and stability assessment of load controlled cyclic tearing tests have been performed using the above CRIEPI/Miura [62, 64-66] procedure. Here cyclic maximum $J$-integral, $J_{\text{max}}$ and cyclic $J$-integral range, $\Delta J$, were used for describing the crack growth behaviour in a large scale yielding region. The $J_{\text{max}}$ and the $\Delta J$ integrals were evaluated using experimentally measured load displacement and are discussed in following sections:

### 5.3.1 Evaluation of $J_{\text{max}}$ –Integral

The cyclic $J_{\text{max}}$-integral, at the $i^{\text{th}}$ cycle has been evaluated as sum of the elastic and plastic $J$ integral using following equations as given by Miura et. al. [62, 64-66]

\[
J_{\text{max},i} = J_{\text{max,el},i} + J_{\text{max,pl},i}
\]

\[
J_{\text{max,el},i} = f_b^2 \left( \frac{\theta_i}{\pi} \right) \left( \frac{M_i^2}{E R^3 t^2} \right)
\]

\[
J_{\text{max,pl},i} = 1 + \frac{\Delta a}{R} \gamma(\theta_i) J_{\text{max,pl},i-1} + \frac{2\eta(\theta_i)}{1 - \frac{\Delta a}{R} \gamma(\theta_i)} U_i
\]
\[ U_i = \int_{\phi_{pl,i-1}}^{\phi_{pl,i}} M \, d\phi_{pl} \]  \hspace{1cm} (5.6)

The \( U_i \) is the area under the envelop moment (\( M \)) versus crack plastic rotation (\( \phi_{pl} \)) evaluated between two subsequent cycle peaks. The \( \eta(\theta_i) \) and \( \gamma(\theta_i) \) are evaluated using Eqs.(4.5 and 4.6). Figure 5.9(a) shows the schematic of \( U_i \) and positive half of envelope \( M-\phi_{pl} \) curve. Here the function \( f_b, \eta \) and \( \gamma \) are geometry functions [32].

![Figure 5.9: Schematic of \( U_i \) and \( \Delta U_i \) Calculations from the envelope \( M-\phi_{pl} \) curves](image)

### 5.3.2 Evaluation of \( \Delta J \)-Integral

The Cyclic \( \Delta J \)-integral has been evaluated as sum of the elastic and plastic \( \Delta J \) integral using following equations as given by Miura et. al. [62, 64-66]

\[ \Delta J_i = \Delta J_{el,i} + \Delta J_{pl,i} \]  \hspace{1cm} (5.7)

\[ \Delta J_{el,i} = f_b^2 \left( \frac{\theta_i}{\pi} \right) \left( \frac{\Delta M_i^2}{ER^3t^2} \right) \]  \hspace{1cm} (5.8)
\[ \Delta J_{pl,i} = \frac{2\eta(\theta_i)}{1 - \frac{A_p}{A_v}(\theta_i)} \Delta U_i \] 

(5.9)

\( \Delta U_i \) is the total area under the M-\( \phi_{pl} \) envelope curve corresponding to \( i^{th} \) cycle. Figure 5.9(b) shows the schematic of \( \Delta U_i \) and envelope M-\( \phi_{pl} \) curve.

### 5.3.3 Instability Criteria

Here the cyclic \( \Delta J \) integral along with monotonic J-R curve was used to determine the instability using equation as given below.

\[ \Delta J(\Delta a) \geq J_R(a - a_o) \] 

(5.10)

Where ‘\( a \)’ is the current crack length and ‘\( a_o \)’ is the initial crack length. For analytical cyclic tearing assessment of a cracked pipe, it was proposed to evaluate a \( J_{\text{max}} \) integral using standard analytical schemes such as GE/EPRI method etc. [32]. The \( J_{\text{max}} \) integral is the applied J-integral value of the cracked pipe evaluated corresponding to the maximum cyclic load. Then the \( \Delta J \) integral is evaluated from this \( J_{\text{max}} \) using following equation

\[ \frac{\Delta J}{J_{\text{max}}} = \frac{4a}{2a - a_o} \] 

(5.11)

The crack extension in \( i^{th} \) cycle, (\( da_i \)), is calculated using the \( \Delta J_i \) using and Eq. (5.1 and 5.2). Then the crack size after \( i^{th} \) cycle is evaluated by adding the (\( da/d/N \subtract), to the crack size in previous cycle that is \( a_{i-1} \).

### 5.3.4 Crack growth and stability assessment of Carbon Steel Base Metal pipes

In order to assesses the suitability of the above CRIEPI with respect to current programme pipe tests, the data of five numbers of load controlled cyclic testing tests on carbon steel
base metal (CSB) 8" size pipe have been analysed. The carbon steel pipe tests are selected since it is similar to the material, STS410, used in CRIEPI programme. The envelope curve of the load-displacement history obtained from a load controlled cyclic tearing test has been used to evaluate the $J_{\text{max}}$ and the $\Delta J$ as described above. The constants ‘C ‘and ‘m’ used in present study, were obtained in a different test programme, Singh et. al [111], on SA333 Gr.6 carbon steel. The C and m as evaluated are $3.982 \times 10^{-12}$ and 3.01 respectively. In above equation, unit of $\frac{da}{dN}$ is m/cycle and $\Delta K$ is in MPa$\sqrt{m}$. These constants have been obtained as per the ASTM Standard E647 using three-point bend specimen machined from the pipe material stock of same heat as used in present program. These tests have been carried out for stress ratios of 0.1, 0.3 and 0.5. The stress ratios have negligible effect in the Paris region. The results are as given below:

(i) Figure 5.10 plots the $\Delta J$ integral versus $\Delta a$ (evaluated from test data of load-displacement and crack size). These figures also plot the monotonic J-R curve evaluated form monotonic fracture test on identical pipe having same nominal size, thickness, crack size, material and heat of material. The instability point observed in these tests is marked by a circumscribing circle. The instability point and the monotonic J-R curve ($J_R$ versus $\Delta a$) is also plotted in Figure 5.10. This figure clearly shows that the above instability criteria, Eqn.(5.10) does not hold good for these CSB tests. Figure 5.11 plots the $J_{\text{max}}$ integral versus $\Delta a$. This figure shows that in all the five tests, the $J_{\text{max}}$ in the cyclic tests is below the corresponding monotonic J-integral value till instability.
(ii) To investigate Eqn.(5.11) which is an important relation provided for analytical assessment of cracked pipe under cyclic loading, the parameters $\Delta J/J_{max}$ has been evaluated for all the five tests and plotted against ‘$a/a_o$’ in Figure 5.12. For tests no. L2 to L5 the initial crack size, $a_o$, was nearly 67° while for L6 test it was 97°. The $\Delta J/J_{max}$ as evaluated using Eq. (5.11) for these two initial $a_o$ values is also plotted in Figure 5.12. This figure clearly shows that the $\Delta J/J_{max}$ ratio calculated from Eqn.(5.11) are not in agreement with those obtained from the test. However, the ratio $\Delta J/J_{max}$ has remained between 2 and 4 for all the tests similar to that obtained in CRIEPI programme. This clearly indicates the dependence of the ratio $\Delta J/J_{max}$ on the loading parameters in addition to $a/a_o$, as proposed by Miura [62, 64-66]
Figure 5.11: The $J_{\text{max}}$ (load control cyclic tearing test) and $J$-integral (monotonic test) versus crack extension plot for carbon steel base metal 8" size pipe load controlled cyclic tearing tests.

Figure 5.12: Analysis of load controlled carbon steel cyclic tearing tests on 8" pipe; (b) Cyclic $\Delta J / J_{\text{max}}$ versus crack extension $\Delta a$.
Figure 5.13: Predicted crack growth using CRIEPI method versus test measured crack extensions

Figure 5.14: Predicted crack growth using CRIEPI method versus test measured crack extensions
(iii) The evaluated crack growth versus number of cycles has been plotted in Figure 5.13 and Figure 5.14 for all the four number of load controlled cyclic tearing tests on identical carbon steel base metal 8" size pipes. The Figure 5.13 show that the reasonable predictions, while in Figure 5.14 the predicted crack growth was found much higher than that measured during test. Hence the above method over predicts the crack growth.

From the above evaluations and comparisons with current program tests data, it has been seen that the envelope curve based method (Miura et. al. [62, 64-66]) of assessment of cracked pipe under reversible cyclic loading the cyclic tearing test, is not universally applicable to other materials and pipe sizes. This may be due to following reasons:

(a) The procedure was developed and validated on pipe tests conducted on 4 inch size and STS410 steel. In these small size ductile material pipe tests the plastic collapse is likely to govern over ductile tearing,

(b) The procedure had used envelope curves in evaluation of the \( J_{\text{max}} \) and \( \Delta J \) integral. The use of envelop curve may not be realistic when the loading is of reversing cyclic nature (negative load ratio, \( R<0 \)). The use of envelope curve is well established and accepted for alternating loading cases for positive load ratio \( R>0 \), (for example standard monotonic \( J-R \) curve tests on specimens [44] or components). However for negative load ratio, no validation / justification of envelope curve use is available in literature.

5.4 Use of Envelope curve: alternating versus reversing load

In order to assess and understand the uses of envelope curve under alternating and fully reversing loading, a study has been carried out involving the data of above analysed tests and a series of 2D finite element analysis on standard CT specimen geometry.
From study of the cycle by cycle hysteresis loops along with the corresponding envelop curves obtained from the test data used in above analyses, following observations are made:

(a) The loading branch of the $M-\phi$ hysteresis loop shift/ratchet towards right (in positive $\phi$ direction) on subsequent application of reversible cyclic loading. This can be clearly seen in Figure 5.15 and Figure 5.16.

(b) The envelope $M-\phi$ curve does not account for the $M-\phi_{pl}$ loading branch shift/ratchet towards right (in positive $\phi_{pl}$ direction) on repeated reversible cyclic loading. This can be clearly seen in Figure 5.16 where each cycle loading branch of $M-\phi_{pl}$ hysteresis and envelope $M-\phi_{pl}$ curve are plotted for a load controlled cyclic tearing test namely QCSP-8-60-L3-CSB. Similar observations were there for other tests also. The reason for such behaviour is that, under fully reversible loading, the pipe plastically deforms more in crack opening direction loading (positive $\phi_{pl}$ direction) than that in crack closing direction. This is due to asymmetry in pipe stiffness owing to crack closing/opening. Due to this asymmetric plastic deformation of pipe, and the loading $M-\phi_{pl}$ curve shifted in positive $\phi_{pl}$ direction with increase in number of load cycles, as can be seen in Figure 5.16. It becomes clear that the use of envelope curve in $\Delta J_{pl}$ calculation (as suggested in CRIEPI program) would use a larger area for each cycle loading expect 1$^{st}$ cycle. This will result in over estimation of $\Delta J_{pl}$. Hence the $\Delta J$ evaluated using envelope curve, after 1$^{st}$ cycle is larger than the actual $\Delta J$ if evaluated from that cycle loading branch. This leads to over estimation of the crack growth (see Figure 5.14).

In order to further understand the suitability of envelope curve usage and the plasticity
ahead of crack tip under alternating and fully reversing loading, elastic plastic finite element analyses on a standard size CT specimen has been carried out. Figure 5.17, plots schematic of standard CT specimen, the FE mesh, loading and boundary conditions and gap-contact elements as used in the analyses. Full domain of CT specimen has been modelled using 8 noded 2D plane strain elements. Gap-contact element has been used to model the mating crack surfaces (Figure 5.17). Both contactor and contacting surface are taken as deformable. These contact elements would simulate the crack closure. The stress strain curve of the SA333Gr6 material (Figure 3.4a), with multi-linear kinematic hardening rule has been used to model the material. The applied load (in y-direction) is distributed over 7 nodes on one end while the y-degree of freedom of the 7 symmetric nodes on the other side, have been fixed. All these 14 nodes are in a line. The x-direction is fixed at two nodes as shown in Figure 5.17. The analyses have been carried out for two loading conditions, one alternating cyclic load (load ratio, R = 0) and the other fully reversing cyclic load (load ratio, R = -1). Figure 5.18 plots the load versus pseudo time history for these two cases. The maximum loads in the two cases are kept identical.

Figure 5.15: Load versus rotation for different loading cycles branch of envelop curve
Figure 5.16: Load versus plastic load line displacement plot of different cycles loading branch and of envelop loading curve of QCSP-8-60-L3-CSB test used in cyclic $\Delta J$ calculation

Figure 5.17: Schematic of CT specimen and the 2D finite element mesh along with gap-contact elements used for cyclic loading analyses
Figure 5.18: Load versus pseudo time history used in analyses (a) case-1 with load ratio R=0, (b) case-2 with load ratio R=-1

Figure 5.19: Load versus Load Line Displacement (a) for case-1 loading (Load ratio R = 0); (b) for case-2 loading (Load ratio R=-1)

Figure 5.20: Plastic strain ahead of CT specimen crack tip at maximum load point under alternating and fully reversible cyclic loading
Figure 5.19 plots the load versus load line displacement response obtained respectively from the alternating and reversing cyclic loading schemes. It shows that the 1st load application (that is point-1), the unloading and repeat applications of load (point 1a, 1b and 1c); the load and LLD point is nearly same. However, in case of fully reversing loading, see Figure 5.19(b), the effect of crack closure and the shift in the loading branch can clearly be seen. Figure 5.20 plots the plastic strain variations ahead of CT specimen crack tip at different loading stages in both the loading cases. Following observation/conclusions are made from this:

(i) In case of the alternating cyclic loads, the plastic zone ahead of the crack tip remained nearly same on reloading to maximum load (see plot for point 1, 1a and 1b in Figure 5.20). Here on reloading the specimen to its previous maximum load level, the von-Mises plastic strains ahead of the crack in the specimen is nearly same as that before unloading. Hence, for $R \geq 0$, the envelope curve may be used to evaluate the J-integral and static tearing (as is done for monotonic fracture tests with partial unloading). Only precaution required is, if there are large numbers of unloading cycles then it is necessary to account for the fatigue crack growth.

(ii) For fully reverse loading, the CT specimen analyses has shown (see Figure 5.20) that the plasticity zone ahead of the crack tip is much larger than that before unloading. This shows that for $R = -1$ loading, the crack tip plasticity depends on each cycle of loading. Chang-Sung Seok et al. [71] has shown that for fully reversible loading i.e. $R = -1$, at the minimum load, the crack tip positive plastic strains due to previous tensile load vanishes, and the compressive plastic strains are generated at tip. On reloading to maximum load in tensile direction generates bigger
plastic zone (tensile) ahead of the crack tip. Hence the crack growth assessment procedures which are based on M-\(\phi\) envelope curve may not properly account for the elastic plastic unloading effects on crack tearing/damage on subsequent loading.

In view of above tests data and FE analyses of the CT specimen, the use of envelope curve for fracture assessment under fully reversible cyclic loading, is found unacceptable due to following reasons:

(a) The rightward shift/ratchet (in positive \(\phi\) direction) of the loading branch of the M-\(\phi\) hysteresis loop under repeated fully reversible cyclic loading is not accounted in the envelope M-\(\phi\) curve approach.

(b) In case of fully reversible cyclic loading, the finite element study has shown that the crack tip plasticity and plastic zone under repeat application of load is much larger than that before unloading. This indicates increase in damage owing to reverse direction loading/unloading, on reloading up to the maximum load point and which may not be accounted in the envelope approach.

5.5 Development of a crack growth assessment procedure based on each cycle loading

The crack growth assessment of CSB pipe tests of current programme using envelope curve based procedure (section-5.3) are not in good agreement with measured values. The discussion in 5.4 has clearly shown that the use of envelope curve for simulating the fully reversible load tests is not justifiable and has no basis. In view of this, a procedure where crack growth evaluation in each cycle is based on its loading branch has been used. Here the crack growth is evaluated due to both the fatigue and the static tearing. The total crack
extension in let us say $i^{th}$ cycle can be given by the following equation.

$$
\left( \frac{da}{dN} \right)_i = \left( \frac{da}{dN} \right)_{i,\text{fatigue}} + \left( \frac{da}{dN} \right)_{i,\text{tearing}}
$$

(5.12)

The fatigue crack growth, is evaluated using the widely accepted cyclic Dowling’s cyclic J-integral range ($\Delta J$), while the static tearing crack growths is evaluated using a cyclic $J'$-integral described in section 5.5.2. Both the $\Delta J$ and $J'$ integrals are evaluated for each loading cycle in place of envelop curve. The each loading cycle based calculations is used due to following reasons:

(a) In previous section, it is shown that the M-$\phi$ envelope curve based assessment leads to overestimation of $\Delta J$ and hence over predicts the crack growth. Hence for fatigue crack growth assessment, the $\Delta J$ is evaluated for each loading cycle as given in section 5.5.1

(b) The static tearing crack growth is based on J-integral which, in turn, depends on the plastic-strain field ahead of crack tip. The finite element studies on CT specimen under reversible cyclic loading, (see section 5.4) has shown that, for fully reversing load (load ratio = -1, as for the tests in current program) the crack tip plasticity and plastic zone, is much larger than that before unloading. This shows that for $R = -1$ loading, the crack tip plasticity is strong function of unloading and depends on each cycle loading cycle. Hence the static tearing crack growth is calculated for each cycle of loading, using a new proposed $J'$-integral as described in section 5.5.2
5.5.1 Crack growth due to fatigue using $\Delta J$ integral

The fatigue crack growth calculations have used the $\Delta J_i$ integral evaluated from each cycle loading as described below:

$$\Delta J_t = \Delta J_{i,el} + \Delta J_{i,pl}$$  \hfill (5.13)

Here the $\Delta J_{i,el}$ and $\Delta J_{i,pl}$ are elastic and plastic part of the total $\Delta J_i$ integral. The elastic part $\Delta J_{i,el}$ is evaluated as given below:

$$\Delta J_{i,el} = f_b^2 \left( \frac{\theta}{\pi} \right) \left( \frac{\Delta M_i^2}{ER^3 l^2} \right)$$  \hfill (5.14)

The $f_b$ is function of pipe and crack geometry and is evaluated using equations given by Zahoor [32]. $\Delta M_i$ is the moment range that is the difference of $M_i^{max}$ and $M_i^{min}$. For the $i^{th}$ cycle, $\Delta J_{i,pl}$-integral is calculated using the loading branch of $i^{th}$ cyclic hysteresis $M$-$\phi_{pl}$ loop, i.e. from minimum load point ‘A’ to the maximum load point ‘C’ of loading branch (see Figure 5.21).

$$\Delta J_{i,pl} = \int_{\phi_{pl,i}^{min}}^{\phi_{pl,i}^{max}} \eta \Delta M d\phi_{pl} + \int_{\theta_{i-1}}^{\theta_i} \gamma \Delta J_{pl} d\theta$$  \hfill (5.15)

Where the $\phi_{pl,i}^{min}$ and $\phi_{pl,i}^{max}$ are the crack plastic rotations corresponding to starting and end point of the loading branch of $i^{th}$ cycle hysteresis loop. The Eqn.(5.15) of $\Delta J_{i,pl}$ is a non-linear equation and cannot be solved explicitly because of the second term. Within a cycle, the continuous variation of the half crack angle ‘$\theta$’ with ‘$\phi_{pl}$’, is unknown. However, the crack growth per tip in each cycle is known. If the crack growth within a cycle is small enough, then the equation for $\Delta J_{pl}$ can be simplified as given below:
\[
\Delta J_{i,pl} = \int_{\phi_{pl,i}^{\text{min}}}^{\phi_{pl,i}^{\text{max}}} \eta \Delta M d\phi + \Delta J_{i,pl} \int_{\theta_{i-1}}^{\theta_i} \gamma d\theta
\]

\[
\Delta J_{i,pl} = \eta(\theta_i) \times \Delta U_i + \Delta J_{i,pl} \times \gamma(\theta_i) \times (\theta_i - \theta_{i-1})
\]

\[
\Delta J_{i,pl} = \frac{\eta(\theta_i) \times \Delta U_i}{1 - \gamma(\theta_i) \times (\theta_i - \theta_{i-1})} \quad (5.16)
\]

Where the \(\eta(\theta_i)\) and \(\gamma(\theta_i)\) is evaluated using Eq.(4.5 and 4.6) and the \(\Delta U_i\) is the area under the loading branch for the \(i^{th}\) cycle of the \(M - \Phi_{pl}\) record.

![Diagram of moment versus plastic crack rotation hysteresis showing points A, B and C in loading path of \(i^{th}\) cycle](image)

Figure 5.21: A typical moment versus plastic crack rotation hysteresis showing points A, B and C in loading path of \(i^{th}\) cycle

In Figure 5.21, \(\Delta U_i\) is the area ABCDEA under the curve ABC from minimum load point ‘A’ to the maximum load point ‘C’. It is evaluated using equation given below:

\[
\Delta U_i = \int_{\phi_{pl,i}^{\text{min}}}^{\phi_{pl,i}^{\text{max}}} \Delta M d\phi_{pl} \quad (5.17)
\]
\[ \Delta M = M - M_{i}^{min} \] (5.18)

The M is the moment at a point in loading branch. The total \( \Delta J_i \) integral is evaluated using Eqn. (5.13 to 5.18). Then using this \( \Delta J_i \) integral, the fatigue crack extension, \( (da/dN)_{i, \text{fatigue}} \) is evaluated using following equation which has been obtained from the Eqn. (5.1) and Eqn. (5.2) of section 5.3.

\[ \left( \frac{da}{dN} \right)_{i, \text{fatigue}} = C(\sqrt{\Delta J_i E})^{m} \] (5.19)

5.5.2 Crack growth due ductile tearing using a cyclic \( J' \) integral

A new cyclic \( J' \)-integral has been used which is calculated cycle-by-cycle from the positive half of the Moment – rotation \( \phi_{pl} \) hysteresis. For \( i^{th} \) the cycle \( J'_i \) is evaluated as given below

\[ J'_i = J'_{el,i} + J'_{pl,i} \] (5.20)

\[ J'_{el,i} = f_b^2 \left( \frac{\theta_i}{\pi} \right) \left( \frac{M_i^2}{ER^3 \varepsilon} \right) \] (5.21)

The \( J'_{i,pl} \) integral is calculated using \( \eta \) and \( \gamma \) factors from the positive part of loading branch that is from zero load point ‘B’ to the maximum load point ‘C’ of \( i^{th} \) cycle M-\( \phi_{pl} \) hysteresis (see Figure 5.21).

\[ J'_{i,pl} = \int_{\phi_{pl,i}^{M=0}}^{\phi_{pl,i}^{max}} \eta M d\phi_{pl} + \int_{\theta_{i-1}}^{\theta_i} \gamma J'_{pl} d\theta \] (5.22)

Where the \( \phi_{pl,i}^{M=0} \) and \( \phi_{pl,i}^{max} \) are the crack plastic rotations corresponding to zero moment and end point (that is maximum moment) of the loading branch of \( i^{th} \) cycle hysteresis loop.
The Eqn.(5.22) of \( J_{i,pl} \) is a non-linear equation and cannot be solved explicitly because of
the second term. However, similar to the section 5.5.1, this equation can be simplified to
as given below:

\[
J_{i,pl}' = \frac{\eta(\theta_i) \times U_i}{1 - \gamma(\theta_i) \times (\theta_i - \theta_{i-1})}
\]  
(5.23)

Where the \( \eta(0_i) \) and \( \gamma(0_i) \) are evaluated using Eqns.(4.5 and 4.6) and the \( U_i \) is the area under
the positive half of the loading branch for the \( i^{th} \) cycle of the \( M - \Phi_{pl} \) record of loading
branch. The area under the positive part of loading curve, that is BC (see Figure 5.21) over
the zero load line, is designated as energy \( U_i \). It is evaluated using equation given below:

\[
U_i = \int_{\Phi_{pl,i}}^{\Phi_{pl,\text{max}}} M \, d\Phi_{pl}
\]  
(5.24)

The \( M \) is the moment at a point in loading branch. In Figure 5.21, it may be observed that
the area ‘BCDB’ is more than the area ‘OCDO’ which one would have used in envelope
curve based \( J_{\text{max}} \) integral calculation. The additional area is coming due to reversible
loading which seems to account for the damage due to compressive loading.

The total \( J_i' \) integral is evaluated using Eqns. (5.20 to 5.24). Then the crack extension owing
to ductile tearing in \( i^{th} \) cycle, has been evaluated using the \( J_i' \) integral along with the
monotonic fracture resistance i.e. \( J-\Delta a \) curve. Here if the \( J_i' \) is less than the \( J \)-initiation then
there will not be any growth due to ductile tearing. However the crack will grow only due
to fatigue. The monotonic \( J-R \) curve equation used is as given below:

\[
J_R' = 230 + 1000 \, (\Delta a)^{0.51}
\]  
(5.25)
Using Eqn. (5.12, 5.19, and 5.25), the total crack growth in \( i \)th cycle may be written as

\[
\left( \frac{da}{dN} \right)_i = C \left( \sqrt{\Delta J_i E} \right)^m + \left( \frac{J'_{230}}{1000} \right)^{1/0.51} \tag{5.26}
\]

It may be noted here that the second term on right hand side of above equation assumes that tearing crack growth start from the initiation toughness each cycle. Then the total crack size at end of \( i \)th cycle will be given as

\[
a_i = a_{i-1} + \left( \frac{da}{dN} \right)_i \tag{5.27}
\]

The crack growth assessment of all the four number of carbon steel load controlled cyclic tearing tests have been carried out using the above procedure and equations, Eqn.(5.12 through 5.27). The crack growth versus number of cycles as predicted using above procedure and as measured from the experiments have been plotted in Figure 5.22 and Figure 5.23. These figures show that the crack growth predicted using above proposed equation and those measured during tests are in good agreement. This shows that the proposed analyses procedure which considers the M-\( \phi \) loading curve of each cycle (not based on envelope curve) and also accounts for the crack growth due to both fatigue (under large scale yielding) and ductile tearing mode is reasonable and may be used for assessment cyclic tearing. However it is still not useful for analytical assessment since it required assessment of \( J' \) and \( \Delta J \) integrals. This requires generation of the cycle by cycle M-\( \phi \) hysteresis curves by carrying out non-linear finite element analysis with simulation of crack extension (as evaluated by Eqns.(5.26 and 5.27) after each cycle.
Figure 5.22: Predicted crack growth using proposed method versus test measured crack extensions

Figure 5.23: Predicted crack growth using proposed method versus test measured crack extensions
5.6 Instability Criteria for load controlled Tests

For load controlled cyclic tearing tests, peak load versus measured crack size were investigated along with monotonic load versus measured crack size. Figure 5.24 plots result from 5 load controlled cyclic tearing tests and 3 monotonic fracture tests on same material and size of pipes. A monotonic failure curve is plotted by joining the maximum load point of the 3 monotonic fracture tests which were conducted by Chattopadhayay et al [107], on the identical pipes except the initial crack size. This figure clearly shows that there is large crack growth in cyclic tests before the instability point. However in monotonic tests, there is very small crack growth up to the maximum moment (instability load). Further it also shows that the instability point of cyclic tearing tests lie close to the monotonic failure line. Hence, instability occurs when the current crack size ‘a_i’ in cyclic tests reaches a critical size ‘a_c’ for the given loading amplitude and evaluated using monotonic instability assessment. In term of load it also can be written as below

\[ M_{\text{max, cyclic load}} \geq M_{\text{Monotonic capacity}}(a_i) \]  

(5.28)

Here the a_i is the crack size in i^{th} cycle of loading and may be evaluated using Eq. (5.26 and 5.27). It includes the crack growth due to fatigue and ductile fracture as well as their synergistic damage.

The step by step procedure of assessment of cyclic tearing stability is explained in flow chart as given in Figure 5.25. Here the M_{max, cyclic load} is the magnitude of reversible bending moment applied to cracked pipe. a_i is the crack size at the end of i^{th} cycle, for 1^{st} cycle calculation the initial crack size, a_0, and the M_{monotonic capacity} of the crack pipe evaluated using standards and established methods.
Figure 5.24: Loading versus Crack Extension from Cyclic Tearing and Monotonic Fracture Tests

Crack size $a_0$; Bending Moment $M_{\text{max, cyclic load}}$; $i=0$

Loading cycle number, $n=i+1$

Monotonic capacity of pipe $M_{\text{monotonic capacity}}(a_i)$

It is evaluated as minimum of (Fracture Instability, Plastic Collapse Load) using any of the standard procedures such as those given in ASME-XI, French A-16 Guide, R-6 Method etc. as described in section 2.3.4

$M_{\text{max, cyclic load}} < M_{\text{monotonic}}$

Yes, $i=i+1$

$\frac{d}{da}a_i = a_{i-1} + \left(\frac{d}{da}\right)_{\text{fatigue}} + \left(\frac{d}{da}\right)_{\text{tearing}}$

Fatigue and tearing crack growth in $i$th cycle is evaluated as described in section 5.5

No,

Number of Cycles to Instability = $n$

Crack size prior to instability cycle = $a_i$

Figure 5.25: Flow chart showing the stability assessment under reversible cyclic loading conditions
5.7 Chapter conclusions

The investigations carried out to study pipe fracture behaviour under load controlled cyclic loading have revealed the following:

(a) Pipe fracture tests showed that under load controlled reversible cyclic loading, the unstable tearing leads to DEGB like failure of pipe in very few cycles (10-20) when the applied moment magnitude are 80 to 90% of the critical moment of the pipe obtained from monotonic fracture tests on identical pipe.

(b) Significant crack growth by fatigue and stable tearing precedes the instability. The crack growth increases with number of cycles which, in turn, reduces the size of load bearing ligament (cross section). After certain number of cycles, the load bearing section becomes critical resulting in unstable tearing failure.

(c) The number of cycle to unstable failure of pipe, \( N_f \), and the stable crack growth, \( \Delta a_f \) increase with decrease in the amplitude of moment.

(d) Under identical loading conditions on identical pipe, the crack located in SSW undergoes larger tearing/ crack growth compared to when same crack is located in base metal or in NGW weld. The number of cycles to instability is found higher in case of NGW welded pipes as compared to SSW welded pipes.

(e) The number of cycles to unstable failure of pipe depends on both the maximum moment and the load ratio. Significant reduction in number of cycles is observed in the tests for same maximum moment but with larger compressive load in reverse direction (larger negative load ratio). This is due to significant reversible/compressive plasticity that leads to significant damage ahead of crack. The compressive stress ahead of crack are known to
re-sharpen the blunted crack tip, flatten the rounded voids and hence accelerates the process of void coalescence, thus leading to accelerated the tearing in subsequent load cycles. These apparently accelerate the fracture process in subsequent tensile portion of the loading cycle leading to additional crack growth.

(f) The analysis of load controlled cyclic tearing tests on carbon steel pipes using $J_{\text{max}}$ and $\Delta J$ integral evaluated from load-displacement envelope curve, proposed by Miura/CRIEPI program has shown that:

(i) The $\Delta J/J_{\text{max}}$ ratio, in addition to $a/a_0$ as proposed in CRIEPI program, is found to depend on loading history also.

(ii) The $\Delta J(\Delta a) \geq J_R(\Delta a)$ criterion is unable to predict instability in the tests.

(iii) The use of envelop curve based $\Delta J$-integral overestimated the crack growth.

(g) The correctness of uses of envelope curve under alternating (load ratio >0) and fully reversing loading (load ratio = -1) is investigated using the test data and a nonlinear 2D finite element analysis on standard CT specimen geometry. The study has revealed that:

(i) In case of repetitive alternating cyclic loading (load ratio $\geq$0), the plastic strain field ahead of the crack tip remained nearly same on reloading to the maximum load. Hence envelope curve is justifiably used to evaluate the $J$-integral.

(ii) For reversed loading, (load ratio = -1) the plastic zone ahead of the crack tip on reloading to maximum load, is much larger than that before unloading. This indicates increased damage owing to unloading followed by reverse direction loading. The envelope approach doesn’t account for this damage.
(iii) Further from test data (for load ratio = -1) studies it has been observed that there is ratchet/shift of zero load displacement (loading branch of load displacement hysteresis) in crack opening direction. The envelop curve procedure does not account for this and so leads to overestimation of $\Delta J$ and crack growth.

(h) A crack growth assessment procedure under reversible cyclic loading is proposed where contributions of both fatigue and static ductile tearing are considered. The cycle by cycle crack growth, evaluated using this procedure is found to be in good agreement with the test results.

(i) An instability criteria and an algorithm is developed to evaluate number of loading cycles to unstable failure of a cracked pipe when it is subjected to fully reversible load controlled cyclic load.