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FAILURE OF CRACKED COMPONENTS
SINTAP TASK 2 REVIEW

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SUMMARY

SINTAP (Structural Integrity Assessment Procedures for European Industry) is a Brite-Euram Project co-ordinated by British Steel with the objective of providing a unified structural integrity evaluation method for European industry. The project is divided into 5 tasks dealing with: weld metal strength mismatch; failure of cracked components; optimised treatment of data; secondary stresses; and procedure development. The first activity is a state-of-the-art review and this report contains that review for task 2 - failure of cracked components. This task covers a number of technical issues and for each of these the available information is cited along with an identification of remaining problems and the extent to which these are being addressed within SINTAP.

Key words: SINTAP, Fracture, Review

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1. INTRODUCTION

European industry makes increasing use of so-called "fitness for purpose" or "engineering critical assessment" methods in order to assess the likelihood of failure of structures, or to specify materials requirements and safe operating conditions. These methods rely on a detailed knowledge of the relationships between the toughness of the materials of construction, the presence of defects, and the stresses applied to a structure.

In this report, some aspects of these "fitness for purpose" methods are reviewed. For each aspect considered, the review identifies what is well accepted, what is state of the art, and what needs to be done both in verifying the state of the art and in further development. The review is not exhaustive but addresses only those aspects within task 2 of the SINTAP (Structural Integrity Assessment Procedures for European Industry) project. Other aspects are being addressed within the other SINTAP tasks.

Currently, there are two self-contained defect assessment procedures: the British Standards document PD6493 (Ref 1) and the R6 approach (Ref 2). Both procedures have been extensively validated and shown to be safe, i.e. to err on the side of conservatism. However, to apply these procedures to practical structures it is necessary to have certain basic information. This information may be conveniently discussed by considering the two parameters K_r and L_r used in R6. These are defined by

$$K_r = K_I\,(P, a) \,/\, K_{mat} \qquad (1)$$

$$L_r = P \,/\, P_L\,(a, \sigma_y) \qquad (2)$$

K_r measures the proximity to linear elastic fracture and depends on the material fracture toughness, K_{mat}, and on the linear elastic stress intensity factor K_I, which in turn depends on the magnitude of the applied loading, P, and the defect size, a. L_r measures the proximity to plastic collapse with P_L being the value of the plastic collapse load for a perfectly plastic material with yield stress, σ_y.

Clearly, solutions for K_I and P_L are required for a range of geometries, defect sizes and loading conditions in order to calculate K_r and L_r and, therefore, to apply defect assessment procedures. In view of the fundamental importance of these solutions, these are discussed first in Section 2 of this review.

Although the limit load is defined in equation (2) in terms of a yield stress, materials used in industry exhibit a wide range of hardening behaviour beyond initial yield. Such hardening can lead to an increase in load bearing capacity beyond that for an elastic perfectly plastic material. This is reviewed in Section 3 in terms of the effect of the yield to tensile strength ratio.

For pressurised components, a defect may grow in such a way as to cause, in the first instance, a stable detectable leak of the pressure boundary rather than a sudden, disruptive break. A leak-before-break argument is aimed at demonstrating that leakage of fluid through a crack in the wall of a pipe or vessel can be detected prior to the crack attaining conditions of instability at which rapid crack extension occurs. Such arguments are a useful complement to arguments that a crack will not penetrate a pressure boundary and are reviewed in Section 4.

Although the PD6493 and R6 procedures have been shown to be conservative, there is a growing need to quantify the safety factors inherent in the use of engineering critical assessment procedures. One area where such a safety factor is known to exist is in the measurement of the fracture toughness, K_{mat}, in equation (1). This is typically carried out on small-scale specimens containing a deep crack and tested under predominantly bending loads. In contrast, structures such as pipelines, tanks and pressure vessels are often under predominantly tensile loading and contain only shallow defects as might arise from fatigue cracking or fabrication problems. Transfer of results from standard small-scale tests to structures therefore incorporates a variable safety factor due to ‘constraint’, which should ideally be quantified. Developments in this area are reviewed in Section 5.

As written, equations (1) and (2) refer to single application of a load P. However, components are often subjected to a variable load history and, in particular, to a prior overload or proof test. This prior overload can affect the material properties to be used in a subsequent defect assessment and this is discussed in Section 6.

Following the reviews of the various inputs to a defect assessment in Sections 2-6, which include the future work required to develop defect assessment methods in these areas, some concluding remarks are contained in Section 7.

2. STRESS INTENSITY FACTOR AND LIMIT LOAD SOLUTIONS

2.1 Stress Intensity Factor Solutions

For mode I loading, the stresses σ_{ij} close to a crack tip, calculated elastically, may be written as

$$\sigma_{ij} = \frac{K_I}{(2\pi r)^{1/2}} g_{ij}(\theta) + T\delta_{1i}\delta_{1j} + 0(r^{1/2}) \qquad \textbf{(3)}$$

as $r \rightarrow 0$, for polar co-ordinates (r,Θ) centred at the crack tip. Here, g_{ij} are angular functions of Θ, δ_{ij} is Kronecker's delta and the second-order T stress term can be regarded as the stress parallel to the crack flanks. This term is considered as a measure of constraint in Section 5. In this section, attention is focused on the stress intensity factor, K_I.

2.1.1 Calculation of Stress Intensity Factors

Solutions for stress intensity factors (SIFs) can be obtained using analytical or numerical methods. Closed form solutions obtained by analytical methods are available for some cracked bodies, especially those where the geometry is simple or the body is infinite. However, most solutions for SIFs have been obtained by numerical methods with the finite element method mainly used for this purpose.

Values of SIF are usually published in terms of non-dimensional geometry factors, Y,

$$Y = K_I / (s \sqrt{p\,a}) \qquad \textbf{(4)}$$

where σ is some convenient applied stress. In some cases interpolation formulae are provided for Y as a function of parameters such as crack aspect ratio, normalised crack depth or radius/thickness ratio of a cylinder. Such formulae are convenient where calculations are required for a range of crack sizes; for example, when addressing fatigue or stable crack growth or calculating margins on crack size.

In practice, components are subjected to a range of loading conditions including thermal and residual stresses. Therefore, SIF solutions are often required as a function of uncracked body elastic stresses normal to the prospective crack plane rather than simply as a function of a nominal applied stress as in equation (4). For non-uniform stresses the influence and weight function methods can be used. These functions are available for some cracked bodies. In applying influence functions the uncracked body stresses have to be approximated by polynomials. Such an approximation is not necessary if weight functions are used.

2.1.2 Handbook Solutions for Stress Intensity Factors

Many SIF solutions can be found in handbooks (Refs 3-7), some of which (Refs 3,5) describe in more detail the calculation methods discussed above. For frequently used geometry and load conditions, documents on defect assessment methods (Refs 1,2,8 & 9) can also be used and these contain references to other solutions. There are also simplified SIF formulae for very general use in codes (Refs 1,10,11) which, however, are necessarily rather conservative.

Most solutions can be found for mode I loading, while for modes II and III solutions are available for simplified geometries only. For numerical solutions estimates of their accuracy are usually given. Despite the advantage of newer editions, for some cases more information can be found in older handbooks, for example cracks in sheets with stiffeners in (Ref 4).

Many exact solutions and solutions with high accuracy can be found in Tada's handbook (Ref 5). Most are for two-dimensional cracks in semi-infinite solids or in plates. The handbook includes solutions for multiple cracks, cracks under point loads and modes II and III loading, cracks at holes and penny-shaped cracks.

Zahoor's handbook in 3 volumes covers cases of cracked pipes including cracks in piping tees and elbows. Solutions are provided for a wide range of geometry parameters but the accuracy of some solutions is not very high.

Murakami's handbook, also in 3 volumes, is the largest collection of SIF solutions. Volumes 1-2, published in 1987, are divided into 18 chapters. Several chapter titles give some idea of the structure of the handbook: Fracture Mechanics Test Specimens; Finite Width Plate Containing

Two-Dimensional cracks; Cracks in a Circular Plate or Cylinder; Cracks at Stress Concentration; Three-Dimensional Surface and Interior Cracks; Cracks in Welded Joints; Cracks in Residual Stress Field. Volume 3, published in 1991, has the same structure and can be regarded as a supplement containing newer results.

2.1.3 Solutions for Complex Geometries and Flaws

In practice, complex geometries are often handled by using solutions for simple geometries to which the actual one can be approximated. However, the corresponding stress fields, as determined by finite-element analysis for example, are generally non-linear and hence K_I needs to be determined by influence or weight function techniques. Such functions are, therefore, needed for surface, embedded and through-thickness cracks and basic requirements are listed in Table 1. For many of the cases listed, solutions are available in the literature but for others further work is needed as discussed in Section 2.1.4.

For offshore structures, stress intensity factor solutions are required for tubular joints. A limited number of solutions are available, particularly from finite-element analyses of Y joints with semi-elliptical surface cracks (See Appendix L of the draft revision to Ref 1). More generally, estimates can be obtained using plate solutions.

For through-wall axial and circumferential cracks in cylinders, comprehensive stress intensity factor solutions have recently been reported (Ref 12). These solutions were obtained from three-dimensional finite-element calculations for cylinders with mean radius to wall thickness ratios ranging from 3 to 100. The solutions cover membrane (or internal pressure), global bending and through-wall bending loads.

2.1.4 Further Work Needed

Although the stress intensity factor handbooks are valuable sources of information, a collation of SIF solutions is needed for efficient use of defect assessment methods. Such a collation should take account of additional solutions not included in handbooks and address a limited number of solutions most suitable for practical use regarding modelling of geometry and loading of real components, accuracy, and validity for a wide range of geometry parameters. This limited list is indicated in Table 1 which also includes areas where further work is needed beyond extraction of results from the literature. It may be noted that work is needed, in particular, to increase the order of available influence functions to address the highly non-linear stresses which occur in locations with stress concentrations and in the residual stress fields associated with welded joints. For this last application, it may be noted that stress intensity factor solutions for self-balancing through wall residual stress distributions representative of those which might arise from some welding processes are given in (Ref 13) for through-wall defects. These stress intensity factors are dependent on the wall thickness rather than the length of the through-wall crack.

2.2 **Limit Load Solutions**

In this section, attention is focused on the limit load P_L. Methods for generating limit loads are briefly discussed in Section 2.2.1. Available solutions for homogeneous components are reviewed in Section 2.2.2 and for components containing mismatched welds in Section 2.2.3. For through-wall cracks, the limit load is often the so-called 'global' collapse load, ie. the rigid plastic limit load of the structure, calculated for a rigid-plastic material with a yield stress equal to σ_y. For surface or embedded flaws it is also possible to define a 'local' collapse load which is the load needed to cause plasticity to spread across the remaining ligament, calculated for an elastic-perfectly plastic material with a yield stress σ_y. This distinction between local and global collapse loads is discussed in Section 2.2.4. Finally areas where further work is needed are reviewed in Section 2.2.5.

In this section, limit loads in the region of a flaw are reviewed but it should be recognised that when a component is being assessed, the possibility of collapse elsewhere in the structure should also be investigated. For such investigations, plastic collapse loads for undefective components are needed and these are contained in a number of textbooks and have, for example, been reviewed by Save (Ref 14).

2.2.1 Methods for Obtaining Limit Load Solutions

There are a number of methods for obtaining limit load solutions including the following:-

The forces and moments, or their equivalent elastically calculated stresses acting over the gross section containing the flaw may be treated using a generalised plate model. Such an approach is common in Codes but care must be taken when the stress normal to the crack plane is not the dominant stress component; as occurs for circumferential cracks in pressurised cylinders, for example.

Established plastic limit load analysis or lower bound limit analysis may be used.

Non-linear finite-element analysis may be used.

Elastic finite element analysis may be used with a lower bound limit load deduced by invoking the lower bound limit load theorem. The accuracy of this approach may be improved by iteratively modifying the elastic stiffness of individual elements and convergence of the method to the true limit load may be examined by involving the upper bound theorem of limit analysis (Ref 15).

A scale model of the structure may be tested, taking care not only that the flawed structure and loading are modelled correctly, but also that the model fails by plastic mechanisms. Care needs to be taken in interpreting limit loads in tests as a result of material work hardening beyond yield or ductile crack growth prior to collapse.

More information on some of these methods is given in Appendix 2 of R6 (Ref 2) and in (Ref 16).

2.2.2 Available Limit Load Solutions for Homogeneous Components

A review by Miller (Ref 16) contains solutions for flaws in plates, bars, cylinders, spheres, pipe bends and some shell intersection geometries. Further solutions are contained in (Ref 9). However, it should be recognised that the accuracy of many solutions is not known and (Ref 16) lists a number of alternative solutions for some cases.

In a similar manner to Table 1, a list of basic limit load solutions required for a practical assessment procedure is given in Table 2. These solutions are required as functions of external loads rather than local stresses as local thermal and residual stresses do not influence plastic collapse. The basic solutions may then be supplemented by specific solutions for elbows and tubular joints, although such solutions are often limited to specific component and flaw dimensions. For example, global collapse loads for ship structural details are given in (Ref 17) including advice for treating stiffened members.

2.2.3 Available Limit Load Solutions for Mismatch Welds

For defects in welds with a mismatch in tensile properties from the surrounding base material, the mismatch limit load, which takes account of this strength difference, is an important input to defect assessment procedures (Ref 18). Such limit loads may be calculated by the established methods described in Section 2.2.1. While solutions for defective mismatched welds are not widely available, results have been obtained for centre cracked plates, three point bend specimens, and for fully circumferentially cracked cylinders under axial load and tension. These solutions are summarised in (Ref 18) and more recently in (Ref 19) and cover a range of mismatch ratios. When the size of an overmatched weld is large compared to the remaining ligament ahead of a defect, the limit load may approach that of the defective geometry made totally of weld metal.

2.2.4 'Global' and 'Local' Solutions

The distinction between so-called 'global' and 'local' collapse loads for part-penetrating defects has been described above. As the ligament thickness ahead of a part-penetrating defect tends to zero, the 'local' limit load tends to zero. However, failure of the ligament need not correspond to overall yielding as the component may be able to sustain a fully penetrating defect.

The 'local' limit load is less than or equal to the 'global' limit load. Therefore, in assessments its use generally leads to conservative results. However, in leak-before-break cases, for example, a more realistic assessment is required and, therefore, the 'global' limit load may be preferred.

It is important to recognise that the limit load is often used explicitly or implicitly to estimate crack tip parameters such as J or COD. Therefore, the choice of limit load solution can be based on whether the 'local' or 'global' solution provides the more accurate estimate. Some available solutions for J have been listed by Chell et al (Ref 20) and include semi-elliptical surface flaws in plates subjected to tension and bending and in cylinders subjected to internal pressure. As demonstrated in (Ref 20), an appropriate limit load can be chosen to estimate J at both the surface and deepest points of such defects. The 'global' limit load often appears to provide the better estimate of J but can lead to non-conservative assessments.

The use of a reference load to estimate J has also been developed by Gilles et al (Refs 21-25). These workers have examined surface defects in pipes and elbows under pressure, tension, bending and combined loadings. The results lead to 'limit loads' which can be used to define L_r by eqn (2) within the R6 method (Ref 2) or to define J-estimation schemes. The results of (Refs 21-25) are of importance in view of the practical geometries considered.

2.2.5 Further Work Needed

A collation of limit load solutions is needed for efficient use of defect assessment methods. In a similar manner to Table 1 for SIF solutions, a limited list of solutions most suitable for practical use is given in Table 2 for limit loads. This table indicates areas where further work is needed beyond extraction of results from the literature. It can be seen that an important area for further work is provision of advice on the use of the 'global' or 'local' limit loads. It is also important to quantify the accuracy of the available solutions.

3. YIELD/TENSILE STRENGTH RATIO EFFECTS

The trend towards the optimisation of the useful weight of structures has led to the use of increased strength material. In this context high strength ferritic steels (σ_y >450 MPa) have a significant potential contribution which still remains largely unrealised. This is predominantly due to design code limitations, the upper allowable limit of yield stress/ultimate stress ratio (Y/T) being particularly severe. (Ref 26) presents a review of the current literature on the origins, causes and structural significance of high Y/T ratios in steels. In this section, the broad conclusions of this review are summarised and areas for further work are identified.

3.1 Summary of Effects

The significance of the yield/ultimate tensile strength ratio on the fracture behaviour of steels has been investigated by means of a literature review (Ref 26). The principal areas assessed are the origins of the concern over Y/T ratio, the inter-relationship between tensile parameters, the structural significance of the Y/T ratio and its treatment in assessment codes. The main findings of the review are summarised in the paragraphs below.

Limits to the Y/T ratio were introduced into design codes based on the behaviour of 'first-generation' high strength steels and the notion that a high Y/T value equates to poor fracture performance. Modern steels give higher elongation values for a given strength level and Y/T ratio and the initial concern is of lower relevance to modern steels.

Modern ferritic steels produced via controlled rolling or quenching and tempering generally have Y/T in the range 0.8 - 0.95 compared to 0.65 - 0.75 for normalised steels. A high Y/T ratio is generally associated with a low work hardening rate (high value of n in eqn (6) below); the relationship is however neither linear nor consistent.

Current design code limits for Y/T vary between 0.67 and 0.90. There is general agreement that values of up to 0.85 are satisfactory in conventional structural applications and values up to 0.95 in specific cases. However, these limits have not yet found their way into design codes.

Of the various parameters applicable to the post-yield regime, the yield tensile ratio, strain hardening exponent, local elongation (Lüders strain) and strain at UTS are the most relevant parameters for structural integrity assessments.

Numerous estimates of n are available; most rely on the assumption that n can be correlated with yield stress. Such correlations are promising, particularly when different forms are used for different strengthening mechanisms. Furthermore, the strain at UTS appears to give a reasonable estimate of n.

The presence of a crack modifies the shape of the load-deflection curve; crack depth and n dictate the extent of this. The yield point may be suppressed and a Lüders band not obtained. The latter effect can however be observed in the CTOD strain response where a plateau of CTOD can be achieved beyond yield in steels showing a Lüders plateau in the conventional tensile test.

The significance of Y/T in buildings and bridges is only relevant for cases of earthquake resistance in the former and plastic design in both structures. Design rotation capacities (maximum/yield rotation) of connections are typically 3 for general plastic design and 7 for severe earthquake design. The rotation capacity tends to decrease with increasing Y/T ratio although geometry and thickness also have a major influence.

For tension members gross section yielding rather than net section fracture is the preferred failure mode. Achievable elongation in the presence of holes such as bolt holes is very sensitive to Y/T. For tapered members, strong sensitivity is only noted above Y/T of about 0.85. However, industry experience suggests steels with values of Y/T up to 0.95 can be used without problems.

In the case of pressure vessels, burst pressure has been found experimentally to increase with decreasing strain hardening exponent.

For tubular joints, decreasing Y/T from 1.0 to 0.66 at constant yield stress has been found to enhance the ultimate joint capacity by only 6%. Tentative guidance suggests an upper limit of Y/T of 0.85.

Work on defect containing pipelines has demonstrated that the effect of increasing defect depth on tolerable defect length is more significant than increasing Y/T. For deep defects there is little influence of Y/T above 0.85. For shallow defects there is significant influence above Y/T of 0.90.

A number of failure assessment diagrams (FAD) are currently available in R6 (Ref 2) which incorporate the Y/T effect indirectly through limits on flow stress definition. Others are generated using actual stress-strain data. Both the shape of the FAD and the plastic collapse parameter cut-off depend on the Y/T ratio. Industry experience with these methods is wide but the influence of high Y/T steels on the suitability of the FAD parameters needs assessing.

The Engineering Treatment Model (ETM) incorporates an estimated strain hardening parameter in its approach. The method therefore predicts different behaviour for steels with the same yield strength but varying n. The method is of significant interest in its ability to characterise behaviour of steels with varying Y/T ratios and n values.

Crack driving force curves based on PD6493 (Ref 1) suggest relatively little influence of the effects of Y/T in the range 0.5 - 0.8 but a significant influence above this level. Similar curves based on the ETM show a systematic effect of n at all levels.

3.2 Future Work

Future work on the subject of yield/tensile ratio within the SINTAP project should concentrate on the following aspects:

1. Establish relationships between yield strength, Y/T, strain at UTS, n, composition and steel type to enable more accurate predictions of the relevant post yield tensile parameters.

2. Examine the relationship between conventionally defined n and the ETM defined n and establish the significance of this on predictions.

3. Examine the significance of the yield plateau on behaviour of steels containing cracks.

4. Assess the inter-relationship between crack depth and significance of Y/T.

5. Determine the influence of Y/T on the shape and cut-off limits of FADs and assess the accuracy of predicted crack driving force curves through comparisons with the results from wide plate tests on different steels (parent plates).

6. Assess the potential of a strain-based FAD, methods of deriving such an FAD and how it compares with actual wide plate data.

7. Assess the abilities of PD6493 levels 2 and 3 and the ETM to predict the crack driving force curves of parent plate wide plate tests.

8. Link with SINTAP Task 1 to assess the influence of Y/T in welded joints and the effect on the significance of mis-match.

4. LEAK BEFORE BREAK AND CRACK SHAPE DEVELOPMENT

Over recent years, the concept of Leak-Before-Break (LBB) has gained considerable world-wide momentum in establishing safety cases for pressurised components, particularly in the nuclear industry in relation to primary pipework.

The various stages in the development of a LBB argument may be explained with the aid of the diagram shown in Fig. 1. This diagram has axes of crack depth, a, and crack length l, normalised to the pipe or vessel wall thickness, t. An initial part-through crack is represented by a point on the diagram. The crack may grow by fatigue, tearing or any other process until it reaches some critical depth at which the remaining ligament ahead of the crack breaks leading to a through wall defect. The crack then continues growing in surface length until there is sufficient opening to cause a detectable leak or until the crack becomes unstable. A LBB argument is aimed at demonstrating that leakage of the appropriate gas or fluid through the crack can be detected prior to the crack attaining conditions of instability at which rapid crack extension occurs.

LBB arguments may be used as part of the case for the elimination of pipe-whip restraints, be applied in regions that are difficult to inspect due to inaccessible or hazardous conditions and/or be applied as defence-in-depth considerations.

Various methods for LBB have been developed in several European countries, many of which are based on a procedure published by the US Nuclear Regulatory Commission in NUREG 1061 (Ref 27). Procedures relating to the NUREG 1061 approach are relatively simplistic in that they are based on detectable leakage. Their starting point is to postulate a fully-penetrating defect and show that, should such a defect arise, the leakage would be detectable before the defect grows to a limiting length. More rigorous LBB approaches have also been developed which involve calculating the growth of a postulated initial surface flaw up to and beyond penetration of the back-surface after which leakage rates are evaluated. The recently revised LBB Appendix (Appendix 9) (Ref 28) of R6 (Ref 2) incorporates both the NUREG 1061 and the more rigorous type of approach.

4.1 General Outline of Procedures

The NUREG 1061 type of approach consists of the following steps:

1. The limiting length of a through-wall crack is evaluated for the most severe loading conditions (ie. the loading condition resulting in the lowest value of limiting crack length).

2. The length of through-wall crack, corresponding to that which leaks at the minimum detectable rate under normal operating conditions, is evaluated. In practice, crack opening areas and leakage rates are calculated for different crack lengths until the detectable leakage length is attained.

The actual NUREG 1061 procedure requires there to be factors of at least 10 on the detectable leakage rate, at least 1.4 between the loads to cause instability of the postulated flaw and normal operation + Safe Shutdown Earthquake (SSE) loads, and at least 2 between the limiting crack length and the leakage crack length. In contrast, no safety margins are specified in the R6 LBB adoption of this method. This is consistent with the general R6 procedures which invoke the principle of undertaking sensitivity studies as an alternative to the safety factor concept.

The more rigorous LBB approaches typically consist of the following steps:

1. A known or postulated "initial" crack (usually a surface crack) is characterised in accordance with appropriate characterisation rules.

2. The shape development arising from potential crack growth mechanisms of the postulated crack is assessed.

3. The defect length at which ligament failure is predicted to occur is calculated.

4. The defect for which ligament failure is predicted to occur is re-characterised as a through-wall crack.

5. The limiting length of through-wall crack is evaluated for the most severe loading conditions (ie. the loading condition resulting in the lowest value of limiting crack length).

6. Provided the limiting length of through-wall crack (step 5) is greater than the re-characterised through-wall crack at ligament failure (step 4), the crack opening area for the re-characterised crack is calculated.

7. Leakage rate is calculated for the crack opening area evaluated in step 6.

8. The time to detect the leak from the crack is estimated.

9. The time to grow the re-characterised through-wall crack at ligament failure (step 4) to the limiting crack length (step 5) is evaluated.

4.2 **Review of Main Aspects**

4.2.1 Limiting Crack Length

The limiting length of a through-wall crack may be calculated using defect assessment procedures such as (Refs 1, 2). It is important that the minimum limiting length is calculated for the most severe loading condition which could be a frequent, infrequent or seismic loading case. To ensure a conservative assessment, lower bound material properties relevant to the crack location (eg. weld, parent or heat affected zone material) would usually be specified. All relevant primary and secondary stresses need to be taken into account.

4.2.2 Crack Opening Area

Estimation methods for crack opening area can be classified into three categories: linear elastic models; elastic models incorporating a small scale plasticity correction; and elastic-plastic models.

Several elastic solutions are available in the literature for crack opening areas in pipes, cylinders and spheres. However, the solutions are generally based on thin-shell theory and the calculated crack opening is assumed to be at the mid-thickness position of the wall. These solutions therefore do not take account of crack taper arising from geometry effects and from through-wall bending loads.

(Ref 12) includes crack opening displacement (2δ) solutions for a range of axial and circumferential through-wall cracks in cylinders. As explained in (Ref 12), crack opening area can be calculated from these values by assuming an elliptical shape (ie. $\pi a\delta$ where a is crack semi-length). Alternatively, to always ensure conservatism, crack opening area can be evaluated as $2.5a\delta$.

The solutions of (Ref 12) are included in a table in the revised Appendix 9 of R6, reproduced here as Table 3 which gives recommended crack opening area solutions for the three categories referred to above. The more accurate elastic-plastic model of (Ref 33) is recommended for best estimate LBB calculations where stress levels are high enough to induce significant plasticity (ie. L_r greater than about 0.4). However, this method is detailed and requires a description of the material stress strain curve.

Some justification for the solutions recommended in Table 3 is given in (Ref 34).

4.2.3 <u>Leak Rate</u>

The calculation of the fluid flow or leak rate through a crack is in general a complex problem involving the crack geometry, the flow path length, friction effects and the thermodynamics of the flow through the crack.

Several computer codes have been written to predict leakage rates through cracks for a variety of fluids. For single-phase flow, DAFTCAT (Ref 35) calculates flow rates through rectangular section cracks and includes the effects of friction. For two-phase

flow of steam/water mixtures, PICEP (Ref 36) and SQUIRT (Ref 37) can be used to calculate leak rates through a variety of cracks. All of these programmes have been validated to some extent against a variety of experimental data and reasonable agreement with experiment obtained. Whilst flow rate measurements have been made on real cracks, the extent of validation for such cracks is relatively small and the agreement with theory less good.

The likely accuracy of the leak rate predictions for both single and two-phase flows depends on a variety of factors and must be judged by examining the available validation data.

Formulations for friction factor f, show it to increase continuously as roughness increases or crack width (opening) reduces. However, flow rate experiments show that f does not increase continuously, but reaches an effective maximum. The effective maximum friction factor, f_{max}, is dependent upon surface geometry. In (Ref 38) a theory is advanced to justify the existence of a maximum friction factor and this is assessed against experiments using relatively large scale conforming surfaces (ie. one surface is manufactured and the opposing one is a replica). For the surface of most relevance to structural defects, random roughness, f_{max} was approximately 0.2. For a very regular and stepped surface f_{max} was unity. This range of values has been confirmed by experimental data on flow through real cracks, the results of which are discussed collectively in (Ref 39). The above discussion on f values relates to fully developed turbulent flow. Higher values can occur in laminar flow, but are of little interest in LBB.

4.2.4 Leak Detection

Reference 40 gives some information and guidance on leak detection systems. There are two broad categories; global and local. Examples in the global category are sump pumps, pumps for water systems, humidity detection for steam leaks, gas levels in air for gaseous systems, and radiation monitors for nuclear systems. All global systems detect all leaks and hence any leakage indications on the monitoring equipment need to be investigated and the source established. The response time for such systems is relatively long and depends on plant segregation.

Local leak detection systems monitor specific plant features (eg. a weld) or a well defined area (eg. length of pipe). Some detectors are medium or plant specific. For example, moisture sensitive tape only works in water or steam systems where condensation can take place on the outer surface.

Leakage through cracks generates acoustic emission that is transmitted through the structure, and, in some circumstances, through the air. Wave guide and microphone systems have been developed which offer flexible and sensitive leak detection capabilities for a wide range of fluids.

4.2.5 Crack Shape Development

All the aspects covered under sections 4.2.1 to 4.2.4 are relevant to the simplified NUREG 1061 approach in that a straight fronted crack is assumed with no account being taken of prior growth. In reality, after breakthrough, the crack shape for real growing cracks can be quite complex with the outer crack length often much smaller than the crack length at the inside of the pipe. This affects both the resulting leak rate as well as the crack growth rate at the leaking crack. A good understanding of crack shape development is important for undertaking the more detailed LBB methodology outlined in Section 4.1. Therefore, a more detailed review of this aspect is given in Appendix 1.

It may be noted that Brickstad (Appendix 1) infers that local cooling of the outer wall surface can occur due to flow discharge. This phenomenon has also been reported by Eperin et al (Ref 41) from analytical work on applying LBB to the Leningrad Nuclear Power Plant. However, such calculated cooling may be a consequence of inaccurate thermodynamic assumptions since there is experimental evidence (Ref 42) to suggest that no significant cooling actually occurs within the material.

The review given in Appendix 1 covers the growth of a surface crack, wall penetration and growth of a leaking crack to final failure, all with reference to both reported experimental and analytical studies.

Guidance on crack shape development, particularly at and following wall penetration is also given in the new Appendix 9 of R6. The guidance is based on experimental evidence, reviewed in (Ref 43), which shows that for cases where stress distributions are predominantly tensile, cracks tend towards a rectangular shape. The R6 recommended re-characterisation rules for such loading and where failure occurs in a ductile manner are summarised in Fig. 2. In order to be conservative for cases where the stress distributions are predominately through-wall bending, the R6 recommended re-characterisation rules are as summarised in Fig. 3.

4.3 **Unresolved Issues and Scope for Further Work**

In Appendix 1, Brickstad highlights some unresolved issues as being (i) the accurate characterisation of the breakthrough crack, (ii) the problem of being able to accurately predict crack shape development from J-R data obtained from small specimens, and, (iii) problems in accurately evaluating crack opening areas in weldments due to both strength mis-match and residual stress effects.

Appendix 1 outlines proposed work under the SINTAP programme to undertake a series of detailed non-linear finite element analyses to assess crack characterisation at breakthrough.

Other uncertainties where there is scope for further work include; (a) the effect of restraint and non-symmetrical loading on crack opening area (eg. a crack around a nozzle), (b) crack opening area solutions for pipe bends and T-junctions, (c) validation of limit load solutions for through-thickness cracks, (d) further experimental validation of flow rate models applied to realistic cracks, and (e) the effect of multiple defects and associated proximity effects which are relevant to defects associated with stress corrosion cracking, for example.

5. CONSTRAINT

Both numerical analysis and laboratory measurements have been used to demonstrate that the toughness of a material under elastic-plastic conditions depends on geometry, loading, specimen thickness and normalised crack depth, a/w, where w is specimen width. Such a dependence is usually referred to as an effect of 'constraint'. It is found that tests on deeply cracked bend specimens (high constraint geometries) under plane strain conditions provide the lowest value of toughness and the use of such toughness as K_{mat} in equation (1) leads to conservative assessments. However, there has been significant worldwide activity in recent years to reduce conservatisms by taking credit for the increased toughness in lower constraint conditions.

In this section, the recent developments in quantifying constraint effects are reviewed. First, parameters for indexing constraint levels in structures and their availability are discussed in Section 5.1. Then, experimental data illustrating the influence of constraint on material toughness are described in Section 5.2. Section 5.3 reviews 'local' approach methodologies which are capable of predicting the observed increase in toughness with reducing constraint. While such methodologies are capable of being applied to structural geometries, the numerical effort required is large and, therefore, simpler methods based on the parameters discussed in Section 5.1 have been developed. These are reviewed in Section 5.4. Validation for these methodologies is described in Section 5.5 and areas for further work are outlined in Section 5.6.

5.1 Constraint Parameters and Their Availability

The elastic T-stress in equation (3) has been used as a means of quantifying crack tip constraint (Ref 44). As T can be evaluated by elastic analysis it is relatively straightforward to determine and solutions are available in the literature for a number of cases. A compendium of these solutions has been compiled by Sherry et al (Ref 45) for both two and three dimensional cracked geometries. The solutions in the compendium are for the following geometries:

- Centre-cracked plate tension specimen (CCT)
- Centre-cracked plate tension specimen biaxially loaded (CCBT)
- Double-edge cracked plate tension specimen (DECT)
- Single-edge cracked plate tension specimen (SECT)
- Single-edge cracked pure bending specimen (SENB)
- Single-edge cracked three-point bending specimen (3PB)
- Compact-tension specimen (CT)
- Double-cantilever beam specimen (DCB)
- Axisymmetrically-cracked tensile specimen (ACT)
- Circumferentially-cracked cylinder under a tensile stress (CCCT)
- Semi-elliptically cracked plate under uniform tension (SECPT)
- Semi-elliptically cracked plate under uniform bending (SECPB)

For use in the procedures described in Section 5.4, it is convenient to normalise the T-stress in terms of the limit load parameter L_r of equation (2). This produces the elastic constraint parameter.

$$\beta_T = T/(L_r \sigma_y) \qquad \textbf{(5)}$$

For the cases listed above, with the exception of the DCB geometry, Sanderson et al (Ref 46) have compiled a compendium of β_T solutions. An example for the SECPB geometry is given in Figure 4.

Although the T-stress is based purely on elastic analysis, it has been used to characterise constraint beyond the elastic and small-scale yielding regimes. To extend constraint descriptions into the widespread plastic regime, analyses of crack tip fields for elastic-plastic materials have been performed. For materials in which plastic strain, ε^p, is related to stress, σ, by a power law

$$\in^{p} = \alpha' \in_{\gamma} (\sigma/\sigma_{\gamma})^{n} \tag{6}$$

where α', n are constants and $\varepsilon_y = \sigma_y/E$, the stress field near the crack tip as $r \rightarrow 0$ may be written

$$\frac{\sigma_{ij}}{\sigma_y} = \left(\frac{J}{\alpha' \sigma_y \varepsilon_y I_n r} \right)^{1/n+1} \tilde{\sigma}_{ij}(\theta, n) + Q\delta_{ij}, \quad |\theta| < \frac{\pi}{2} \tag{7}$$

Here I_n and σ_{ij} are functions of n as determined by the asymptotic analyses of Hutchinson, Rice and Rosengren.

Unlike eqn (3) for elastic response, eqn (7) is not a strict asymptotic analysis of the higher-order terms for the non linear problem. Instead, the hydrostatic term, Q, is an approximation to the collective behaviour of a number of higher-order terms in the forward sector, $|\Theta| < \pi/2$, ahead of the crack (Ref 47).

An alternative convention used to define Q is as the difference between the near-tip stress field and that under small-scale yielding at the same value of J with T = 0; ie.

$$\sigma_{ij}/\sigma_y = \sigma_{ij}^{ssy}/\sigma_y + Q\delta_{ij} \tag{8}$$

In practice, at least in two-dimensional problems, the Q stress of eqns (7, 8) varies slightly with distance from the crack tip and has been evaluated at $r/(J/\sigma_y) = 2$. Thus, Q is evaluated at different physical distances from the crack tip as the load, as measured by J, increases.

It is worth noting that the yield stress, σ_y, in eqn (6) is somewhat arbitrary as the same stress-plastic strain relationship can be obtained for different values of σ_y by adjusting the constant α'. As Q is evaluated at a distance dependent on σ_y, it is, therefore, necessary to adopt a consistent definition such as the 0.2% proof stress.

The load dependence of the Q-stress is more complex than the linear relationship for the elastic T-stress which enables a load-independent parameter to be defined by eqn (5). However, it transpires that an approximately load-independent parameter, at least for loads which are small compared to the collapse load, may be obtained as

$$\beta_Q = Q/L_r \tag{9}$$

Reference 46 contains β_Q solutions, for Q defined by eqn (8), plotted against L_r for the CCT, DECT, 3PB and CT geometries. Figure 5 gives an example of the β_Q solutions for the 3PB geometry for a range of work hardening coefficients, n, and a/w values. All currently available Q-stress solutions were taken on board in compiling the β_Q solutions given in (Ref 46). It may be seen from Fig. 5 that β_Q is only weakly dependent on load (or L_r) and on the value of n but that constraint reduces with reducing relative crack depth, a/w.

Parameters other than T and Q have also been proposed as measures of constraint. For example, the ratio of hydrostatic to equivalent stress ahead of the crack tip or higher order terms in the expansion of the crack tip stress field (Ref 48) have been proposed. From slip-line field solutions, the level of constraint at the crack tip at plastic collapse ($L_r = 1$) can be deduced and this has been used to deduce constraint levels for defects in mis-matched welds (Ref 49). Such approaches are less well developed than the T and Q stress methods and information for their use is not widely available. Therefore, they are not discussed further here.

5.2 Influence of Constraint on Material Toughness

5.2.1 Cleavage Fracture

Several sets of experimental data have been presented for the transition region which illustrate that specimens with negative T-stress or Q-stress are tougher than deeply cracked specimens with positive T-stress or Q-stress values.

Betegon and Hancock (Ref 50) presented experimental results as J versus T/σ_y. Bend geometries with a/w < 0.3 give negative T values. For the deeply cracked geometries (a/w > 0.3), the toughness was found to be independent of geometry, as these geometries are known to have positive T.

Toughness tests on a low-grade mild steel at -50°C have been reported (Refs 51-53). Three point bend specimens with a/w between 0.05 and 0.78 were tested along with centre cracked plate tension specimens with a/w ratios between 0.63 and 0.77. Figure 6 shows the critical value of J versus T/σ_y at cleavage. A comparison between the two types of specimen shows that the low constraint CCT specimen gives slightly higher values of J_c than the 3PB specimen, at the same T-stress values. Fig 7 shows the same data reanalysed in terms of the Q-stress and indicates that slightly better correlation of CCT and 3PB specimens can be achieved. However, the dominant effect is the increase in toughness with reducing constraint rather than the choice of constraint parameter.

Reference 53 also presented data from test specimens in a high strength weld metal with a yield stress of 700 MPa at a test temperatures of -30°C. The critical value of J
was plotted as a function of both the T-stress and Q-stress parameters. Constraint

enhanced toughness was found to be even more significant for this type of material than for the mild steel. The J/T and J/Q analyses described the data equally well.

Kirk et al (Ref 54) have presented cleavage toughness data for an A515 steel at room temperature, using edge cracked bend bars with different a/w ratios and various thicknesses. They presented the results in terms of both J/T and J/Q. Similar trends were shown between the two types of analysis in terms of increased toughness with more negative crack-tip constraint parameter value. However, there appeared to be some inconsistencies between the actual values of T-stress and Q-stress in so much that whilst the reported T/σ_y values ranged from approximately -1.7 to +2, the Q values ranged from only approximately -1.3 to zero.

Sharples et al (Ref 55) reported on an experimental programme on 70mm thick A533B-1 steel plate specimens containing a surface crack and loaded in either uniaxial four-point or biaxial eight-point bending. Tests were carried out at temperatures of -75°C and -90°C for crack depths of 10% and 20% of the plate thickness. Fracture toughness values obtained from the tests showed relatively little scatter and all the values were shown to lie above the SENB shallow crack (a/w) lower bound transition curve. No detrimental effect of biaxial load was shown in terms of either toughness or crack mouth opening displacement, in contrast to other experimental work (Refs 56, 57). However, further finite element analyses are required before the experimental results of (Ref 55) can be fully verified. Results of T-stress analyses of the uniaxial and biaxial bend specimens are included in (Ref 55). The T-stresses are generally negative with little variation near to the deepest point of the crack but with a strong variation close to the free surface. Since failure in the experiments emanated from the deepest point of the crack, these T-stress results are consistent with the experimentally measured increase in toughness. However, (Ref 55) also presents the variation along the crack front of the sum of normalised T and S stresses where S is a constant stress term which acts parallel to the front of a three-dimensional crack. The (T+S) results for the eight-point bending cases are all positive and the results for the four-point bending cases are all negative. These (T+S) stress results are therefore inconsistent with the reported experimental results of (Ref 55) but generally support the results of (Refs 56, 57) that the fracture toughness for biaxial loading is lower than that for uniaxial bending.

5.2.2 Ductile Fracture

Hancock et al (Ref 58) tested samples of an American pressure vessel steel denoted A710 in the upper shelf regime. Crack extension occurred by stable tearing enabling both J and Crack Opening Displacement to be measured as a function of crack extension Δa. Figure 8 presents J for crack extensions Δa = 0, 0.2mm and 0.4mm. Figure 9 shows the corresponding crack opening displacement, δ, values.

The results show a marked effect of constraint on toughness after small amounts of crack growth. J values for centre cracked plates are approximately 4 times greater than those of highly constrained deeply cracked bend bars and CT specimens at a crack extension of 0.2mm. For the higher crack extensions (Δa = 0.4mm), the constraint effect is even more significant as J for the centre cracked plate specimens is more than 5 times the value for the deeply cracked bend bars. Geometries with positive T-stresses (deeply cracked bend bars and CT specimens) show little or no geometry dependence on toughness. Conversely, geometries which have negative values of T show geometry dependent toughness.

Sherry et al (Ref 59) investigated the upper shelf fracture toughness of an A533B-1 steel plate. J-resistance curves were obtained for small-scale (w = 50mm, B = 25mm) three-point bend specimens with a/w values of 0.3, 0.5 and 0.7 and for small-scale (w = 50mm, B = 25mm) centre-cracked plate tension specimens with a/w values of 0.5 and 0.7. J was plotted against the normalised T-stress (Fig 10). This shows a similar trend to the data of (Ref 58) in Fig 8. Curve fits to the data in Fig 10 were produced in terms of the parameter β_T of eqn (5), see Fig 11.

5.2.3 Summary of Observed Behaviour

The typical results described above illustrate general trends which may be summarised as follows.

(a) The fracture toughness at cleavage fracture tends to increase with reducing constraint.

(b) The fracture toughness at ductile crack initiation is relatively insensitive to constraint but the toughness after some small ductile crack extension tends to increase with reducing constraint.

(c) In view of (a) and (b), for ferritic steels there tends to be a reduction in the brittle to ductile transition temperature with reducing constraint.

(d) Data on the influence of biaxial loading do not show consistent effects although this may simply be that different specimen types have different constraint levels under such loadings.

5.3 **Local Approach Methodologies**

Local approach models have been the subject of much research in recent years and have been used to analyse fracture events in steels for a range of geometries and loadings (Refs 60-64). In essence, micro-mechanical models of the failure processes are used to relate the stresses, strains and 'damage' local to a crack tip or stress concentrating feature to the critical conditions required for fracture. The models are calibrated through material specific parameters which are derived from analyses of laboratory tests on notched or cracked components and quantitative metallography. Once the material parameters have been derived, structural assessments can be performed. Therefore, the effects of constraint are handled automatically and the models are capable of predicting the general trends summarised in Section 5.2.3.

There are a number of micro-mechanical models of fracture addressing both cleavage and ductile fracture. Some of these are briefly described here but fuller details can be obtained from the references cited.

> The Beremin cleavage model (Ref 65) predicts the cumulative probability of cleavage failure through a weakest-link interpretation of the cleavage fracture process.
>
> The Beremin model of ductile fracture (Ref 66) is based on the evaluation of stress and strain ahead of a crack tip controlling the initiation, growth and coalescence of voids (i.e. damage). The development of this damage controls the initiation and propagation of cracking.
>
> The Rousselier model of ductile fracture (Ref 67) also models crack extension through the initiation, growth and coalescence of voids. However, whereas the Beremin model uses an estimate of void growth rate due to Rice and Tracey (Ref 68), in the Rousselier model the constitutive equations of the material are modified to represent the deterioration of the material properties as voids develop. The ability of the material to support stress is gradually lost in the highly damaged regions and the crack tip essentially advances automatically without the need for a separate failure criterion, such as critical void radius.
>
> The Gurson model of ductile fracture (Ref 69), also uses a constitutive equation incorporating void growth. Refinements to this model have also been made to include void nucleation effects (Ref 70). The model has also been embodied in descriptions of a decohesion layer within which crack advance occurs when the cohesive strength of the layer is reached (Ref 71).

The separate ductile and cleavage models described above have also been combined to predict cleavage fracture in the brittle to ductile transition region of a ferritic stress where cleavage can occur after some ductile tearing (Refs 70, 72). These applications are very recent and still in the development stage.

As noted above, local approach methods have the ability to describe the effects of constraint on material response. This ability to predict the broad trends seen in Figures 6-11 may be used to reduce testing requirements by interpolating between data obtained at only a few levels of constraint. In addition, the models may be used directly to predict structural response. However, the methods are complex for general structural applications and require highly refined meshes near the crack tip. Hence, modifications to simplified defect assessment procedures to incorporate constraint have been developed. These are described next.

5.4 Defect Assessment Methodologies

5.4.1 Two parameter approaches

If fracture is described by a single parameter such as J or COD, then constraint can not be treated unless a component specific critical value of these parameters is available. More generally a lower bound critical value is obtained from deeply cracked bend specimens and conservative assessments arise. The constraint parameters, T and Q, described in Section 5.1 allow some reduction of conservatism by invoking two parameter fracture mechanics. In these approaches, fracture is assumed to be controlled by two parameters, such as (J, T). A locus of (J, T) with increasing load may be produced for a specific component and defect size using standard J estimation procedures such as (Ref 8) and solutions for T (or Q) from the information cited in Section 5.1. Examples for specimen geometries have already been given in Fig 10. The intersection of the (J, T) locus with the corresponding material toughness curve (for example Fig.11) then defines the load at fracture.

5.4.2 Failure Assessment Diagram Methods

Although they also include plastic collapse criteria, the failure assessment diagram methods embodied in R6 and PD 6493 (Ref 1, 2) are essentially single parameter procedures in that fracture is assumed to be governed by a single value of toughness or crack opening displacement. The parameters K_r and L_r of eqns (1, 2) are evaluated and the point (L_r, K_r) plotted on a failure assessment diagram. If this point lies within a failure assessment curve, $K_r = f(L_r)$, then failure is avoided (see Fig.12).

The failure assessment diagram methods may be modified to allow for constraint by replacing K_{mat} in eqn (1) by a constraint dependent toughness, K^c_{mat} say, which is a function of T or Q (Ref 73). With this approach, the shapes of the failure assessment curves in (Refs 1, 2) are unchanged but K_r is a nonlinear function of load through the dependence of K^c_{mat} on load (since both T and Q are load dependent). Alternatively (Ref 74), the failure assessment curve is modified to

$$K_r = f(L_r)\,(K^c_{mat}/K_{mat}) \qquad (10)$$

and the definition of K_r in eqn (1) is retained. A convenient fit to data which has been used in Fig.11 is

$$\begin{aligned} K^c_{mat} &= K_{mat} && \beta L_r > 0 \\ K^c_{mat} &= K_{mat}\,[1 + \propto(-\beta L_r)^m) && \beta L_r < 0 \end{aligned} \qquad (11)$$

where $\propto$, m are material and temperature dependent constants and β can be β_T or β_Q, Section 5.1, depending on the constraint parameter used. With this fit, eqn (10) becomes

$$K_r = f(L_r)\,[1 + \propto(-\beta L_r)^m] \qquad (12)$$

Some modified failure assessment curves using the R6 option 1 curve for $f(L_r)$ are shown in Fig.12. Whereas the option 1 curve is independent of geometry and material, the modified curves depend on geometry (through β), on material toughness properties (through $\propto$, m) and also on material tensile properties if β is defined in terms of Q.

Both the procedure using a modified definition of K_r and the procedure using a modified failure assessment curve have recently been included in a new Appendix 14 to R6 (Ref 2). The methodology is limited to mode I loading, to primary loads and to loss of constraint under plane strain conditions. Further work is, therefore, required to extend the approach. There is experimental validation for the modified R6 methods and this is discussed in Section 5.5.

5.4.3 The J_{ssy} Approach

An alternative approach for addressing constraint has been developed by Dodds et al (Ref 75). This involves calculating the value of J in the component which gives the same crack tip stressed volume as would be achieved in a specimen under small-scale yielding at a value J_{ssy}. Clearly, the 'stressed volume' needs definition and for cleavage fracture has been chosen as the volume over which the maximum principal stress exceeds a particular value. The relationship between J_{ssy} and J is shown schematically in Fig 13. High constraint conditions correspond to $J = J_{ssy}$. Low constraint corresponds to $J > J_{ssy}$. In essence, J_{ssy} is equated to the J equivalent of the (high constraint) fracture toughness K_{mat} and under low constraint the value of applied J must exceed J_{ssy} in order for the critical fracture condition to be achieved at the crack tip.

A difficulty with the approach is that component analyses must be performed to generate curves of the type shown in Fig. 13. Such curves are specific to the component, stress-strain curve, defect size, loading type and fracture criterion adopted. However, the curves have been found to be only weakly dependent on the magnitude of the maximum principal stress criterion (Ref 75) so that some simplification is possible.

5.5 Validation of Methodologies

Experimental validation for the modified R6 methods outlined in Section 5.4.2 has been addressed in (Ref 73) for centre-cracked and three-point-bend specimens for a range of materials: cleavage fracture of a grade 43A normalised plain carbon steel at -50°C; cleavage in a high strength weld metal at -30°C; cleavage fracture of a quenched plain carbon steel at room temperature; cleavage fracture of a normalised CMn steel at temperatures below -140°C; ductile crack initiation in an A710 pressure vessel steel at room temperature. The results demonstrate that points (L_r, K_r) lie close to, but outside, the R6 option 1 failure assessment line when K_r is defined in terms of K^c_{mat}. In contrast, analyses using the basic R6 procedures lead to over-conservative results with points well outside the failure assessment line.

Experimental validation on small- and large-scale tests is also summarised in the new R6 Appendix 14. The cleavage fracture results on a grade 43A normalised plain carbon steel at -50°C, have been compared with the modified R6 method using the local approach to obtain a lower bound to the constraint modified fracture toughness, K^c_{mat}. The results again demonstrate conservatism in the approach which is less than that using the basic R6 procedures. Cleavage fracture in two low constraint biaxial bend experiments on A533B steel plates in the lower transition region has also been addressed. The constraint modified R6 approach reduced the conservatism of the conventional failure assessment curves leading to a 30-40% benefit in terms of load margin for the lower constraint geometry, while maintaining conservatism. When the approach was applied to ductile fracture of a large scale single edge notched A533B plate at 20°C, the load margin was unchanged at about 1.1 because the structural constraint was not particularly low.

5.6 Further Work

The availability of solutions for constraint parameters, as discussed in Section 5.1, is essential if simplified procedures are to be used in practice. While solutions are known for a number of simple geometries, the range of solutions available is much less than for the stress intensity factor and limit loads solutions discussed in Section 2. Therefore, further work is required to generate accurate solutions, particularly for Q. Specifically, solutions are needed for surface cracks in plates under uniaxial and biaxial loading and in pressure vessels under pressure and bending.

The influence of constraint on material toughness has been discussed in Section 5.2 and the observations summarised in Section 5.2.3. There is a need to confirm these observations by analysing data on a wider range of materials, addressing both J and COD approaches. Some work of this nature has recently been reported for an A533B steel (Ref 76). Large-scale fracture behaviour was found to be sensitive to biaxiality ratio in the lower transition regime. A series of small-scale shallow-crack tests is proposed to enable interpretation of these large scale tests and hence to provide additional validation of the methodologies described above in Section 5.4.

It has been noted in Section 5.4 that the constraint methodologies have been developed for primary loading. For practical applications, it is necessary for methodology to address combined primary and secondary loadings. For such loadings, not only must the methodology be developed but methods for estimating the constraint parameters, T and Q, must also be provided.

For the local approach discussed in Section 5.3, it was noted that analysis of cleavage following ductile tearing had only recently been performed. This is a topic for which further development is required. More generally, the local approach methods need to become well defined procedures so that they can be used for critical assessments or for predicting the dependence of fracture toughness on constraint.

6. PRIOR OVERLOAD

Proof loading and Warm Pre-stressing (WPS) are two areas where prior overload conditions are deliberately imposed. Their effects on structural integrity are quite complex and have been the subject of extensive and continued research. This has demonstrated that a single overload (or proof load, or warm pre-stressing event) can offer significant benefits in the enhancement of the integrity of a structure (Refs 77, 78). The main benefits that have been reported are:

- arrest or retardation of fatigue crack growth;
- redistribution or relief of welding residual stresses; and
- an improvement in the subsequent low temperature defect tolerance of the structure.

In order to make use of these benefits, models have been developed which predict the changes in the behaviour of the structure. These models have been validated and built into assessment procedures such as PD6493 and R6 (Refs 1, 2). For example, PD6493 incorporates the reduction in residual stresses that can be produced by proof testing. More recently, R6 has included models which predict the warm pre-stress effect (Refs 79, 80).

In this section, the warm pre-stress approach in R6 is first briefly summarised in Section 6.1. Then, limitations to application of the approach are summarised in Section 6.2 and future work identified.

6.1 Warm Pre-stressing Methodology

In this section, the warm-prestressing methodology recently embodied in R6 is described. Application of the methodology is, however, only acceptable if certain conditions are satisfied. These are listed in Section 6.2 and used to identify areas for further work.

A warm prestress (WPS) is an initial pre-load applied to a ferritic structure containing a pre-existing flaw which is carried out at a temperature above the ductile-brittle transition temperature, and at a higher temperature or in a less-embrittled state than that corresponding to the subsequent service assessment. A WPS argument elevates the stress intensity factor at fracture, K_f, above the corresponding fracture toughness, K_{mat}, in the absence of the WPS, so that the fracture toughness used in eqn (1) is taken as K_f.

There are three types of cycle which are used in the laboratory to demonstrate the WPS effect (Figure 14). The temperatures at which the pre-load and re-load to failure occur are denoted by T_1 and T_2, respectively, in each case. Similarly, the stress intensity factors due to the pre-load and following the unload are denoted K_1, K_2, respectively.

(a) Load-Unload-Cool-Fracture (LUCF), where the structure is pre-loaded at temperature T_1 to stress intensity factor K_1, unloaded to stress intensity factor K_2, cooled to temperature T_2 and re-loaded to fracture.

(b) Load-Cool-Unload-Fracture (LCUF), where cooling to T_2 takes place prior to unloading and re-loading to fracture.

(c) Load-Cool-Fracture (LCF). This is similar to the LCUF cycle except that no unloading occurs prior to the imposition of extra load to fracture.

The greatest benefit in terms of maximising K_f is given by the LCF cycle, the least by the LUCF cycle with full unloading. For the latter cycle, the value of K_f is

$$K_f = K_2 + 0.2\,(K_1 - K_2) + 0.87\,K_{mat} \qquad (13)$$

provided $K_f - K_2 \leq K_1 - K_2$. Otherwise, it is conservative to assume $K_f = K_2$. Methods for reducing this conservatism and for treating other cycles may be found in (Refs 77-80).

6.2 Limitations of Methods and Further Work

For a WPS argument to be made according to the methods of Section 6.1, the following conditions must be met:

(i) the failure mechanism at the service condition must be by cleavage.

(ii) The flow properties of the material should increase between the WPS and the service failure condition; this may be due to a decrease in temperature or due to in-service hardening.

(iii) There should be no significant sub-critical crack growth between the WPS and the service failure condition. The amount of any such crack growth should be much less than the extent of the residual plastic zone following unloading.

(iv) The stress intensity factor K_1 due to the WPS loading exceeds the fracture toughness K_{mat} at the re-load condition.

(v) Small-scale yielding conditions hold, that is the plastic zone size at the pre-load is much less than the size of the uncracked ligament and any relevant structural dimensions.

(vi) The pre-load and re-load should be in the same direction; that is, both tensile or compressive at the crack tip. A compressive pre-load followed by a tensile re-load may reduce the apparent fracture toughness.

Further work is clearly required to reduce some of these limitations. Within SINTAP, item (iii) is being addressed by examining the effect of crack extension by
fatigue crack growth on the WPS behaviour of A533B steel. This work is also examining the in-service hardening requirements, item (ii), by using accelerated embrittlement techniques and comparing tests with a proof load and subsequent embrittlement with tests with embrittlement alone. The overall aim is to increase the level of confidence associated with the use of WPS models when repeat or multiple proof tests are involved (Refs 81, 82) and to allow reduction of limitation (iii) where sub-critical crack growth occurs (Ref 83).

7. CONCLUDING REMARKS

This review has addressed various aspects of defect assessment which are within the scope of SINTAP Task 2, failure of cracked components. For each topic, the available information has been briefly described and areas for further work have been identified. These areas for further work are summarised in Sections 2.1.4, 2.2.5, 3.2, 4.3, 5.6 and 6.2. Collectively, these sections outline the future work programme which is to be performed within Task 2. In addition, commencing in September 1997, the information arising from this work programme will be used to develop and verify defect assessment procedures.

Overall, it is concluded from this review that the work planned within Task 2 is well aligned to fill gaps in existing knowledge and to resolve remaining issues. The review will assist in refining the details of the work packages. If the future work is successful, it will lead to valuable developments of fracture mechanics methodology and assist in the production of a comprehensive defect assessment procedure within SINTAP.

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TABLE 1 - BASIC STRESS INTENSITY FACTOR SOLUTIONS NEEDED FOR ASSESSMENT PROCEDURE

Component Geometry	Defect Geometry	Loading Type	Work Needed
Flat plate, free edges	surface	Polynomial crack face stress	literature survey
	embedded	Polynomial crack face stress	literature survey plus development
	through-thickness	Polynomial crack face stress	development
Flat plate, restrained edges	surface	Polynomial crack face stress	literature survey
	embedded	Polynomial crack face stress	literature survey plus development
	through-thickness	Polynomial crack face stress	development
Cylinder	axial surface	Polynomial crack face stress	literature survey plus extension to cover rangeof radius/thickness ratios and defect aspect ratios
	axial through-thickness	Polynomial crack face stress	development to allow treatment of stress gradients
	circumferential surface	Polynomial crack face pressure	literature survey plus extension to cover range of radius/thickness ratios and defect aspect ratios
		External moment	literature survey
	circumferential through-thickness	Polynomial crack face pressure	development to allow treatment of stress gradients
		External moment	development

TABLE 2 - BASIC LIMIT LOAD SOLUTIONS NEEDED FOR ASSESSMENT PROCEDURE

Component Geometry	Defect Geometry	Loading Type	Work Needed
Flat plate, free edges	surface	External force and moment	literature survey plus advice on use of ‘local’ and ‘global’ solutions
	embedded	External force and moment	- " -
	through-thickness	external force and moment	literature survey
Flat plate, restrained edges	surface	external force and moment	literature survey plus advice on use of ‘local’ and ‘global’ solutions
	embedded	external force and moment	- " -
	through-thickness	external force and moment	literature survey
Cylinder	axial surface	internal pressure	literature survey
	axial through-thickness	internal pressure	literature survey
	circumferential surface	internal pressure, external force and moment	literature survey plus advice on use of ‘local’ and ‘global’ solutions.
	circumferential through-thickness	internal pressure, external force and moment	literature survey

TABLE 3 - CRACK OPENING AREA SOLUTIONS RECOMMENDED IN NEW APPENDIX 9 FOR R6

Geometry	**Loading**	**Elastic or Small Scale Yielding**		**Elastic-Plastic**
		Elastic Model	**Plasticity Model**	
Plates	Membrane	Westergaard (Ref 29)	Dugdale (Ref 31)	-
	TWB	Miller (Ref 30)	-	-
Spheres	Pressure	Wuthrich (Ref 31) $R/t \geq 10, \lambda \leq 5$	Dugdale (Ref 31)	-
	TWB	Miller (Ref 29)	-	-
Cylinders with Axial Cracks	Pressure	France et al (Ref 12) $5 \leq R/t \leq 100$	Dugdale (Ref 31)	-
	TWB	France et al (Ref 12) $5 \leq R/t \leq 100$	-	-
Cylinders with Circumferential Cracks	Membrane (Pressure)	France et al (Ref 12) $5 \leq R/t \leq 100$	Dugdale (Ref 31)	Langston (Ref 33) $5 \leq R/t \leq 20$
	Global Bending	France et al (Ref 12) $5 \leq R/t \leq 100$	Dugdale (Ref 31)	Langston (Ref 33) $5 \leq R/t \leq 20$
	Membrane + Global Bending	Add elastic components	Dugdale (Ref 31)	Kumar (Ref 32) $5 \leq R/t \leq 20$
	TWB	France et al (Ref 12) $5 \leq R/t \leq 100$	-	-

Figure 1 The leak-before-break diagram

Figure 2 **Recommended recharacterisation in R6 of defects at breakthrough for predominantly tensile loading**

Figure 3 **Recommended recharacterisation in R6 of defects at breakthrough for predominantly through-wall bending loading**

Figure 4 **Normalised T-stress solutions of eqn (5) for a semi-elliptical surface crack in a plate under uniform bending for various ratios of crack depth, a, to plate thickness, t, and to crack semi-length, c. Solutions are given as a function of angle Φ around the crack tip, where $\Phi = 0$ corresponds to the deepest point and $\Phi = \pi/2$ corresponds to the plate surface**

Figure 5 **Normalised Q-stress solutions of eqn (9) for three point bend specimens**

Figure 6 Critical value of J as a function of T/σ_y for 3PB and CCT specimens for a mild steel at -50°C (from Ref 52)

Figure 7 Critical value of J as a function of Q for 3PB and CCT specimens for a mild steel at -50°C (from Ref 53)

Figure 8 **Ductile toughness of an A710 steel at various crack extensions as a function of normalised T-stress (Ref 58)**

Figure 9 **Crack tip opening displacement of an A710 steel at various crack extensions as a function of normalised T-stress (Ref 58)**

Figure 10 **Ductile toughness of a A533B-1 steel plate at 20°C for a variety of specimens, all 20% side-grooved (Ref 59)**

Figure 11 **Data of Figure 10 plotted in terms of the constraint parameter $\beta_T L_r$ (= T/σ_y) of eqn (5) with curve fits to the data**

Figure 12 The R6 failure assessment diagram

Figure 13 Schematic of J_{ssy} Approach

Figure 14 Typical laboratory warm pre-stress cycles

APPENDIX 1

Review of Crack Shape Development

by

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