NUGENIA position on fracture mechanics assessment

Fracture Mechanics Assessment – The European view of the State of the Art

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POSITION PAPER

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FOREWORD

The NUGENIA position paper on fracture mechanics assessment is a living document issued by NUGENIA Technical Area 4 “System and Component Integrity”.

This NUGENIA position paper provides a state-of-the-art summary and describes open gaps of the specific technical field of fracture mechanics assessment that is in the scope of NUGENIA. The position paper is meant for an audience with a good knowledge on Gen II & III reactors, but without an in-depth knowledge of the specific technical field. Thus it provides a comprehensive state-of-the-art summary related to the regarded topic/position and comprehensive description of the open gaps of the dedicated technical field without being excessively detailed.

NUGENIA position papers clearly reference most recent projects on the dedicated technical field and are comprehensible without the referred documents. NUGENIA position papers are consensus documents, i.e. reflecting a common position of the “community” behind the document. The “community” does not only include the authors and contributors of the document, but in fact the whole technical area(s) from which the position paper in scope originates from.

20 March 2015

1 Introduction

Fracture mechanics is associated with describing the behaviour of crack-like defects in engineering systems, structures and components (SSCs) by way of defining the conditions (e.g. stresses and strains) local to the crack tip [1]. In an engineering sense, such conditions are usually defined by the elastic stress intensity factor parameter (K_I) or the elastic-plastic J-integral parameter (J). For a system, structure or component containing a known or postulated crack-like defect, knowledge of the geometry and stresses in the "cracked" region is used to evaluate K_I or knowledge of the geometry, stresses and tensile material properties is used to evaluate J. These structural parameters are then considered in conjunction with relevant fracture mechanics material properties in order to assess the behaviour and significance of the defect in a conservative manner. In terms of fracture process, a more advanced description of the process can be obtained by applying models in the micromechanical scale. They allow for a realistic prediction of the whole fracture process with initiation, crack growth and final fracture, but currently they are mainly used in the scope of research and development, because detailed material properties are needed and the analyses are more time consuming.

Structural integrity assessments, referred to in some countries as Engineering Assessment Methods (EAMs) or fast fracture analyses are an important part of management programs in Nuclear Power Plants (NPPs). EAMs, for example, may be required for such aspects as plant life and ageing management, maintenance and design changes. These assessments are generally one requirement for the effectiveness of periodic safety reviews. Issues that may need to be considered include definition of integrity assessment over a specific period or the whole life cycle, the various degradation mechanisms, ageing issues and safety margins.

The SSCs that need to be considered are generally those important for safety and availability, those that require high costs to replace and those that cannot be replaced without a significant long term refurbishment program.

Making a safety justification based on fracture mechanics typically involves defining an "initial" defect size, that has been detected and sized by Non-Destructive Examination (NDE) techniques (with error margins taken into account) or alternately that has been defined by knowledge of the detectable limits of NDE techniques that have been used on the plant component. That is to say that the "initial" defect is assumed to be of a size which could just have been missed being detected at the previous inspection. It may be noted though, that in practice, NDE detection and sizing of defects are complex issues currently open to much debate and research and development activities by experts within the field, specifically if complex geometries and/or material composites, like dissimilar materials are investigated. In undertaking a so-called fracture assessment, the critical, or limiting, defect size is obtained. This is defined as the maximum size of defect that can be tolerated in the component in the location being assessed. Based on knowledge of the operational cycles of the component, a fatigue crack growth calculation is undertaken to evaluate how large the "initial" defect is likely to grow over a given operational period. It is then required to demonstrate that the "grown" defect is of a size, significantly smaller (mainly by applying a specific safety factor) than that of the critical or limiting defect. In some circumstances, "backward" fatigue crack growth calculations from the critical or limiting crack size may be undertaken, for a defined operational period, in order to evaluate an "initial" defect size. This information may then be used in order to inform NDE requirements. That is to say that knowledge of what size of defect needs to be found may be helpful to the NDE planning process. Such a calculation can also be useful by way of comparing the evaluated "initial" defect size with the "limit of detection" size in order to hopefully demonstrate that the former is significantly larger than the latter.

Design codes such as ASME III [2] contain sections on fracture mechanics at the design stage and in fact most European countries use an ASME III Appendix G type of approach for a basic assessment of the avoidance of the risk of fast fracture in primary components such as the ferritic primary reactor pressure

vessels (RPVs) of NPPs. The method is based on a comparison of elastic K_I, calculated using a reference defect size with a reference value of material fracture toughness. Some European countries use the ASME III design code and others such as France and Germany have developed their own equivalent design codes, RCC-M [3] and KTA [55, 56] respectively.

ASME XI [5] and KTA [4] are primarily intended to address in-service issues of NPPs and as such contain various elements of fracture mechanics including taking plasticity effects into account. If the defects are crack like and have been originated during operation, they usually have to be repaired. Within Europe, fracture mechanics based procedures and codes have been developed which can be used for assessing the significance of defects found in-service. Usually they are indications, which have been originated during manufacturing and for which the crack growth during in-service is relatively small. Probably the most advanced of these procedures are the RSE-M methodology in France [6] and the UK R6 procedures [7], but also ASME XI [5] is applied for defect tolerance analyses and KTA [56] contains fracture mechanics methods in the ductile material regime. The difference compared to the design analyses is the character of the defect. At the design stage, the defect is a postulate based on the capabilities of the NDE, whereas the in-service analysis is usually the assessment of an indication, which has been detected by NDE during operation. In this case it has the character of a flaw tolerance analysis using the size of the indication and loading conditions during operation.

In considering the use of fracture mechanics in order to evaluate the structural integrity of plant components in NPPs, there are general aspects which are common to all metallic components. However, there are also specific aspects which relate to components like the RPV and to piping and associated components. These differences lie in the fact that some components (e.g. some piping components) are manufactured from a more ductile material (e.g. austenitic stainless steel) than others (e.g. RPVs in pressurized water reactors (PWRs) which are manufactured from ferritic steel), coupled with the fact that different types of loads and metal temperatures need to be considered for different components when considering safety justifications. Likewise, there are some common and some different aspects associated with (a) fracture assessments (to evaluate such parameters as critical crack size) and (b) fatigue crack growth assessments. These different aspects are covered in subsequent sections.

The recently completed EC Framework STYLE project [8] was focussed on various structural features found in piping and associated components. This position paper was primarily intended to report on the main findings of the STYLE project in relation to fracture mechanics assessment methodology. However, in compiling the document, the opportunity has been taken of including other aspects of fracture mechanics assessment in addition to the information resulting from the STYLE project.

Section 2 below deals with general aspects in terms of the current understanding and where further work is required. This is in relation to a general sense and specifically to fracture and to fatigue crack growth. Specific aspects relating to RPVs are covered in Section 3 and relating to piping and associated components in Section 4, the latter mainly relating to outcome of the STYLE project. Conclusions are outlined in Section 5, recommendations are given in Section 6 and acknowledgements in Section 7.

2 General aspects – Current understanding and where further work is required

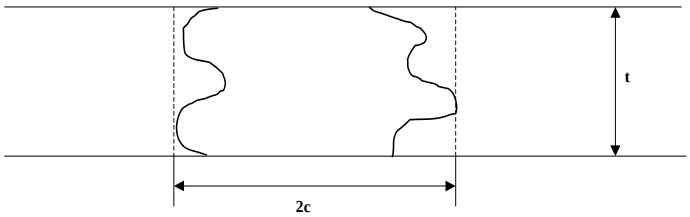
2.1 General

There are various aspects relating to fracture mechanics which are common to both fracture and fatigue crack growth assessments. Such main aspects considered here are associated with defect characterisation, stress intensity factor solutions, mixed mode loading, weld residual stresses and characterisation of loads. These are now dealt with in some detail.

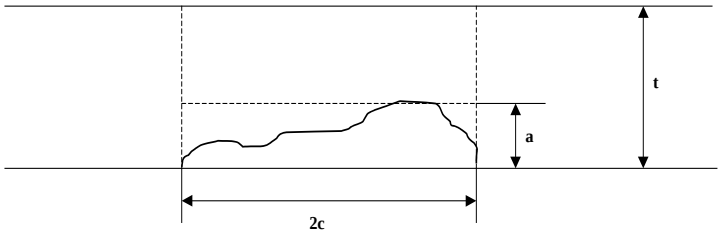
2.1.1 Defect characterisation

Defect characterisation is the term given to the process of modelling existing or postulated defects.

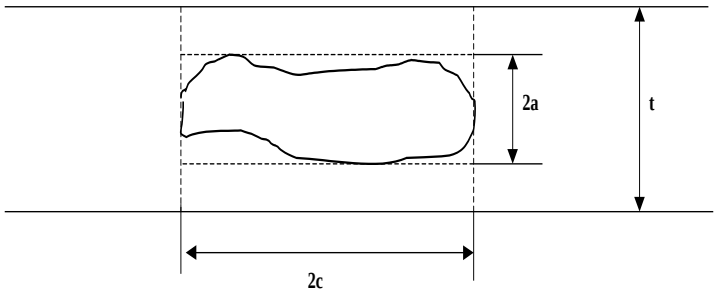
Postulated defects are in general of regular form, like elliptical, semi-elliptical or rectangular shape.



Through-thickness defect



Surface defect



Embedded defect

Figure 1: Flaw shape idealisations

Indications or existing defects can be of complex shape or being adjacently located. They have to be modelled by geometrically simpler ones more amenable to analysis. This process may involve the

merging of separate defects if the interaction between them is considered to be significant. Flaw re-characterisation may be a second stage of characterisation which can be undertaken after an assessment has failed to show that the defect is acceptable according to a particular code or procedure being adopted. For example, in some circumstances a submerged defect could be re-analysed as a surface defect, or a deep surface defect as a through-wall crack.

In general, defects to be characterised could be:

(a) a design specification, or reference defect;
(b) implied by the limits of NDE methods;
(c) discovered during fabrication or in service.

The defects in (a) and (b) are likely to be amenable to analysis and characterisation rules are therefore primarily relevant to (c).

In general, defects need to be represented by equivalent plane crack-like defects before they can be assessed (Figure 1). It is usual for defects to be projected on to one or more suitable reference planes, taking into account the geometry and the nominal stress in the absence of defects. Suitable reference planes could typically include:

- Plane through the defect – This may sometimes be difficult to apply in practice though because of mixed mode loading considerations (see Section 2.1.3).
- Principal planes – A simple option is to set the defect dimension used in the calculations to the measured values, irrespective of orientation, which is generally considered to be conservative. A more complex, and generally less conservative, option is to establish equivalent defect dimensions that can be defined by mathematical equations based on trigonometry.
- Plane normal to a free surface – It may be sometimes useful to choose a plane normal to the nearest free surface of the component but parallel to the major axis of the defect. Again, mixed mode loading may be a consideration.

Defects are usually classified as through-thickness, surface or embedded defects. Regardless of the classification, once both axes of the defect have been adequately characterised, the defect is circumscribed by a rectangle. This is the characterised defect for the through-thickness case. For surface and embedded defects, the rectangle is inscribed by a semi-ellipse and an ellipse respectively.

Different countries in Europe tend to apply different characterisation rules depending on the code or procedure being used. For example, while the UK R6 methodology provides guidance on various methods (as highlighted above), the French RSE-M code has characterisation rules that are generally based on the principal planes option.

The fact that there are various ways of defect characterisation is not generally considered to be a matter of any significant concern and it is likely that the different methods are roughly equivalent to one another anyway. Nevertheless, for plant life extension and ageing studies where fracture mechanics assessments are needed which are not overly-conservative, it would be useful to better understand the accuracies of the various defect characterisation methods. This would then usefully lead to unified European guidance being developed on this aspect.

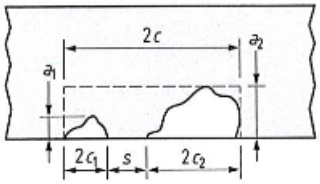
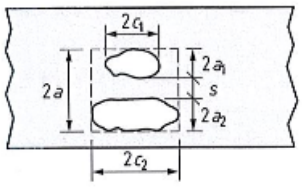
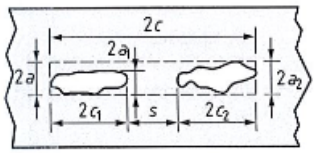
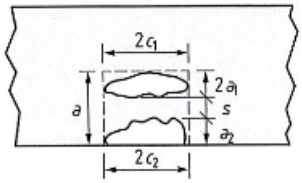
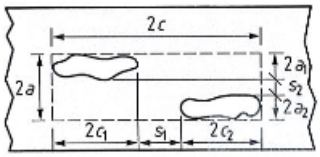
Schematic flaws	Criteria for interaction	Effective dimensions after interaction
i) Coplanar surface flaws 	$s \le 2c_1$ for a_1/c_1 or $a_2/c_2 > 1$ $s \le$ max. $\{0.5a_1, 0.5a_2\}$ for a_1/c_1 and $a_2/c_2 < 1$ $(c_1 < c_2)$	$a =$ max. $\{a_1, a_2\}$ $2c = 2c_1 + 2c_2 + s$
ii) Coplanar embedded flaws (interaction in thickness direction) 	$s \le a_1 + a_2$	$2a = 2a_1 + 2a_2 + s$ $2c =$ max. $\{2c_1, 2c_2\}$
iii) Coplanar embedded flaws (interaction in width direction) 	$s \le 2c_1$ for a_1/c_1 or $a_2/c_2 > 1$ $s \le$ max. $\{a_1, a_2\}$ for a_1/c_1 and $a_2/c_2 < 1$ $(c_1 < c_2)$	$2a =$ max. $\{2a_1, 2a_2\}$ $2c = 2c_1 + 2c_2 + s$
iv) Coplanar surface and embedded flaws (interaction in thickness direction) 	$s \le a_1 + a_2$	$a = 2a_1 + a_2 + s$ $2c =$ max. $\{2c_1, 2c_2\}$
v) Coplanar embedded flaws (interaction in thickness and width direction) 	$s_1 \le$ max. $\{a_1, a_2\}$ for a_1/c_1 and $a_2/c_2 < 1$ $s_1 < 2c_1$ for a_1/c_1 or $a_2/c_2 > 1$ and $s_2 \le a_1 + a_2$ $(c_1 < c_2)$	$2c = 2c_1 + 2c_2 + s_1$ $2a = 2a_1 + 2a_2 + s_2$

Figure 2: Flaw interaction rules for coplanar flaws

The various fracture mechanics based codes and procedures used within Europe contain re-characterisation rules for multiple defects. These rules generally emanate from those which were included in ASME XI [5] many years ago (Figure 2). In considering interaction effects, multiple defects are characterized by a single flaw for the purpose of assessment. For multiple surface flaws for example, the characterized flaw is typically of a semi-elliptical shape with the dimensions being the extreme dimensions of the original flaws, sometimes with a small additional margin. Work in recent years relating to the interaction of multiple flaws has mainly been undertaken by Japanese and UK researchers [9, 10]. The work in the UK has been undertaken primarily to address a potential non-conservatism in the R6 guidance [7] and in the BS7910 code [11] for re-characterizing twin touching defects in ferritic steel components operating on the lower shelf (not really relevant to NPP components). This work resulted in the R6 and BS7910 guidance for such cases being revised. Other than this particular issue, no significant concerns are considered to exist on the re-characterisation rules for multiple defects and the requirement to assess multiple defects lying in close proximity to each other is not thought to be great anyway.

2.1.2 Stress intensity factors

Stress intensity factor is a key parameter in fracture mechanics, particularly in the brittle and brittle to ductile regimes and for fatigue crack growth, and as such, most relevant codes and procedures contain sections or Appendices containing K_I solutions for cracks situated in various geometries and under various loading types.

The stress intensity factor (K_I) defines the amplitude of the crack-tip singularity in the stress field, perpendicular to the crack plane, obtained using linear elastic stress analysis methods. In general, K_I may vary with position around a crack front and this should be taken into consideration when undertaking assessment calculations. When assessing surface defects for example, it is fairly standard practice to evaluate K_I at the surface and deepest points of the defect.

Common geometries considered include, centre through-wall and edge cracks in plates, axial and circumferential through-wall and surface cracks in cylinders, through-wall and surface cracks in spherical vessels and embedded cracks in plates and cylinders. Traditionally, the types of loading the K_I solutions were available for were membrane and bending. This necessitated the stress fields (obtained for the un-cracked body) to be resolved into membrane and bending components. However nowadays, several codes and procedures (e.g. RSE-M [6] and R6 [7]) contain solutions presented in terms of weight or influence functions. Thus, K_I can be evaluated for arbitrary stress fields without the need to resolve the stress fields in to the two components. Polynomial fits to the stress field are usually required for such solutions.

K_I solutions are also available for geometrical features like cracks situated in pipe bends and cylinder nozzles. It should also be noted that in addition to the various codes and procedures, there are several stand alone compendia, e.g. [12, 13, 14] that contain K_I solutions for many types of geometries and loading. In addition, with increased computer power and increased reliance on computerised analysis methods and techniques, it is not uncommon for assessment engineers in the European nuclear industry to evaluate K_I solutions for specific geometry and loading cases by way of undertaking cracked body finite element analyses.

Although comparisons made of K_I solutions available in different codes and procedures for the same geometries and loads have highlighted some differences, there is generally no major concern regarding being able to evaluate this parameter with a reasonable degree of accuracy. The extension of the solutions available in the various codes and procedures to include more geometrical features, such as attachments and further nozzle cases would likely prove advantageous though.

2.1.3 Mixed mode loading

Stress intensity factor has been defined thus far as K_I. The "I" actually relates to the common case of mode I or opening mode of the crack. However, there are two other modes, and associated stress intensity factors which sometimes need to be taken into account in fracture mechanics assessment (Figure 3). These are the in-plane shear mode II (K_{II}) and the out-of-plane (or torsion) mode III (K_{III}). As inferred in Section 2.1.1, when characterising a defect into the principal planes, mixed mode loading will not be an issue. However, for the "plane through the defect" and "plane normal to a free surface" characterisation cases, mixed mode loading will be required to be taken into consideration. This is because shear stresses in the plane of the characterised defect (in the un-cracked body) are present which need to be taken account of. It may also be noted that component features, such as dissimilar metal welds (DMWs), containing complex stress fields, may require serious consideration of mixed mode loading effects.

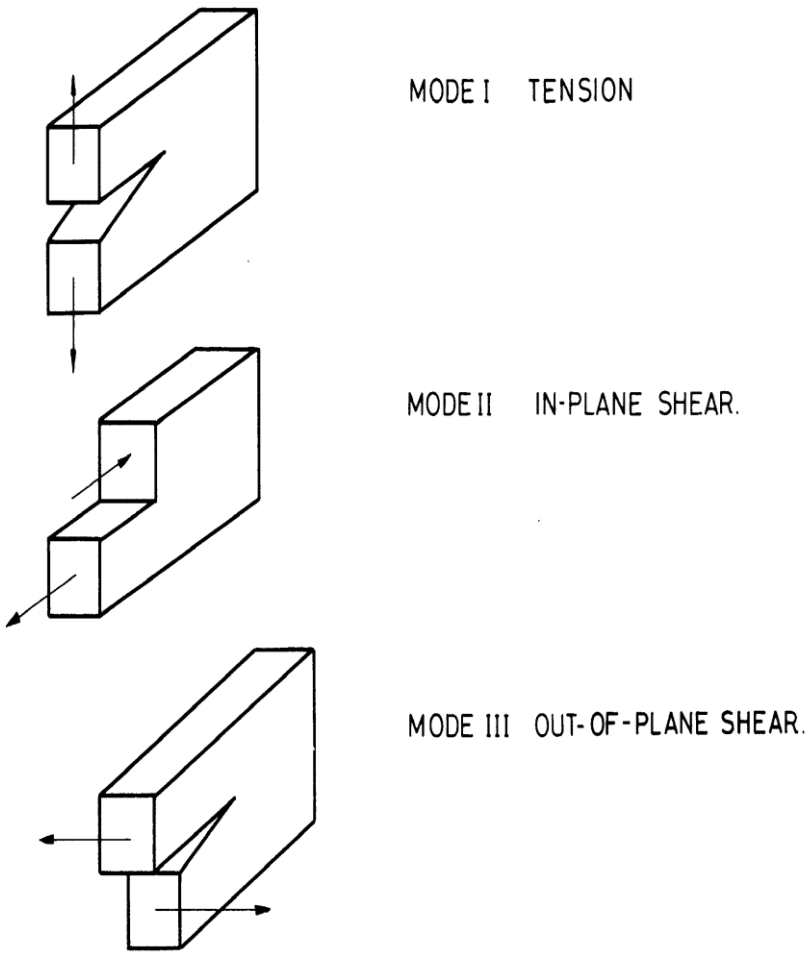


Figure 3: Definitions of loading modes

Some European codes and procedures (e.g. RSE-M [6] and R6 [7]) contain guidance on evaluating an effective stress intensity factor by way of combining the K_I, K_{II} and K_{III} components. This effective stress intensity factor is then used in the assessment being undertaken. The fracture mechanics related

properties (for the fracture or fatigue crack growth assessment being undertaken) are generally taken as the mode I values though since these are mainly the ones that will be available. Such values are considered to be conservative (i.e. lower fracture toughness in fracture assessments and higher crack growth rate in fatigue crack growth evaluations) compared to the mode II or mode III values.

The level of conservatism of using mode I parameters in fracture and fatigue crack growth assessments has not sufficiently been substantiated and particularly for plant life extension and ageing studies, it may be beneficial in some circumstances for advantage to be taken of using mode II and/or mode III properties. However, it should be borne in mind that there are no universally accepted methods of fracture toughness or fatigue crack growth testing other than those for mode I loading, although such testing has been the subject of several researchers e.g.[15, 16] over the years. This is therefore an area for future consideration with the ultimate aim of developing a unified European guidance on mixed mode loading, including the evaluation of fracture and fatigue crack growth material properties under mode II and mode III loading.

2.1.4 Weld residual stresses

Stresses resulting from welding processes are of a varying type and are self balancing throughout the structure or component. Consequently, they are categorised as secondary stresses (see Section 2.1.5). In welds with no post weld heat treatment (PWHT)such stresses can attain magnitudes approaching or exceeding the yield value (e.g. 0.2% proof stress) of the material,and they should be considered when assessing crack-like defects located in the tensile region (based on un-cracked body analysis). This is particularly true for situations where the applied primary stresses do not result in significant plasticity in the section containing the crack. For situations where there is significant yielding in the “cracked section”, the influence of the weld residual stresses will be substantially reduced due to plasticity effects and hence become of less importance.

The residual stress field in a welded structure can be characterised by components of stress in the weld longitudinal and transverse directions and the special variation of these components in the transverse and through-thickness directions. Because of the potential importance of weld residual stresses in structural integrity assessment of SCCs with no heat treatment or with a repair weld, a significant amount of work has been undertaken in recent years within Europe and elsewhere aimed at gaining a better understand of the magnitude and profile of such stresses for various types of geometries and weld types, usually aimed at specific applications. This work has included the use of destructive (e.g. sectioning ad slotting), non-destructive (e.g. neutron diffraction) and semi-destructive (e.g. deep hole drilling) experimental techniques carried out on mock-up weldments and also the use of finite element modelling of the welding process to various degrees of complexity.

Ideally, in fracture mechanics assessments, best estimate residual stresses evaluated by experiments, supplemented by FE modelling techniques, may be required, particularly for plant life extension and ageing studies for the reason that has previously been highlighted. However, it is fairly common practice in fracture mechanics assessments for simplistic and conservative assumptions to be made of weld residual stress profiles and values. The simplest assumption, but which in many cases can be excessively conservative, is to take the weld residual stress as a membrane value equal to the yield stress of the weld metal under consideration (0.2% stress for ferritic steels). A less conservative approach to this would be to use upper bound weld residual stress profiles such as those that are contained in the UK R6 [7] and BS7910 procedures [11]. These procedures contain, in both graphical and mathematical form, profiles for common weldments such as pipe butt and pipe seam welds, plate T-butt welds, pipe-butt welds, repair welds and pipe T-butt welds (e.g. Figure 4). These profiles, which are referenced to material yield stress, have been developed by fitting upper bound curves to all available experimental data. Unified European guidance on weld residual stresses could usefully be formulated. Ideally, such guidance would include consideration of the reliability of the methods and techniques that have been used to generate the

experimental data such that a statistical approach can be applied to remove outliers and move towards more realistic profiles being developed.

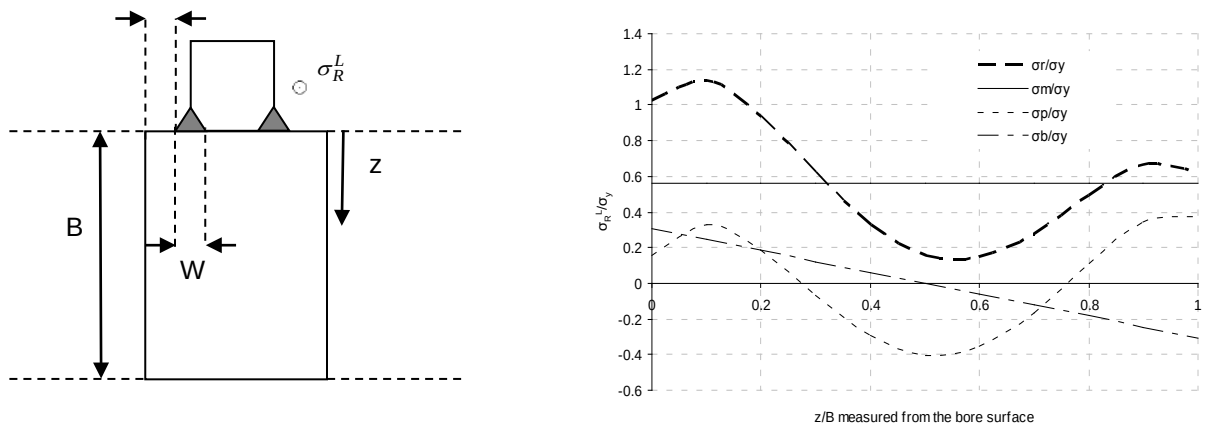


Figure 4: Typical upper bound residual stress distributions for tubular T-Butt Welds

These profiles, which are referenced to material yield stress, have been developed by fitting upper bound curves to all available experimental data. When new data (reported in the open literature or elsewhere) becomes available, they are compared with the recommended profiles which are re-evaluated if necessary to ensure that they still represent upper bound fits. It is recognised though that as more experimental data become available, the likelihood will be that the upper bound fits will tend more towards the membrane yield profile. There is thus the recognition, particularly from a European perspective that a more scientific approach is required in formulating general guidance on residual stress profiles. Ideally this would include consideration of the reliability of the methods and techniques that have been used to generate the experimental data such that a statistical approach can be applied to remove outliers and move towards more realistic profiles being developed.

The information provided so far in this sub-section has generally related to weldments that are in the as-welded condition and prior to start-of-life operation. However there are other aspects, including PWHT, Mechanical treatments and load history and temperature effects that should usefully be taken into consideration in developing future unified European guidance on weld residual stresses.

2.1.5 Characterisation of stresses

Stresses to be used in fracture mechanics assessments can generally be separated into primary and secondary categories. Primary stresses are such that they build the equilibrium to the external loads. Their essential feature is that at a very high (unacceptable) increase of the external loads after the cross section has become fully plastic, the deformation increases without self-limitation. Primary stresses differ between membrane and bending stresses according to the distribution in the load bearing cross section. Secondary stresses develop because of geometrical restraints, or of materials with different Young's modulus or restraint to thermal expansion. Their essential feature is that plastic deformations are self-limiting in cases when the flow stress is exceeded during equilibrating the deformation differences.

Primary stresses arise from loads which contribute to plastic collapse of the section containing the crack. Secondary stresses arise from loads which do not contribute to plastic collapse. Primary stresses are therefore more damaging than secondary stresses.

The categorisation of stresses into these two categories can be a matter of some judgement. Primary stresses are produced by applied external loads such as pressure, deadweight or interaction from other components. Thermal or other displacement induced stresses must often be classified as primary stresses if there is significant elastic follow-up (see below). These stresses will not in general be self-equilibrating.

Secondary stresses are generally produced as a result of internal mismatch caused by, for example, thermal gradients, welding processes and displacement discontinuities. These stresses are self-equilibrating, i.e. the net force and bending moment will be zero. Thermal and residual stresses which are self-equilibrating in the whole structure may not be self-equilibrating on the section containing the defect. Such stresses may therefore not necessarily be classifiable as secondary.

The principle of elastic follow-up, referred to above, originated from a model for creep deformation of a piping system where the possibility for self-springing being accompanied by excessive creep in localised regions of high stress was noted. In the context of creep stress relaxation, elastic follow-up is the argument that the elastic action of the least strained component of a structure restricts the stress in the most highly strained component from relaxing. The concept of elastic follow-up is closely related to the proximity of the structure to a load-controlled or displacement-controlled situation (i.e. the locality of the displacements). For a load controlled situation, elastic follow-up is very high and the corresponding stresses have a primary effect on failure of the structure, hence they should be classified as primary. In contrast, for a displacement controlled situation, elastic follow-up is insignificant and the corresponding stresses are classified as secondary. Identifying if elastic follow-up is present in a structure is not straightforward and in many practical situations lies between these two extremes. Although elastic follow-up is not generally considered to be relevant for nuclear light water reactors, there may be some situations where it is needed to be taken into consideration in defect assessments (e.g. or the case of stresses resulting from some pipework displacements and for some defects in the proximity of weldments).

Codes and procedures relating to fracture mechanics assessments contain rules and/or guidance based on the categorisation of stresses into primary and secondary. If it is difficult to judge in which of these categories particular loads lie, it is usual to assume primary to be on the conservative side. However, significant advantages can be gained by taking stresses as secondary, provided of course it is confidently known that this is the correct categorisation. It would therefore be advantageous for unified European guidance to be developed on characterisation of stresses and this could usefully include rules in order to establish the extent to which elastic follow-up is significant.

2.2 Fracture

2.2.1 Plastic collapse solutions

Consideration of plastic collapse is an important aspect for fracture assessment. In elastic-plastic fracture methodologies, such as RSE-M [6] and R6 [7], plastic collapse solutions are used in order to allow for plasticity effects. In some fracture methodologies (e.g. KTA [56]), plastic collapse solutions are solely used for assessing the defect tolerance of ductile materials, provided the material toughness is deemed to be of a sufficiently high value (ductile behaviour). In the KTA code for example, this toughness requirement is fulfilled if the specified or measured Charpy impact energy is higher than 45 J at the temperature under consideration. Plastic collapse solutions are sometimes referred to as plastic limit load solutions and they can readily be transposed to reference stress solutions, reference stress being the basis for the RSE-M and R6 methods. For the reasons outlined in Section 2.1.5, plastic collapse solutions are usually applied in fracture assessments only for primary stresses and hence the solutions are mainly for pressure and/or membrane and/or global bend loading.

The methods of plastic limit analysis are well developed and the upper and lower bound theorems can be used to estimate values of the limit load. The lower bound theorem, which is based on the balance of forces, leads to an underestimate of the limit load and is thus used in many situations to ensure

conservatism. Conversely, the upper bound theorem, based on balance of energy considerations, can lead to the limit load being overestimated.

There is a distinction between “global” and “local” plastic collapse (or limit load) solutions. “Global” solutions relate to overall collapse of the defective structure assuming elastic-perfectly plastic behaviour, whereas “local” solutions correspond to a yielding of a limited area surrounding the defect. As the ligament thickness tends to zero, the “local” limit load reduces and may tend to zero, depending on the defined yield area. However, failure of the ligament need not correspond to overall yielding since the component may be able to sustain a load equal to the limit load with a fully penetrating defect. This is the basis for the distinction between the so-called “local” and “global” limit loads for partial penetration flaws. The “local” limit load is less or equal to the “global” limit load. For through-wall defects, “global” limit load is clearly applicable. For partial penetration defects, “local” limit load is likely to be more applicable, particularly for assessing the deepest point of fairly deep cracks. When assessing the surface point of such a defect, it may be more appropriate to use the “global” limit load.

Plastic collapse (plastic limit load or reference stress) solutions are contained in fracture assessment procedures such as RSE-M [6], KTA [56], R6 [7] and BS7910 [11]. These, particularly the latter two, tend to be a mixture of “local” and “global” solutions.

For defects located in strength mis-match areas such as dissimilar metal welded (DMW) regions, the pattern of plastic deformation in the neighbourhood of a section containing the defect is likely to be different to that for defects located in non strength mis-match areas. This has led to so-called “mis-match” limit load solutions being developed in recent years, some of which are available in the R6 methodology for geometries which may be idealised as containing two regions with different yield stresses.

Although various codes, methodologies and procedures contain plastic collapse (plastic limit load or reference stress) solutions for various geometries containing cracks, they can result in significantly different values, particularly in relation as to whether they are based on “local” or “global” considerations. It would thus be advantageous and timely if unified European guidance were to be developed on this aspect. It is envisaged that the main aspects that could usefully be addressed in such guidance would include: distinct recommended solutions for particular geometries and loading (including consideration of “global” or “local” solutions and the position on the crack front under consideration), extending current available solutions to more complex geometries (nozzles, attachments bends etc.), extending solutions for combined loading (e.g. membrane plus global bending) and the extension of current mis-match solutions.

2.2.2 Allowance for ductile tearing

Fracture assessments for structural components operating in the ductile regime are conventionally, at least in the first instance, based on initiation (of tearing) fracture toughness values. However, in some circumstances, benefit may be taken of the fact that in reality, a certain amount of stable ductile tearing will occur prior to final failure. This type of analysis is evaluated by reference to the detailed ductile tearing resistance curve obtained from testing on fracture toughness specimens like compact-tension (CT) or three-point bend (3P-B) specimens. This curve is commonly referred to as the J-resistance or J-R curve. In essence, allowing for tearing provides an “apparent” increase in the value of fracture toughness used in an assessment. In some instances, such an increase could typically be the fracture toughness value from the J-R curve corresponding to a small amount of ductile tearing (say 2 mm or 3 mm). This would generally not be considered though for normal operating conditions but rather for upset or accident conditions which can be construed as one off applications. A more detailed and precise way of taking ductile tearing into account is to undertake a full tearing analysis. When evaluating critical or limiting crack size for example, this involves constructing a figure of structural crack driving force, J, plotted against crack size (Figure 5). The J-resistance curve (material J versus amounts of ductile tearing) is then superimposed on this figure. The position of this latter curve is adjusted until it becomes partially tangential to the J versus crack size curve. The tangency point is defined as the instability point from which the critical or limiting crack size, based on instability rather than initiation of tearing, can be

established. Such a full tearing analysis may be typically undertaken in order to establish likely margins in initiation of tearing based assessments. Strictly speaking, a full tearing analysis should also be carried out when invoking an increased fracture toughness value, as described above, in order to ensure that the conditions being considered are below those of instability.

Methodologies like R6 [7] contain guidance on the mechanics of undertaking tearing analyses. Unified guidance on the general mechanics of undertaking such analyses should not be difficult to achieve, the only real issue being the method of evaluating the structural J parameter. The development of unified European guidance on such aspects as how much tearing can be considered and under which loading levels may be much more difficult however because of transferability issues of materials properties measured from specimens and being used for component assessment and of the differing regulatory views and requirements in the different countries.

One particular aspect of tearing analyses where unified guidance would be beneficial is the degree to which a J-R curve can be extended beyond the J-controlled limits as specified by the materials fracture toughness testing codes, bearing in mind that for many practical materials, such limits can be confined to very small ductile crack growth values (of the order of 1 or 2 mm or less in some cases) depending on the toughness of the material and the specimen size. Details on the form the J-R curve should take beyond the J-controlled limit regime should form an integral part of such guidance. Such guidance could also usefully include information on when moving to a tearing analysis would likely be beneficial. This could include such conditions as when the fracture toughness for small amounts of ductile tearing is significantly steeper than the initiation toughness or when the component and defect dimensions, such as crack size, section thickness and remaining ligament, are much greater than the amount of ductile tearing being considered. In considering the materials fracture toughness codes, it is also worth noting that there are actually differences in how fracture toughness initiation is defined. For example the ESIS P2-92 [17] method defines initiation as the fracture toughness corresponding to 0,2 mm of tearing whereas ASTM E1820-11 [18] specifies a construction based on an offset line. As can be seen in Section 4.4.3, these different definitions can result in quite different assessment results and unified guidance on this aspect would also be advantageous.

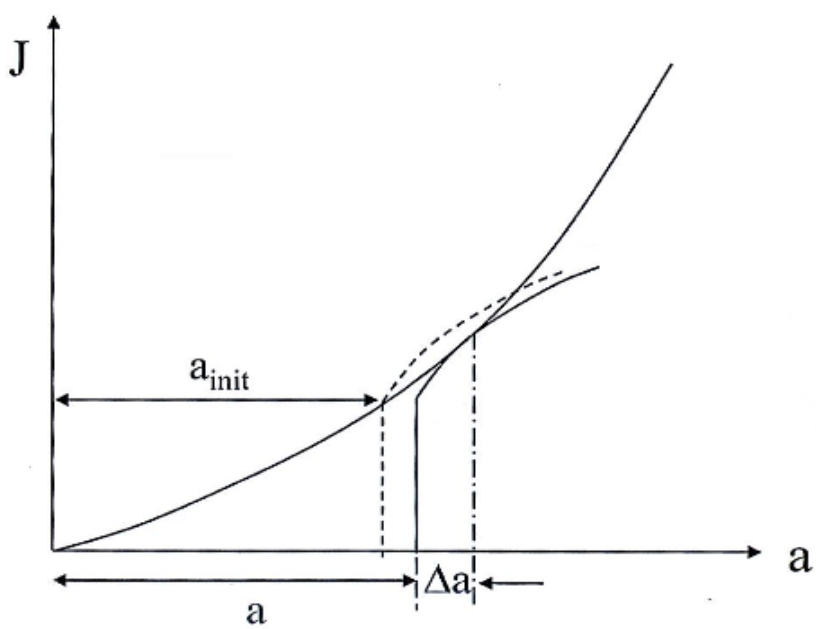


Figure 5: Tearing (instability) analysis for constant applied stresses

2.2.3 Benefits of crack-tip constraint considerations

The enhanced understanding of crack-tip constraint effects and the potential to be able to realise its benefit in structural integrity assessments is probably the most significant development in the field of fracture mechanics in recent years. This arises from the fact that a particular conservatism inherent in conventional fracture mechanics assessments is that the value of fracture toughness used is normally derived from deeply cracked bend specimens using recommended testing standards and validity criteria. These are designed to ensure plane strain conditions and high hydrostatic stresses (i.e. high stress triaxiality) near the crack tip to provide a material property independent of specimen size and geometry. However, it is known that the material resistance to fracture in the brittle to transition regime is increased when specimens with shallow cracks, or specimens in tension, are tested. These conditions lead to lower hydrostatic stresses (i.e. low stress triaxiality) at the crack tip, referred to as lower constraint.

In recent years, there has been considerable effort to quantify the geometry dependence of the material resistance to fracture in the brittle to ductile regime using crack-tip constraint parameters. This has led to proposals for incorporating constraint in fracture assessments. An elastic parameter commonly used to characterise crack-tip constraint is the T-stress which is actually the second order, which can be regarded as the stress parallel to the crack faces, in the expression defining the close to crack tip elastic stresses. The value of T is influenced by remote stresses parallel to the crack as well as geometry, crack size and loading. The value of T may be calculated from elastic finite element analysis using a number of different methods. In addition, there are T-stress solutions available in the literature for a range of two and three dimensional geometries and loading. A commonly used elastic-plastic parameter, Q, is defined as the normalised difference between the actual stress field close to a crack tip and the stress field close to a crack tip under small-scale yielding conditions with T=0. The Q-parameter actually varies slightly with normalised distance $r/(J/\sigma_y)$ and it is commonly adopted to determine the Q-parameter at the normalised distance $r/(J/\sigma_y) = 2$, where σ_y is yield stress. In general, the value of Q is a function of geometry, crack size, type of loading, the material stress-strain curve and the magnitude of the loading. The evaluation of Q necessitates rather detailed elastic-plastic finite element evaluations.

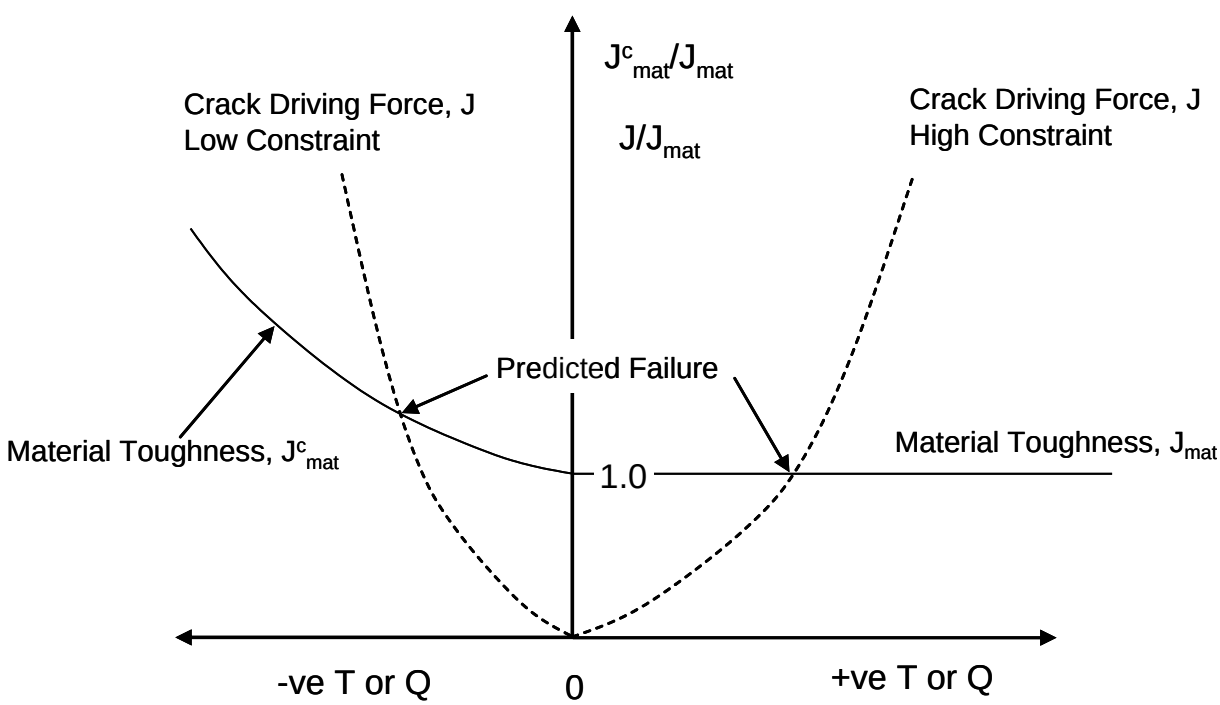


Figure 6: Fracture toughness and crack driving force in J-Q space

The philosophy behind the application of crack-tip constraint methodology in the brittle to ductile regime can perhaps best be understood in diagrammatic form by plotting fracture toughness J against the T or Q parameter (Figure 6 – where J modified for crack-tip constraint, J^C_{mat} is shown normalised by the conventional plane strain fracture toughness, J_{mat}). This diagram defines the "failure" curve. There is generally an upswing in the toughness as Q (and T) becomes more negative, the more negative the Q (or T), the lower the crack-tip constraint. Values of fracture toughness corresponding to Q (or T) equal to zero or positive values represent the high crack-tip constraint regime which is relevant to conventional toughness, as evaluated from deeply cracked bend specimens. Structural J loci can be constructed on the diagram so that the "failure" point can be evaluated, as that point being where the "failure" curve is attained. Certainly for primary loads, cracks (e.g. fairly shallow surface cracks under predominantly membrane loading) to be assessed in structural components are often of low crack-tip constraint (i.e. negative Q or T) and hence the "structural" fracture toughness is likely to be greater, significantly so in some cases, than the conventional fracture toughness. When there is a combination of primary and secondary stresses, there could be a tendency for the crack-tip constraint in structural components to be higher (i.e. less negative Q or T) than for primary stresses acting alone. One of the reasons for this is the fact that secondary stresses are predominantly more bending than membrane in nature.

In order to evaluate the "failure" curve defined above, it would usually be required for three sets of fracture toughness tests to be undertaken, one set for the conventional high constraint type of specimen, one set for low crack-tip constraint and one set for an intermediate level of crack-tip constraint. In practice though, rather than using the construction referred to above, it is not uncommon for fracture toughness to be evaluated for the level of crack-tip constraint corresponding to that of the "cracked" structural component being assessed by way of the Q or T parameter. It may also be noted that construction of the "failure" curve may be achieved by a combination of fracture toughness testing and local approach finite element modelling. While this can reduce the number of fracture toughness testing sets, it may be necessary for significant effort to be put into the modelling, particularly with regard to calibration aspects.

In terms of potential benefits in fracture assessments by the consideration of crack-tip constraint, the elevation of fracture toughness under negative Q or T has been shown to be significant for ferritic steels in the cleavage and ductile to brittle transition regimes. For ferritic and austenitic materials operating on the upper shelf, it has been shown that there can be a general enhancement in the slope of the J-Resistance curve under low (compared to high) crack-tip constraint conditions. The influence of crack-tip constraint on initiation of ductile tearing is less clear however and this may well vary from material to material.

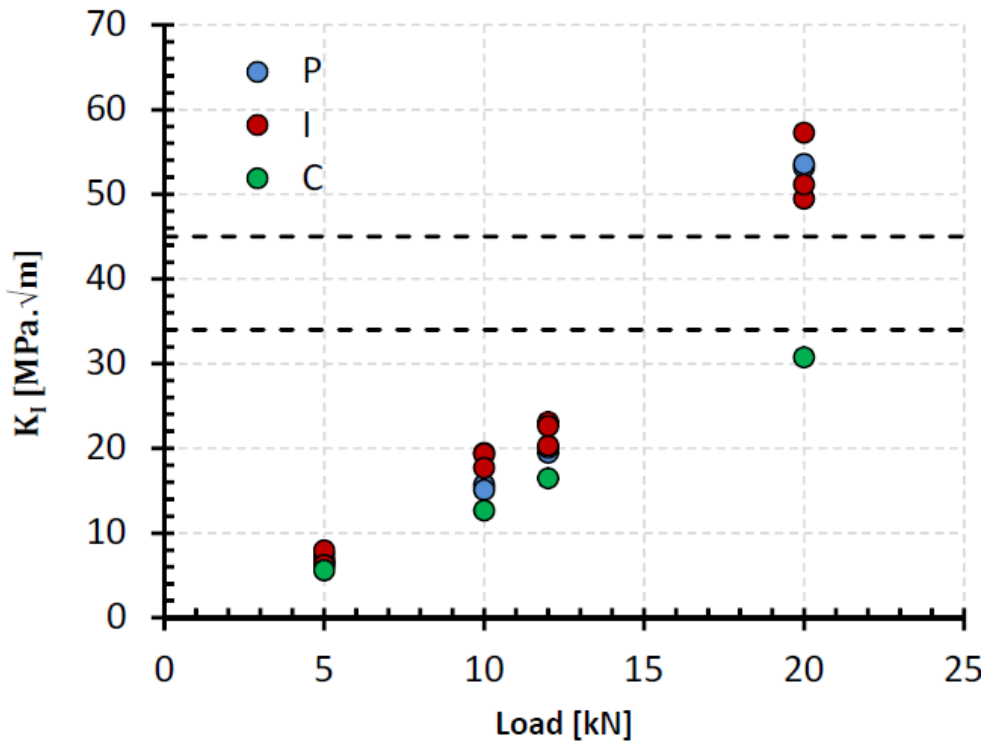
As has already been explained, there has been significant work undertaken in recent years relating to crack-tip constraint aspects and many countries within the EU and indeed throughout the world have applied such methodology albeit mainly, but not exclusively, in R&D projects and studies. The consideration of crack-tip constraint provides a powerful tool for assessing structural components by way of utilising the "real" fracture toughness of the component as opposed to the "lower bound" conventionally used. Perhaps equally as important, it enables margins to be established of parameters (e.g. limiting or critical crack size) evaluated by assessment methods based on conventional fracture toughness values. One area requiring further development leading to unified guidance is a testing procedure for evaluating fracture toughness under low crack-tip constraint conditions (e.g. shallow cracked bend specimens) in the ductile regime. In fact, unified detailed guidance could usefully be developed on constraint based methodology in general, particularly with a view to ensuring that the "failure" curve, as described above, is constructed so as to be unique for a given material and temperature irrespective of whether the loading in the structural component is of primary stress, secondary stress or a combination of the two, the latter being the case in the vast majority of practical situations.

2.2.4 Treatment of combined primary and secondary stresses (including stress relaxation effects

Section 2.1.5 is associated with the characterisation of stresses into primary and secondary components. As has been noted in that section, primary stresses are potentially more damaging than secondary

stresses and this is equally true when considering fracture. This aspect is reflected in elastic fracture mechanics based codes such as ASME III (Section G) [2] and KTA [55] which require the value of the stress intensity factor for primary loads to be multiplied by a factor, depending on the load category before being added to the value of the stress intensity factor for secondary stresses and compared with the reference fracture toughness.

In elastic-plastic fracture mechanics methodologies like R6 and RSE-M, it is recognised that when structures are loaded by a combination of primary and secondary stresses, plasticity effects occur which can modify the influence that the secondary stress has on fracture. Therefore, in such methodologies, the crack driving force corresponding to the secondary stress is effectively multiplied by a factor (denoted as V in R6 for example) and then added to the crack driving force corresponding to the primary stress, the resultant value being compared against fracture toughness. In R6 for example, for situations where the sum of the primary and secondary stresses does not significantly exceed yield, the factor will be approximately 1.0. The factor will be greater than 1.0 for situations where the plastic zone resulting from the primary stresses corresponds to the small-scale plasticity regime (i.e. prior to gross section yielding). Following gross section yielding, the factor will reduce and fall below 1.0. The actual value of the factor, in practice, will depend on the magnitudes of the primary and secondary stresses, particularly in relation to one another.



P – Secondary stress introduced before primary stress

I – Secondary stress introduced after primary stress

C – Primary stress alone

Figure 7: Example of load order effects on crack driving force

Whilst further developments in the treatment of combined primary and secondary stresses tend to be specific to individual national codes and procedures, results of supporting analytical (mainly finite element analysis) and experimental studies could usefully be shared in a unified way across EU organisations, particularly for validation purposes for their own developments in this area. One aspect where further

development work could usefully be undertaken and unified guidance provided is on load order effects where recent work has indicated how crack driving force for combined loading can differ depending as to whether the primary load is applied before or after the secondary load (Figure 7). It has already been stated in Section 2.1.5 that it would be advantageous for unified European guidance to be developed on characterisation of stresses. In this regard it has been suggested that it would be useful to include rules in order to be able to establish the extent to which elastic follow-up is significant. The extent to which the factor referred to above is greater than 1.0 would then be able to be determined. It therefore follows that fracture assessments would potentially benefit by way of methodologies that are able to take elastic follow-up in to account in the treatment of combined primary and secondary stresses. Some initial studies have in fact been performed recently on this aspect with a view to proposing modifications to R6 [19]. It should be again noted though, that elastic follow-up is not generally considered to be relevant for nuclear light water reactors but there may be some situations where it is needed to be taken into consideration in defect assessments.

2.2.5 Treatment of non crack-like defects

Fracture assessment codes and methodologies generally provide guidance on the assessments of defects or flaws that are assumed to be infinitely sharp. In many cases, such as cracks formed by the fatigue mechanism, the assumption that the defect tip is infinitely sharp is appropriate. However, this assumption is inherently pessimistic for flaws that do not have sharp tips such as lack of fusion, porosity or mechanical damage. Furthermore, some design features, such as crevices in tube to tube plate assemblies are usually treated as crack-like defects in assessments which could be excessively pessimistic (i.e. conservative).

Over the last 20 years, several approaches have been proposed to assess the fracture resistance of components containing non-sharp defects. This includes guidance that was developed for the European-wide FITNET procedure. More recently, a further study has been initiated in the UK associated with R6 developments [20]. Such developments could usefully be extended to the European level. It is realised though that applying such a developed procedure will necessitate fairly accurate information on the flaw(s) being assessed, particular with regards to radius of the “flaw tip”. For design features such as crevices, this should not be problematic in general. However, for the other types of non-sharp defects referred to above, developments in NDE methods will inevitably be required before the procedures can be used with full confidence.

2.3 Fatigue crack growth

2.3.1 Counting, combining and applying transient cycles

Sub-critical growth by fatigue of postulated or known cracks has to be taken into account when assessing the integrity of components. For each design of nuclear plant component, fatigue crack growth is generally small since the load cycles which cause the fatigue crack growth loading are generally limited. Environmental conditions are also designed in such a way that the contribution of environmentally assisted crack growth is limited.

Fatigue crack growth calculations involve the stress intensity factor range (ΔK) to be determined for use in the Paris law based equation. For plant components with simplistic loading and applied cycles, such as a pressurised vessel with no thermal loading whereby the pressure ramps from zero to a particular value at regular intervals, ΔK is reasonably straight forward to evaluate. However, in practice, when undertaking fatigue crack growth evaluations for nuclear plant components, all relevant transients need to be taken into account, thus requiring variable amplitude loading considerations. For variable amplitude loading, the stress spectrum is required to be converted to identifiable stress ranges. The stress spectrum is then represented as a distribution of stress ranges versus numbers of occurrences. When various transients are required to be taken into account, these may need to be considered together when evaluating the

stress ranges, rather than completely separately, unless the order in which they are applied is known with a high degree of confidence.

A conservative way of identifying stress ranges in such situations could be to firstly establish the maximum stress (most tensile) and minimum stress (most compressive) for each transient. The highest (most tensile) stress can then be paired with the lowest (most compressive) stress, irrespective of which transients each one of them (i.e. maximum and minimum) resides in and the maximum stress range (and ultimately ΔK) evaluated. This exercise can then be repeated by paring the next highest stress with the next lowest stress and so on. The resulting information will then be a list of stress ranges versus numbers of occurrences. Unless the maximum/minimum stress pairings reside in the same transient, it is likely that the number of occurrences do not match. If this is the case and if the number of occurrences for the transient in which the low stress resides is less than that for the transient in which the high stress resides, then (when the actual pairings have been completed) the high stress can be paired with the next lowest stress. Conversely, the low stress (when the actual pairings have been completed) can be paired with the next highest stress if the number of occurrences for the transient in which the low stress resides is greater than those for the transient in which the high stress resides.

The position on the structural component where the stresses are located in order to establish the stress ranges can be open to judgement. If a surface crack is being assessed at the inner wall of a vessel for example, it may seem logical to consider a relevant location on the inner wall (for the un-cracked body). Likewise, if a crack is being assessed at the outer wall, a relevant location on the outer wall could be considered. For relatively shallow cracks, this scenario would seem reasonable but for deeper cracks, and depending on the through-wall stress distribution, the approach may not necessarily be conservative. Of course, strictly speaking, it should be the range of stress intensity factor, rather than the range of stress that should be directly obtained from the stress analyses (usually by finite element modelling) of the component. However, this would require stress intensity factor to be evaluated at the surface and deepest points (for a semi-elliptical surface crack) of the crack from the through-wall stress distribution for all transients, various transient times and for various crack sizes and possibly various crack aspect ratios. This would require significant computing and data management, hence more simplistic methods such as that suggested above, are likely to be considered in most cases. Although the stress ranges could be obtained by considering the stresses at a particular location, it is of course important to evaluate the corresponding stress intensity factor ranges by considering the actual through-wall stress distributions.

Once the stress and stress intensity factor ranges versus numbers of occurrences have been established, there is then the question of the order in which they should be applied in the fatigue crack growth calculations. One way might be to split the information into blocks, each block corresponding to 1 year of operation for example. Each block could then be taken in turn starting with the highest stress range followed by the next highest and so on. However there are various ways of undertaking such calculations. Strictly speaking, in order to ensure conservatism, a sensitivity analysis could be undertaken of ordering the cycles in various ways in order to assess the significance on the results of the different approaches.

Clearly, there is much scope for unified European guidance to be developed on fatigue crack growth evaluations with regard to the aspects discussed above.

2.3.2 Allowing for crack closure effects

As noted in Section 2.3.1, fatigue crack growth calculations involve the stress intensity factor range (ΔK) to be determined for use in the Paris law based equation. ΔK, by way of stress range for example, is of course evaluated by subtracting the minimum stress intensity factor from the maximum stress intensity factor. When part or whole of the ΔK cycle has a compressive element, there might be crack closure effects which mean that taking the full range is likely to be conservative and excessively so in cases where such an element is significant. Various modifications to the Paris Law relationship have been proposed and utilised in assessments over the years in order to allow for crack closure effects. These modifications

usually incorporate the so-called R-ratio where R is defined as minimum stress intensity factor divided by maximum stress intensity factor. However, although there is guidance on this aspect in some of the assessment codes and procedures, there appears to be no unified agreed modification to the Paris Law relationship in order to satisfactorily allow for crack closure effects. Further work and consideration of this aspect could therefore usefully be undertaken at the European level.

2.3.3 Low and high ΔK regimes

The Paris Law relationship is the linear mid-region of the da/dN (crack growth per cycle) versus ΔK relationship when plotted on a log-log scale (Figure 8). There are two other regions though which sometimes need to be considered when undertaking fatigue crack growth calculations.

One of these is the threshold region which represents crack growth rates typically below 10^{-8}m/cycle. Such crack growth is known to be complex, since it depends upon mechanical, metallurgical and environmental factors. On account of the very low propagation rates possible at low ΔK, total crack propagation life is often dominated by the proportion of life spent in this regime. Where the applied ΔK is less than the threshold value, crack growth rates are effectively zero. Threshold ΔK data for a wide variety of mild and low alloy steels have been collated in the literature.

The other regime is that describing fatigue crack growth near final failure. For ductile materials, this region is associated with when the elastic-plastic driving force (represented by J for example) corresponding to the maximum applied stress in the cycle approaches the initiation fracture toughness. For situations where the driving force exceeds the initiation fracture toughness, then crack growth by the mechanism of combined fatigue and tearing needs to be considered. Based on the results of experimental data, the R6 procedure [7] for example proposes that this may satisfactorily be achieved by a model involving linear summation of tearing and fatigue crack growth.

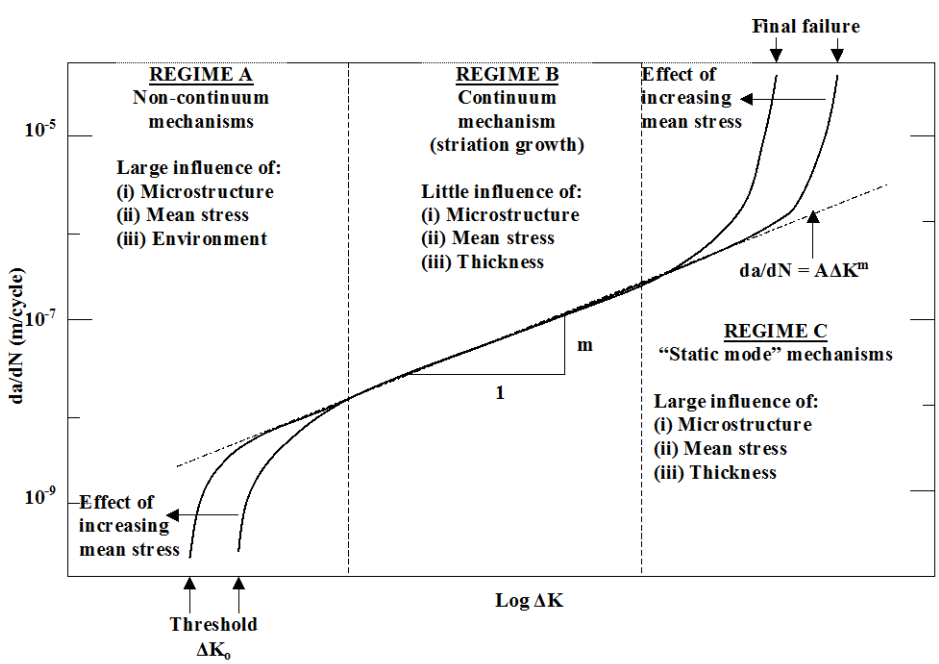


Figure 8: Sigmoidal variation of fatigue crack growth rate (da/dN) with stress intensity factor range (ΔK) and associated fracture mechanisms

It is worth mentioning that a number of crack growth laws have been proposed in the literature to describe the entire da/dN versus ΔK relationship, from the threshold region through to the "Paris Law" region and through to the near final failure region. One of these is the so-called NASGRO equation [21].

Any European-wide guidance on fatigue crack growth, as highlighted in Sections 2.3.2 and 2.3.3, could therefore usefully include consideration of the threshold and near final failure regions.

3 Aspects relating to RPVs – Current understanding and where further work is required

3.1 General aspects

The reactor cores of the majority of the worlds' nuclear power reactors are housed in steel reactor pressure vessels (RPVs), whose primary function is to contain both the reactor core and the primary coolant under operational conditions of temperature and pressure. The integrity of the thick walled steel RPV is vital for safety for two main reasons. Firstly, because the RPV is the container for the reactor core, if it should develop a leak, and if the rate of coolant loss exceeds the maximum capability of the emergency cooling water system to replace it, then the reactor core could become uncovered, overheat and unless some remedial action is undertaken, ultimately melt. Secondly, a massive failure of the RPV itself could both seriously damage the reactor core and, at the same time, could damage the containment building. In this way a single postulated event could possibly outflank the various sequential barriers which prevent the escape of fission products in other postulated accident sequences, namely the fuel cladding, the primary circuit and possibly the containment building. Therefore it is necessary to demonstrate that disruptive failure of the RPV is practically excluded and has a very low probability of occurrence, bordering on the incredible, throughout its working life, since such accidents could give rise to large uncontrolled releases of radioactivity to the public.

Component integrity is usually considered in terms of: the vessel beltline (which receives the highest irradiation dose), cladding and nozzles / penetrations. In considering long life operation of RPVs, one of the main aspects required to be carefully taken into account is the extent to which neutron irradiation causes a shift in the ductile to brittle transition temperature resulting in the material becoming more brittle. Fracture mechanics assessment therefore inevitably plays a very important role in assuring the safety of RPVs [22].

3.2 Pressurised thermal shock analysis

Pressurised Thermal Shock (PTS) analysis is a key part of the RPV structural integrity assessment. PTS is a reactor transient that subjects the vessel to severe thermal shock starting at normal operating loading. Initial sharp cracks are typically postulated to be located at the weld seam area on the inner surface of the vessel. Safety margins against brittle fracture may be justified in an appropriate manner.

Firstly, the most significant transients are selected. Thermal hydraulic analyses are employed for calculating the thermal loading transients. The fluid temperature distribution and the corresponding heat transfer coefficients are needed for the thermal conductivity analyses. Secondly, pressure and thermal stresses through the wall are calculated in selected transient cases. Also the effect of residual stresses in the cladding, under the cladding (see Section 3.5) and in the welds may be evaluated and taken in to account. The location, size and shape of the postulated crack can be assumed based on the fabrication, NDE (non destructive evaluation), previous ISI (in-service inspection) or conventional values. Fracture mechanics analyses are applied for assessing the crack initiation.

In temperature versus fracture toughness and crack driving force (K or J) space, the assessment comprises constructing a loading or crack driving force curve starting at the temperature corresponding to normal operation then as the temperature reduces due to the PTS, the crack driving force increases to a peak value before finally reducing (Figure 9). This curve is then compared with that of material resistance (fracture toughness curve), representing the material ductile to brittle transition curve. Such a curve can

be provided in codes such as ASME being indexed with a reference temperature based on the RT_{NDT} or a fracture toughness based reference temperature. Alternatively a direct measured fracture toughness curve, like the so-called Master Curve can be used, which is based on relevant experimental fracture toughness data.

The Master Curve approach provides a basis for fixing the position of the fracture toughness versus temperature curve by a series of tests which may be carried out at one temperature or temperature distributed to determine a reference temperature. The form of the toughness versus temperature curve is then based on the Master Curve equation, which is an exponential in the difference between the temperature and the reference temperature. However, where assessments are required over a range of temperatures, it is preferable to obtain fracture toughness data over that temperature range. For homogeneous steels such as parent material and welds in the post-weld heat treated (PWHT) state, statistical analysis of the data may be based on the Master Curve method described in ASTM E1921 [23]. For inhomogeneous material, such as weld metal and heat affected zones (HAZs) in the as welded or partially heat treated condition, the Master Curve method based on the European FITNET procedure [24] (now incorporated in BS7910 [11]) may be used. The FITNET procedure, which is based on maximum likelihood is known as the MML method and involves three stages in the analysis of fracture toughness data. The first stage (stage 1) is identical to that described in ASTM E1921. The second stage (stage 2) helps to identify whether the data set is scattered by providing alower tail MML estimation. If the estimates of fracture toughness from the two stages are the same, then the data may be considered to be homogeneous and after applying a size adjustment, the appropriate fracture toughness is calculated. If they are different, then the result from either stage 2, which is normally lower than stage 1 or, depending on the number of tests, stage 3 is used. Stage 3 performs a minimum value estimation to check and makes allowance for outliers, possibly due to gross inhomogeneities in the material. It incorporates an additional safety factor when the number of tests is small.

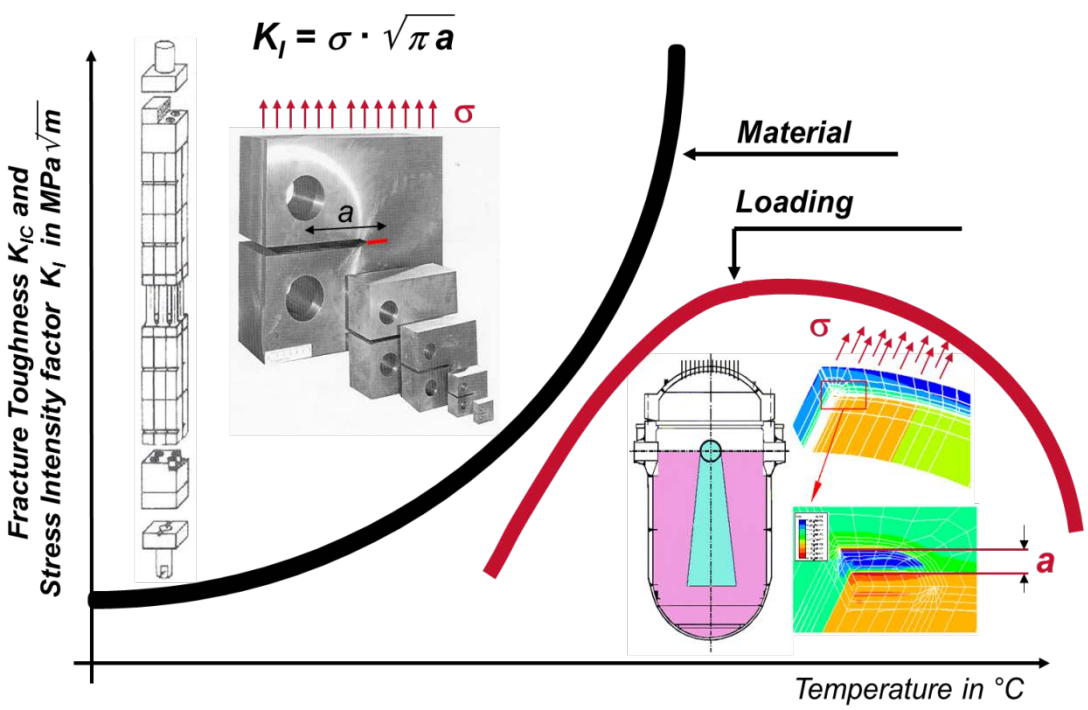


Figure 9: Principle of RPV safety assessment

Returning now to the construction of the loading or crack driving force and the material resistance or fracture toughness curves, in order to safely exclude brittle fracture against initiation, the former curve

should not intersect the latter curve. Nuclear regulators in some European countries allow credit to be taken from the so-called warm pre-stress effect (see Section 3.3). This means that for transients which after the maximum crack driving force show a strong monotonic reducing “load path”, crack initiation is excluded, if the crack tip of a postulated flaw has seen a warm pre-loading in the present transient. This means that the load paths after the maximum load will be neglected.

PTS analysis along the lines described above is commonly applied throughout Europe and the rest of the world with various countries having their own procedures or guidelines. Whilst it is probably true to say that these procedures and guidelines are generally equivalent to one other, there will inevitably be differences at the detailed level, some of these differences being associated with specific regulatory requirements. It would therefore be of benefit for such differences to be properly understood and evaluated with a view to eventually establishing unified European guidance on the subject.

3.3 Warm pre-stressing

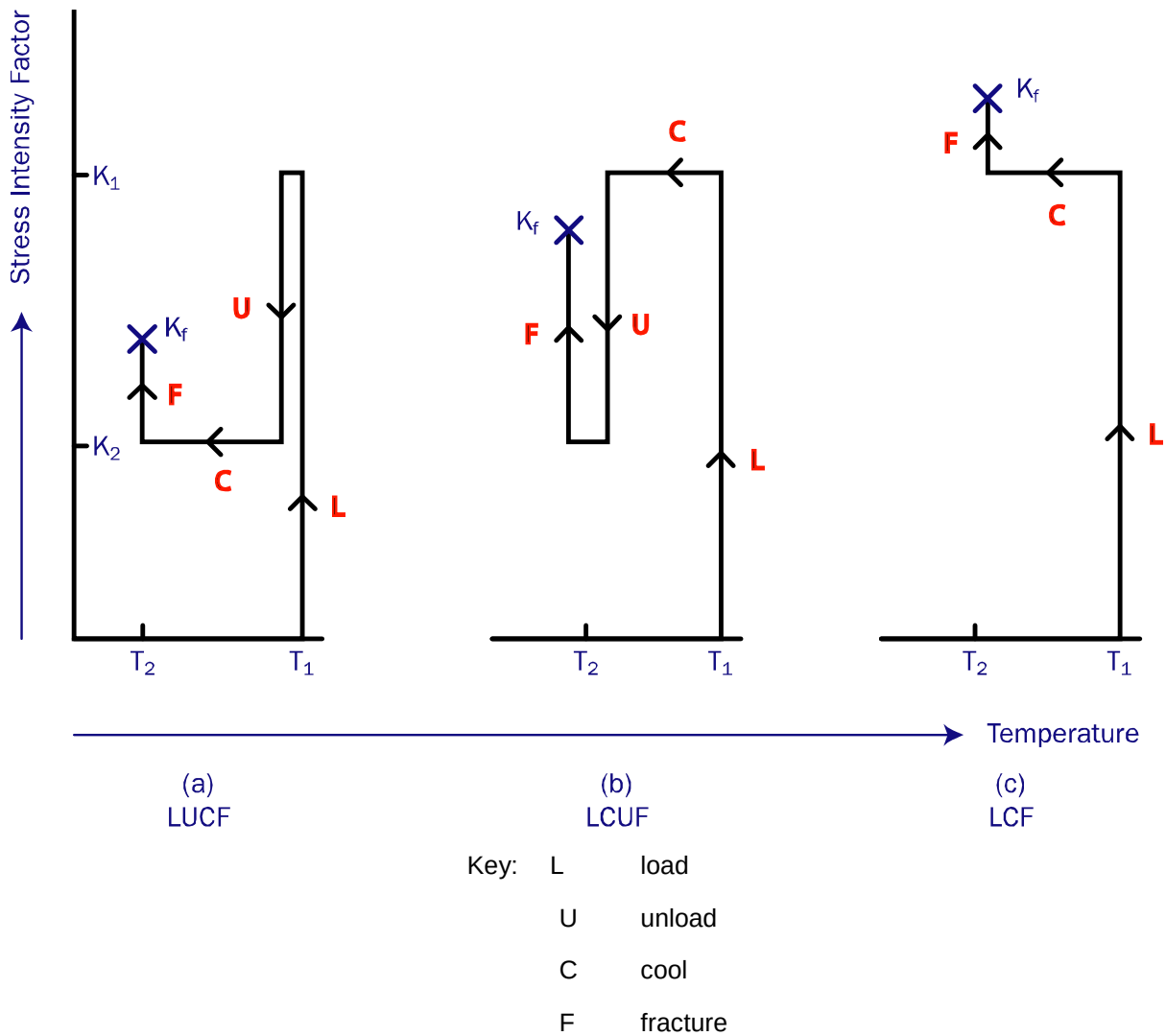


Figure 10: Typical laboratory warm pre-stress cycles

A warm pre-stress (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 is considered to elevate the stress intensity factor at fracture above the corresponding fracture toughness in the absence of the WPS.

There are basically three main types of cycle which are used in the laboratory to demonstrate the WPS effect. The temperatures at which the pre-load and re-load to fracture 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. The three main cycle types are (Figure 10):

- 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. The case where $T_2 = T_1$ is permissible if hardening mechanisms have occurred prior to the re-load to fracture.

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

- 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 the stress intensity factor at fracture is given by the LCF cycle, the least by the LUCF cycle with full unloading. Intermediate forms of cycle, where partial unloading occurs prior to re-loading to failure, and where the temperature and pressure are simultaneously reduced, give benefits lying between these two limits.

It should be noted that there is also a term called “conservative warm pre-stressing principle”. This is associated with the premise that failure is avoided if the stress intensity factor during cooling is constant or montonically falling to a value K_2.

The benefits of a WPS in thick-section components have been attributed mainly to the establishment of a compressive residual stress zone ahead of the crack tip. However, the effects of crack-tip blunting and strain hardening have also been claimed as significant by a number of researchers. Experimental demonstration of the WPS effect has been reported in many references (e.g. [25], [26]). A number of alternative quantitative models of WPS benefits have been developed. These include both engineering models based on crack driving force and energetic considerations, and local approach models.

The reported experimental demonstration of the WPS effect has been on both small specimen and features tests. A good example of the latter is an experimental programme conducted under the recent NESC VII European project [27]. This project was centred on experiments aimed at demonstrating the WPS effect in loading conditions representative of idealistic PTS conditions by way of specimens under biaxial bend loading conditions. Standard specimen WPS testing was also undertaken as well as substantial supporting analytical studies. For all the experiments, the final cleavage fracture occurred during the last isothermal loading phase, with the ultimate loading level clearly higher than the stress intensity factor value reached during the warm pre-loading phase (and also higher than fracture toughness values in the absence of WPS). These experimental results therefore represented a successful demonstration of the WPS effect in biaxial loading conditions, when either a LCF or a more complex LCTF (Load-Cool-Transient-Fracture) cycle is concerned and whatever the unloading level was in LCTF cycles. Analyses of the experiments with various engineering WPS models (Chell and Haigh [28], Wallin [29], modified Wallin [30] and the ACE criterion [31]) showed that they were conservatively able to predict the benefits of the WPS effects. The modified Beremin local approach model also provided conservative analyses.

As noted above, there has been a significant amount of experimental work over the years to demonstrate WPS effects and the various models have generally been well developed and validated

3.4 Long term irradiation effects

During the operation of a nuclear power plant, the neutron flux affects the RPV material properties. The irradiation of ferritic steels by (fast) neutrons with sufficiently high energy causes interactions of the neutrons with the atoms of the RPV steel influencing the microstructure of the irradiated material. This leads to dynamic dislocation generation and changes in the regular atomic configuration in the crystal lattice leading to so-called displacement cascades that contribute to the formation of dislocation loops, point defect and vacancy-solute clusters. These characteristic lattice defects affect the macroscopic material properties of the irradiated material: the yield strength and tensile strength increases, the ductile-brittle transition temperature (DBTT) changes to higher temperatures and the upper shelf energy decreases. The mechanisms responsible for these irradiation induced embrittlement effects are well known in terms of matrix damage, Cu rich precipitates containing Ni, Mn, Si, and grain boundary segregation of P. The main influencing parameters are the weld or base material, material manufacturing processes, chemical composition (in particular, the content of Cu, Ni, Mn, P, N), irradiation temperature, neutron flux, energy spectrum of the neutrons, irradiation time and accumulated neutron fluence (neutron flux and irradiation time).

A dedicated irradiation surveillance programme is mandatory in each NPP for ensuring quantification of the irradiation effects on the reactor pressure vessel materials and to predict the changes in macroscopic material behaviour of these materials by subsequent testing of the surveillance specimens. The irradiation of specimens can be performed in the specific RPV of concern itself, but nearer to the core to receive the irradiation in advance, or in a host reactor. Typically, the high energy neutron fluence or the dpa are used as damage function to define the lead factor for determining the transfer function of the surveillance results to the RPV wall.

Damage doses in ferritic pressure vessel materials (generally low-alloy bainitic steels) reach only around 0.1 to 0.2 dpa at the end of normal life (40 years) and for current RPV steels the irradiation-induced shift in the brittle to ductile transition temperature would typically be no more than 100°C. For this level of damage, the mechanisms are understood reasonably well, but, in the case of an extended life of more than 40 years, two main questions arise. Firstly, does accumulation of more than 0.2 dpa induce other damage mechanisms (for example, segregation of phosphorous to the grain boundaries or development of new phases after a certain incubation time? Secondly, what is the effect of temperature (around 290°C) for a duration of more than 40 years?)

With limited experimental data available at these extended lifetimes, greater reliance must be placed on modelling, which in turn requires a physical understanding of the processes occurring during irradiation and ageing. Progress in the physical understanding of the phenomena involved in irradiation damage has now raised the possibility of linking calculations through from ab-initio calculations on the nano-scale through microscopic models at the level of grain size to macroscopic calculations of material performance. These linked, multi-scale models may provide a basis for representing understanding of material degradation processes and predicting material performance through extended life. The European framework programmes PERFECT [32] and PERFORM 60 [33] brought together a number of European organisations to develop these multi-scale modelling techniques. A first step towards this goal was successfully reached through the development of numerical tools to predict material toughness within the PERFECT project. The subsequent PERFORM 60 project included the objective of improving multi-scale tools aimed at predicting the effects of irradiation on RPVs (bainitic steels) and their scatter in measured data.

A NUGENIA Position Paper has been produced [34] that is centred on the PERFORM 60 project. A brief summary of the project is as follows. It was aimed to develop an Advanced Fracture Toughness Module (AFTM) based on simulating the irradiation degradation on RPV's for durations up to 60 years. Numerical results from the AFTM were compared where appropriate with existing analytical results and experimental data. To this end, there was a strong link between this PERFORM 60 project and another European framework project, LONGLIFE (which is also the basis for a NUGENIA Position Paper [35]), which was

concerned with the collection and evaluation of relevant material properties data in both un-irradiated and irradiated states. The main issue for PERFORM 60 from the industrial point of view was to develop a method to simulate the fracture behaviour of the RPV steels. Four independent models of cleavage fracture toughness prediction were investigated within the project. Together they span the length scales from the micro-structural level to that of RPV components. The materials included RPV base metals, welds material and special 'model alloys', which formed the basis of reference cases used to validate and verify the various fracture models.

During PERFORM 60, specific advances and enhanced understanding were achieved on several fronts, including: precipitation/segregation mechanisms by way of ab-initio simulations and the development of adequate and more sophisticated interatom potentials; interaction between irradiation induced defects and dislocations leading to the development of a new crystal plasticity law based on dislocation dynamics; and the link between the microstructure and its changes and the evolution of the stress/strain field in RPV steels, leading to a better prediction of fracture toughness using advanced models. It was found that, in general, the RPV tools performed well and showed promise in relation to future industrial application. It was clear that although there were still significant limitations in current knowledge, the RPV tools were already valuable for training and for the development of insights.

3.5 Treatment of cladding

Austenitic stainless steel cladding, up to a few millimetres in thickness, is applied by the weld deposition of one, or more, layers (usually two) to the inside wall of Light Water Reactor RPVs. This is to inhibit general corrosive attack of the ferritic low-alloy steel base material, and to minimise any associated radioactive contamination of the reactor coolant system. Cr-Ni steel is the clad material used for most of these surfaces. As a rule, the RPV cladding should be welded in at least two passes to ensure re-crystallization of the heat affected zone in the ferritic base metal and to eliminate the potential for relaxation and hot cracking. To neutralize dilution with the base metal, to prevent hot cracking and to induce resistance to inter-granular corrosion, a highly over alloyed clad metal is used for depositing the first pass of the austenitic cladding and a less over alloyed clad metal is used for the second pass and any subsequent passes.

The cladding is a structural feature which can be taken into account in RPV integrity assessments. A full understanding of the behaviour of clad regions under PTS conditions is essential to the development of improved methods of RPV assessment. The need for this understanding is particularly important in relation to crack front behaviour in the clad/HAZ/base metal interface region. There is currently no clear consensus in Codes and Standards for assessing the influence of austenitic cladding in RPV assessments. However, simplifying assumptions can be adopted for assessing defects in the clad region along the lines of the cladding being considered as a discrete region for the heat transfer analysis, the HAZ being implicitly ignored, only through-clad flaws being considered and flaws being allowed to initiate in the clad region. It is considered that assessment of sub-clad defects based on these simplifying assumptions will be conservative, providing the sub-clad defect is modelled as an appropriate 3D surface breaking defect and estimates of the crack driving force (or strain energy release rate) are made on the basis of 3D elastic-plastic finite element analyses.

Residual stresses in the cladding can be taken into account by the definition of the stress free temperature. Depending on the temperature of the vessel relative to the so-called "stress free" temperature, tensile residual stresses can exist in the cladding, with a steep gradient to much lower, possibly compressive, stresses in the base metal.

Because of this gradient effect, cladding residual stresses are likely to have only a relatively small effect on the driving force for fracture at positions on a crack front below the clad-base metal interface. During a PTS transient, the cladding is subject to significant plastic deformation, which serves to reduce the cladding stresses to a low level after the transient. Warm pre-stressing, linked with the presence of

cladding, is a factor that will tend to inhibit crack initiation in cleavage during PTS transients. WPS benefits refer here to the so-called “conservative” WPS principle as described in Section 3.2.

As has been noted above, there is currently no clear consensus in Codes and Standards for assessing the influence of austenitic cladding in RPV assessments and this is obviously an area where the development of suitable guidance at the European level would be advantageous.

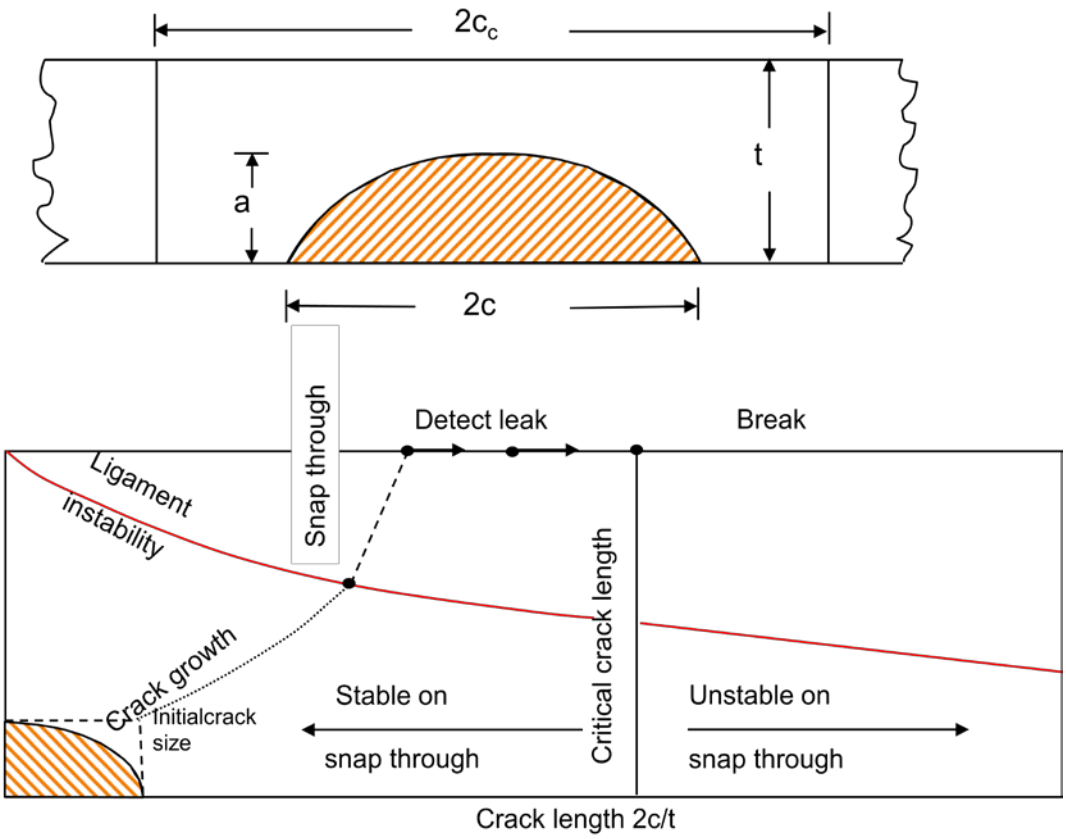
4 Aspects relating to piping and associated components – Current understanding and where further work is required

4.1 General aspects

In considering fracture mechanics assessment methodology for piping and associated components (e.g. steam generators pumps and valves), most of the general topics highlighted in Section 2 are relevant. In addition, there are various structural features which may require additional consideration. Some such features were addressed under the recent STYLE European project [8] and this section is dedicated to taking on board the information gained from that project in relation to fracture mechanics assessment. Leak-Before-Break (LBB) was highly featured in STYLE since LBB arguments are fairly commonly used nowadays as part of the safety justification for piping systems, mainly as a defence-in-depth element.

The various stages in the development of a LBB argument may be explained with the aid of the diagram shown in Figure 11. This diagram has axes of defect depth, a, and defect length, 2c, normalised by wall thickness, t. An initial part-through defect on the “front surface” of the component is represented by a point on the diagram. The defect may grow by fatigue, tearing, creep (in high temperature reactors) or other process until it reaches some critical depth at which the remaining ligament ahead of the defect breaks through the wall at the “back surface”. The defect then continues growing in surface length until there is sufficient opening to cause a detectable leak or until it becomes unstable. A LBB argument is aimed at demonstrating that leakage of fluid through a defect in the wall of a pipe can be detected prior to the defect attaining conditions of instability at which rapid crack extension occurs. In its simplest form a LBB assessment entails evaluating the limiting crack length and the detectable leakage crack length and demonstrating that the value of the former is significantly greater than that of the latter. Evaluation of limiting crack length involves of course conventional fracture mechanics assessment application and the evaluation of detectable leakage crack length involves a combination of fracture mechanics (to obtain crack opening area) and thermal hydraulics principles (to obtain leak rate through the defect).

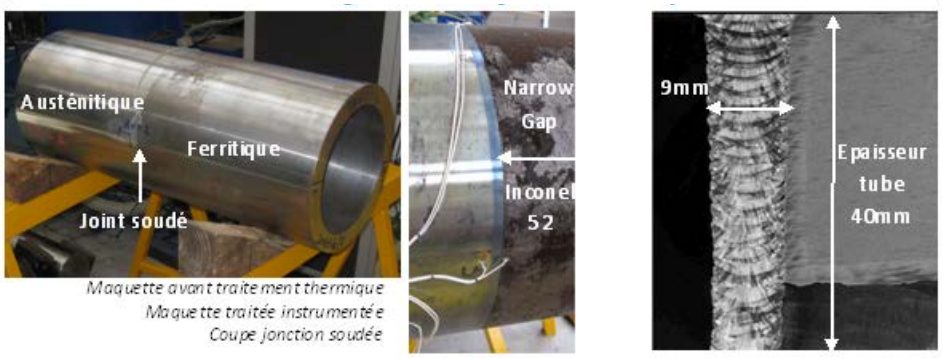
STYLE (Structural integrity for lifetime management – non-RPV components) was a four year project, running from January 2010 to December 2013, consisting of 20 participating European organisations and was aimed at establishing enhanced tools and methodology to be applied in lifetime assessments of pipework and associated components. In assessing the structural integrity of welded and clad components, the influence of residual stresses resulting from the weld/fabrication process can be significant and consequently the evaluation of such stresses, by both experimental and analytical techniques, featured strongly in various work packages of the STYLE project. The project was centred on structural mock-up and supporting experiments. The structural mock-up experiments contained pipes loaded under global bending with features of (i) a narrow gap dissimilar metal weld (DMW) (Figure 12), (ii) a repair weld in an austenitic steel butt weld (Figure 13), and (iii) an austenitic clad ferritic pipe (Figure 14).



$2c_c$ is the critical length of a through-wall crack

Figure 11: The Leak-Before-Break diagram

Within STYLE, an overview of fracture mechanics methodologies (referred to in the project as Engineering Assessment Methods (EAMs)) applied in different European countries was obtained, including commonalities and differences. This was particularly with respect to application to Dissimilar Metal Welds, Repair Welds and Clad pipes. This information is contained in Section 4.2. Section 4.3 contains information on a similar exercise that was undertaken on overviewing LBB methods applied in European countries, including evolution and the regulatory position. Latterly in the project, participating organisations applied their various EAMs to case studies that had been developed based on the three structural mock-up experiments. Information on these studies are presented in Section 4.4. General conclusions and recommendations are provided in Section 5 and Section .6 contains an overview of other aspects.



Maquette avant traitement thermique
Maquette traitée instrumentée
Coupe jonction soudée

Figure 12: STYLE mock-up 1 experiment (Dissimilar metal weld)

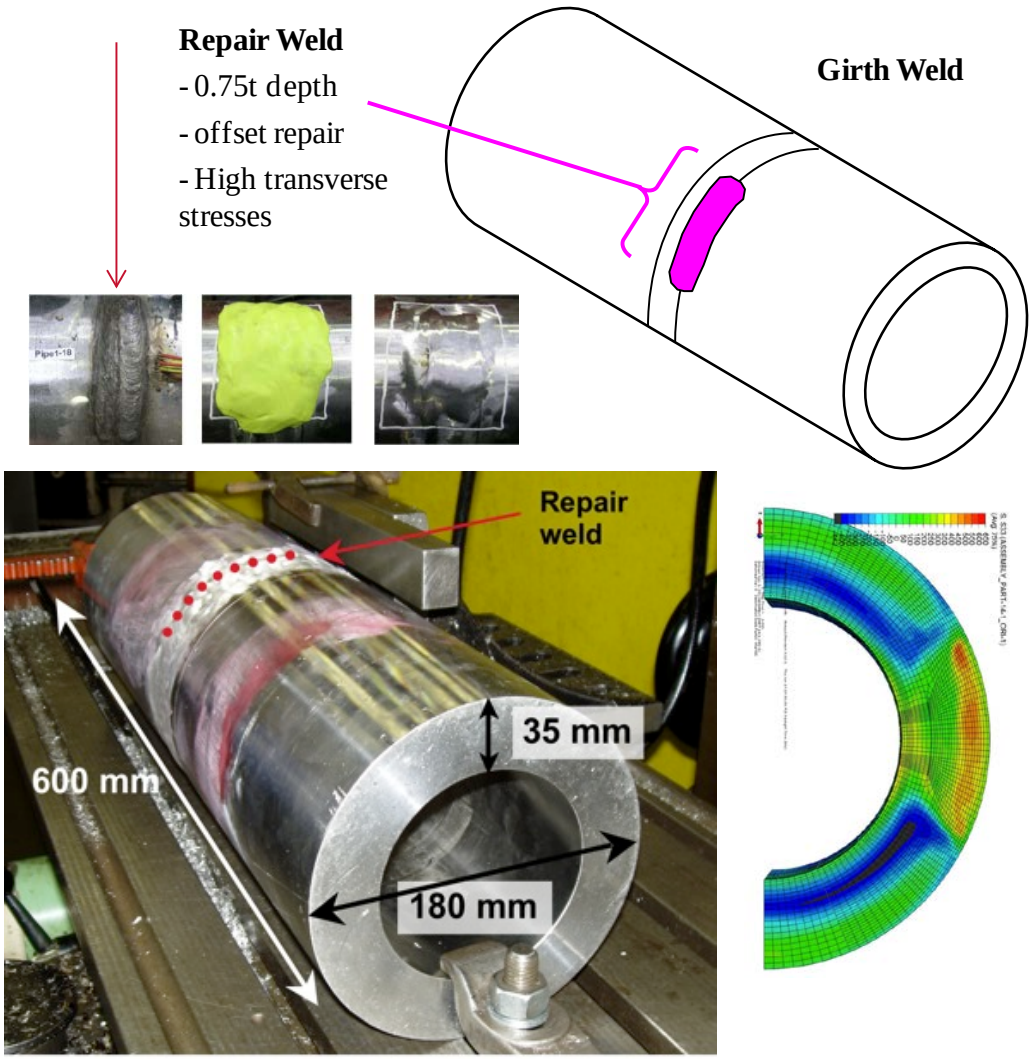


Figure 13: STYLE mock-up 2 experiment (Repair weld)

4.2 Overview of fracture mechanics methodologies applied to piping in general within European countries with particular emphasis on dissimilar metal welds, repair welds and clad pipes

4.2.1 General

A qualitative comparison of Engineering Assessment Methods (EAMs) was undertaken by collating relevant information collected from the various participants The organisations which took part were AREVA-GmbH (Germany), AREVA-F (France), CEA (France), AMEC (UK), University of Bristol (UK), NRG (Netherlands), UJV (Czech Republic), TEC (Spain), VTT (Finland) and Ringhals (Sweden). The main aspects of the study were related to Mock-Up Experiments 1 (DMW), 2 (Repair Weld) and 3 (Clad Pipe). The individual partners provided brief descriptions of their EAMs for structural integrity evaluations of dissimilar metal welds, repair welds and clad ferritic pipes. In their contributions, special attention was intended to be focussed on such aspects as mismatch of material properties, residual stresses, mixed-mode loading and constraint effects.

4.2.2 Dissimilar metal welds

In **Germany**, the KTA standard (mainly KTA 3201.4 and KTA 3206) [4, 56] is used for evaluation of a crack found in DMWs. If a flaw is found, root cause analysis needs to be performed. Fracture mechanics analysis is suggested as part of the safety analysis, but it is not necessarily mandatory. A guidance for performing this analysis is given in the standard KTA 3206 [56], but the following procedure may also be applied.

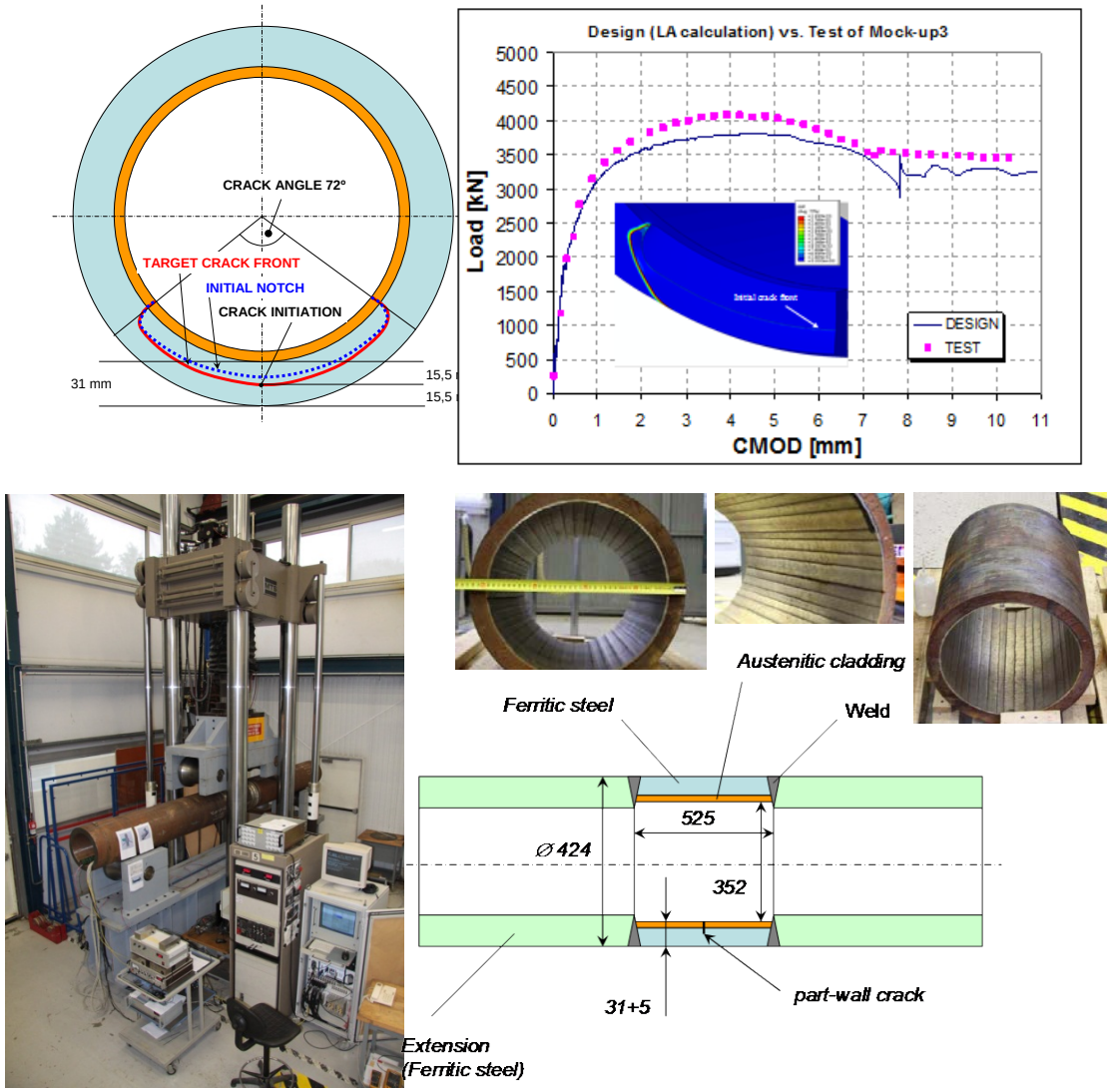


Figure 14: STYLE mock-up 3 experiment (Clad pipe)

If the DMW constituent materials meet the ductility criterion (impact energy > 45 J), structural integrity of the DMW is assessed by using methods for which only strength and impact energy of the base material are needed. These methods are Plastic Limit Load (PLL), Flow Stress Concept (FSC), Battelle Memorial Institute (BMI) and the Ruiz methods. The methods require a specific formulation of both flow stress and the stresses in general, as described in [36]. Thermal ageing (if relevant) is taken into account by considering toughness values, relevant to the time of the next inspection, based on the appropriate ductility criterion. If there are significant torsional loads (mixed mode loading), then torsional moments need to be taken into account. Since welding residual stresses are self-equilibrating, they are not taken into account when undertaking a ductile fracture assessment in order to evaluate critical crack size. Stresses such as those due to thermal expansion should be taken into account however in such evaluations since they are considered to be potentially damaging.

Assessment of whether a flaw found in a DMW is allowable is performed according to the scheme in Figure 15. The left hand side of the scheme in Figure 15 represents flaw characterisation and determination of its end-of-life (in general, end-of-evaluation period) dimensions, while the right hand side represents the determination of allowable (critical) dimensions of the flaw.

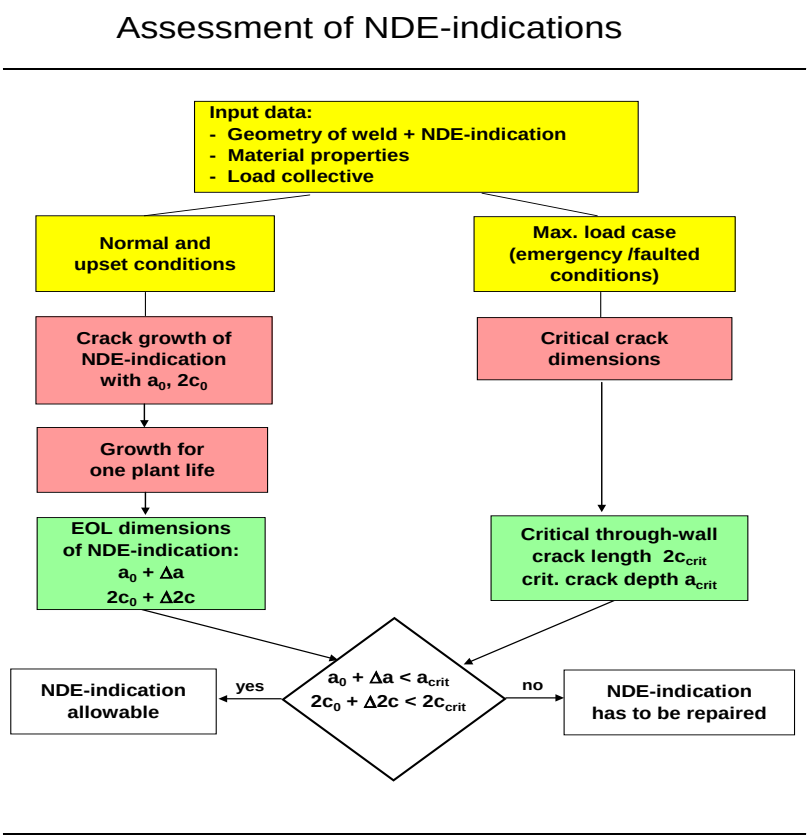


Figure 15: Scheme of assessment of NDE indications as performed in Germany

Determination of allowable (critical) dimensions of a crack is illustrated in Figure 16. In this Figure, the "cloud" curve (the thick curve) represents the locus of critical crack dimensions, i.e. it determines critical crack depth values dependent on critical crack length values. For a given load and given component geometry, critical crack length is calculated based on the LBB principle (see Critical Through-Wall Crack Length in Figure 16) and for this, critical crack length the (relative) critical crack depth is determined, using the "cloud" curve. If the end-of-evaluation-period dimensions of the crack are smaller than the allowable (critical) crack dimensions, then the flaw is deemed to be acceptable (see Figure 16). Using Figure 16 for flaw evaluation also permits assessment of various margins (e.g. margin between end-of-life flaw size and the critical flaw size).

For DMW configurations that do not fulfill the ductility criteria, further detailed investigations may be undertaken. These may include such aspects as weld residual stress evaluation, fracture toughness evaluation of the different weld regions, J-R curve determination on representative specimen and loading and detailed stress analyses, including the use of local approach models. Use can be made of relevant information contained in other codes, and procedures, such as R6 [7], RSE-M [6] or ASME XI [5].

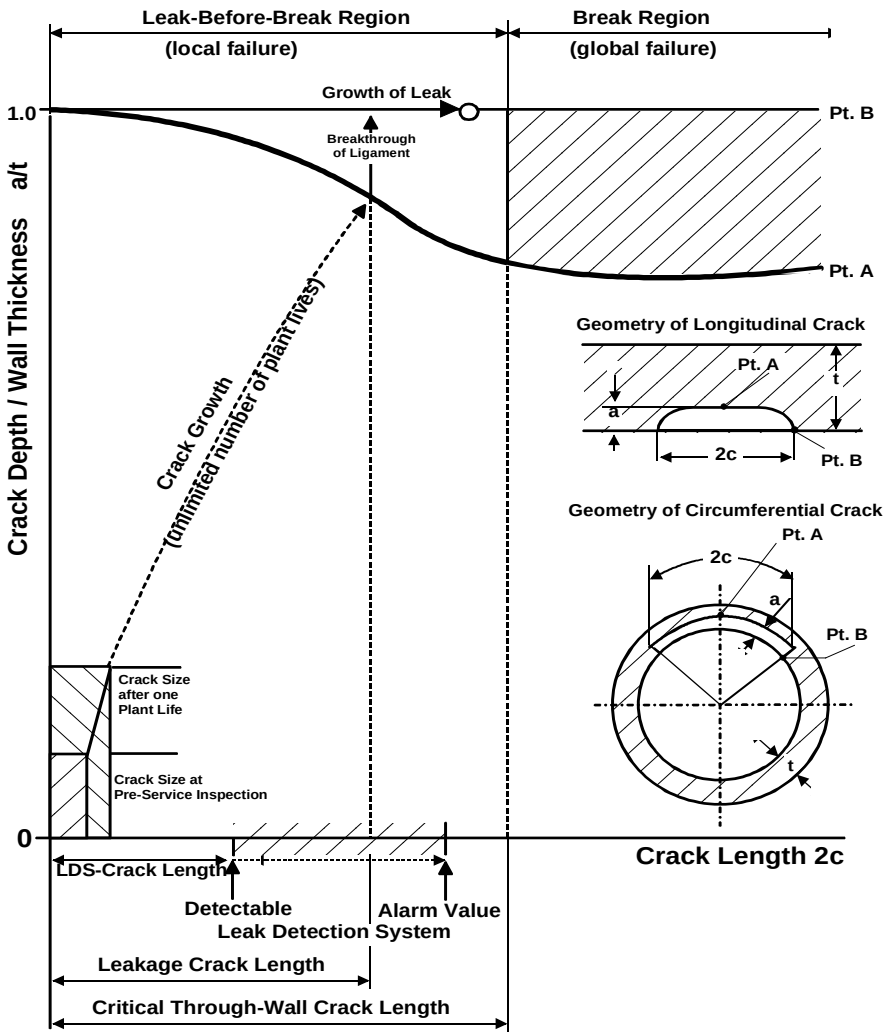


Figure 16: Scheme used (in Germany) for determination of allowable (critical) dimensions of a crack

In **France**, the codes used for the evaluation of flawed DMWs (and flawed components in general) are RCC-M (App. ZG) [3], RCC-MRx (App. A16) [45] and RSE-M (App. 5.4 – 5.6) [6].

Investigation of flaws found in DMWs was originally associated with the evaluation of inter-granular defects observed in the buttering of stainless steel DMWs. A combined approach of FE calculations and code-based assessment was used for evaluation of these defects. FE analyses were conducted to determine J-values at points along the crack front, and then the criteria of ZG Appendix of the RCC-M code [3] were used to assess the defects. Currently, the development of a new engineering assessment method for evaluation of the flawed EPR™ TIG narrow gap Inconel DMW is underway. Within this new method, emphasis is being placed on FE analyses that are considered to be the most suitable tool for demonstrating (where possible) that unstable fracture of the flawed DMW can be ruled out. Defects are postulated on the interface between weld metal and ferritic steel.

In the **UK**, the main assessment methods used for evaluation of flawed DMWs (as well as for other flawed components) are the R6 [7] and R5 [37] assessment procedures. R6 contains fracture procedures and R5, creep and creep-fatigue procedures specifically developed to support the UK Advanced Gas Cooled Reactors (AGRs). As has previously been noted, there is also a fracture mechanics based UK developed British Standards procedure, BS7910 [11], for assessing the acceptability of flaws in metallic structures.

R6 contains elastic-plastic methodology based on a Failure Assessment Diagram (FAD) approach. The procedures offer various assessment levels, ranging from simple and conservative approaches right through to complex and less conservative approaches involving detailed finite element analyses and/or experimental work. R6 does not incorporate specific safety factors as such but prescribes sensitivity analyses in order to assess the significance on the results of varying the values of the various input data. In relation to DMWs, R6 contains guidance on weld residual stresses, material strength mis-match and mixed mode loading. Guidance on weld residual stresses includes: Level 1 simple estimates based on a membrane profile (of magnitude equal to 1% proof stress for stainless steels and 0.2% proof stress for ferritic steels); Level 2 based on upper bound profiles provided by way of a compendium containing such information for various weld geometries; and, Level 3 based on detailed finite element analyses where step by step guidance is provided, combined with experimental validation. Material strength mis-match is dealt with by way of providing limit load (or reference stress) solutions for various cracked geometries with various levels of over- or under-matching yield stress. In terms of mixed mode loading, R6 provides detailed guidance on how to perform appropriate evaluation if significant shear stresses are present, for non-cyclic loading.

In the **Netherlands**, assessment of a flaw found in a DMW consists of a combination of FE analyses and code based assessment. FE analyses are used for evaluating stress profiles around a DMW, and these are then used in fracture mechanics evaluations. The basic fracture mechanics screening of the DMW is first performed according to the basic level 1 guidelines of BS7910:2005 [11]. This leads to significant conservatism and very small allowable defect sizes. As an alternative (less conservative approach), the R6 option 1 assessment procedure can be applied in combination with the R-code software. Fracture mechanics assessments are performed using conservative assumptions with respect to loading, where mechanical and thermal stresses are considered as primary loading. Secondary loading, being welding residual stress is conservatively taken into account as membrane stress of yield magnitude.

In the **Czech Republic**, assessment of a flaw found in DMWs consists of a combination of FE calculations and code-based assessment. However, only simple (one-dimensional) FE calculations are performed using global FE piping models to determine loading stresses (no detailed FE modelling of the DMW region is performed). VERLIFE [38] or the ASME XI code [5] are used for the assessment.

When assessing a flaw in a DMW according to VERLIFE, different constituent materials are considered separately and assessment is performed according to Appendices XIII and XIV of the code, which deal with flaws in austenitic and ferritic piping respectively. These Appendices are based on the appropriate procedures described in ASME XI:1989. In particular, for austenitic material, a procedure similar to that in Article C-5000 ASME XI (flaw evaluation using limit load criteria) is applied. For ferritic material, a screening criterion is first applied, and based on its result, either an analogous procedure to that of Article C-5000 ASME XI is applied (in this case plastic collapse is indicated as the failure mechanism), or a procedure analogous to Article C-6000 of ASME XI is applied (in this case the failure mechanism is plastic collapse preceded by some amount of stable ductile tearing), or a procedure analogous to Article C-7000 ASME XI is applied (in this case the failure mechanism is unstable fracture).

Residual stresses are taken into account either in calculating flaw growth, or in evaluating the flaw according to Article C-7000 ASME XI. Although no guidance on how to determine residual stresses is actually provided in VERLIFE yield stress of the appropriate material is commonly assumed and added to the membrane stress.

It should be noted that according to the latest versions (2008) of App. XIII and XIV of VERLIFE, flaw growth during component operation is not permitted. This means that only manufacturing flaws can be assessed. If there is some indication of a flaw growing during operation, the mechanism of flaw growth must be identified and removed, or the flawed component repaired or replaced. Assessment of a flawed DMW may be performed using the ASME XI Code directly.

In **Spain**, assessment of a flaw found in a DMW is performed in a standard way, using the latest edition of the ASME Code. For flaws in Ni-base DMW weld metal, the evaluation procedures in ASME XI [5] for stainless steels and Ni-base alloys are applied. For fusion-line flaws in Ni-base buttered DMW, the evaluation procedures are those of the adjacent ferritic base metal. These procedures require application of "safety margins" which should be consistent with the margins incorporated in the applicable piping design criteria. They are applied individually to membrane and bending stresses and depend on service level (service Level A, B, C and D) and flaw orientation.

Evaluation of allowable crack size may be undertaken either according to Article IWB-3642 of ASME XI (acceptance criteria are based on Failure Mode Determination) or according to Article IWB-3643 (acceptance criteria are based on the Failure Assessment Diagram of R6). In both cases, flaw depths greater than 75% of the wall thickness are unacceptable. The first approach is described in detail in App. C to ASME XI (as described in more detail above for the practice employed in the Czech Republic), while the second approach is described in detail in App. H of ASME XI.

In **Finland**, assessment of a flaw found in a DMW entails the combination of FE analysis and code based assessment. FE calculations modelling the DMW region in detail are performed to evaluate relevant stresses. For determination of residual stresses, either the SSM Handbook [39] is used, or they are simulated by FE calculation. Alternatively, applicable measured data may be used. For crack growth rate or sensitivity analysis, conservative material properties, corresponding to the weakest of the constituent materials of the DMW (austenitic stainless steel), are used. ASME XI [5] is generally used for determining maximum allowable crack size for DMWs.

In **Sweden**, assessment of a flaw found in DMWs is very similar to that in Finland, involving a combination of FE analysis and code based assessment. FE calculations are performed to model the DMW region in detail. Residual stresses are determined either by using the SSM Handbook [39], or by FE simulation, or applicable measured data. The crack growth is calculated using the "ProSACC" software (for simple crack geometries), or using detailed FE evaluation (for more complex crack geometries). Maximum allowable crack sizes for DMWs are calculated using the procedure of the SSM Research report 2008:01 [40], based on the R6 Method. The appropriate safety factors specified in the SSM report 2008:01 are applied.

4.2.3 Repair welds

The participating organisations reported that no specific procedure for evaluating a flaw in repair welds exists in their national standards. Usually, the same methods and approaches are used as for evaluation of a flaw in DMW. The R6 compendium on residual stresses contains guidance for such stresses in repair welds and this is likely to be revised in the future as more experimental data become available.

4.2.4 Clad ferritic pipes

With the exception of France, all the participating organisations reported that there is no specific procedure for the evaluation of a flaw in clad ferritic pipes (even though some countries have procedures for assessing flaws in cladded RPVs). In evaluating a flaw in a clad pipe, cladding is usually not taken into account. Moreover, if the defect is underclad, then it is usually conservatively characterized as a surface defect.

In **France**, much attention has been paid in recent years to fracture mechanics analysis of underclad defects in both piping and vessels. The appropriate developments have been included in the 2010 edition of the RSE-M standard [6] and they include: stress intensity factor formulae for clad structures (for both underclad and through-clad defects); analytical formulations for the determination of temperatures and stresses through wall thickness for mechanical and thermal shock loading; and, plasticity corrections to allow for yielding of the cladding.

4.2.5 Commonalities

In studying the contributions received from the participating organisations, several types of commonalities were evident. The first commonality relates to the use of a commonly adopted assessment code or methodology. Nearly all partners reported using the ASME Codes [2], [5], either as their main assessment code or in addition to their national codes. Use of the R6 methodology [7] and the KTA standard [4, 56] outside the UK and Germany (respectively) seemed not to be very common and the French standards RCC-M [3], RCC-MRx [45] or RSE-M [6] are mainly specific to France. For assessing the three types of structural features considered though, many of the participants reported that they would undertake detailed finite element analyses, as a supplementary or as an alternative to their codes or procedures. Another common feature was identified as being that practically all of the assessment methods and codes (e.g. ASME XI [5], R6 [7], VERLIFE [38] and KTA [4, 56]) specified the use of conservative material properties, at least in the first instance. A further commonality relates to the fact that in assessing flaws in clad ferritic piping, the actual cladding is generally not taken into account which is considered to be conservative.

4.2.6 Differences

Several differences between the various EAMs were evident. One difference is in the definition of flow stress relating to limit load analysis. Flow stress is defined as the average of the yield stress and ultimate tensile stress in R6 [7] and Appendix C of ASME III [2]. In the German approach though, flow stress is derived from data obtained from a large number of small-scale and structural specimens. Another difference between the individual methodologies is associated with the provision of guidance for treating weld residual stresses when assessing flawed component. Whilst, for example, the R6 methodology provides detailed guidance on how to determine the residual stress variation in the weld, some other codes, such as the French, provide practically no guidance. Traditionally, only R6 allows for strength mismatch to be taken in to account but this aspect has also recently been incorporated into the French codes. Finally, although additional high level analyses, mainly involving elastic-plastic finite elements, are undertaken in many countries when required, R6 is really the only methodology which includes extensive guidance on undertaking such studies (e.g. on allowing for crack-tip constraint effects, modelling of the welding process to evaluate residual stresses and local approach modelling).

4.3 Overview of leak-before-break methods applied in European countries including evolution and regulatory position

4.3.1 General

A qualitative overview of leak-before-break (LBB) national practices and the regulatory position for the different participants was undertaken early in the STYLE project. The organisations which took part were AREVA-GmbH (Germany), CEA (France), AMEC (UK), AEKI (Hungary), UJV (Czech Republic), TEC (Spain), NRG (Netherlands) and ORNL (USA). (ORNL was linked to the STYLE project on a “contribution in kind” basis.)

In addition to STYLE, a pilot LBB project was undertaken under NULIFE [41]. This study involved additional European partners GRS and MPA from Germany, VTT from Finland and LEI from Lithuania. The pilot study also included contributions relating to the Japan Nuclear Energy Safety Organisation (JNESO) and RDIPE (NIKIET) of the Russian Federation.

The information relating to the individual countries was generally considered in relation to LBB practice, regulatory position, evolution, application, past and present research activities and future plans.

4.3.2 LBB Practice

From the information that was obtained from the participating organisations, it is evident that the various methodologies employed can loosely be separated into five different main approaches. It must be emphasised however, that all approaches have some similarities to one another. Of the five approaches,

the UK, French and Japanese approaches are country specific and not generally adopted elsewhere. Both the German Basic Safety Concept (BSC) [42], which is now being called the Integrity Concept (IC) [43], and the American Standard Review Plan (SRP) 3.6.3 [44], form the basic foundation for most countries. In Germany the rupture preclusion is now codified in the KTA ocde 3206 [56]. An overview of the five different approaches is provided in the sections below. (Note that LBB arguments are not currently used in Hungary).

BSC / IC

Country of origin: Germany

Organisations/Countries with methods based on procedure:

AREVA, GRS, MPA (Germany)

TECNATOM (Spain)

NRG (Netherlands)

The BSC / IC provides the overriding safety procedure for the assessment and structural integrity maintenance of NPPs and includes the Break Preclusion Concept (BPC) as one of the arguments. The BPC provides a means by which the complete cleavage of a pipe should be avoided. One of the legs of the BPC includes LBB so that, even if a crack forms, the leakage of fluid will be detected with significant margin to pipe failure. As such, the LBB section of the BPC forms a “defence in depth” argument for the prevention of pipe failure; which will be accompanied by preclusion of other failure mechanisms and other detection and prevention measures.

The LBB assessment itself is applied only to the most probable locations, such as welds, sections of increased load or material degradation and geometric features. The assessment applied uses the detectable leak rate to determine the crack size required for confident LBB detection and compares this to the critical crack size. In these calculations, only deterministic calculations are used. This deterministic approach and defence in depth argument within the BSC / IC renders probabilistic approaches unnecessary. It is codified in [56].

SRP-3.6.3

Country of origin: USA

Organizations/Countries with methods based on procedure:

UJV (Czech Republic)

LEI (Lithuania)

RDIPE (Russian Federation)

TECNATOM (Spain)

VTT (Finland)

ORNL (USA)

SRP-3.6.3 is detailed as the “detectable leakage” procedure when trying to apply LBB. The procedure was initially developed to remove the need for multiple pipe-whip restraints. These can be removed where it can be shown that a crack will be detectable prior to pipe failure, thus allowing intervening action before a pipe-whip occurrence. The procedure is only applicable to an entire section of piping, not individual components or welded joints. The procedure also excludes the use of LBB when water-hammer,

corrosion, creep, erosion, fatigue or environmental condition can lead to component failure within the lifetime of the NPP.

The LBB argument in SRP-3.6.3 can only be applied once the reliability of the detection system can be demonstrated. As with other LBB procedures, the basic principle is to use the detectable leak rate to determine the crack size required for confident LBB detection and compare this to the critical crack size to ensure suitable margins exist.

R6

Country of origin: UK

Organisations using procedure: AMEC (UK)

The approach contained within R6 [7] is based on two methods: 1) the "detectable leakage method" where it is required to demonstrate that there is a sufficient margin between detectable leakage crack length and limiting crack length assuming a through-wall defect from the onset (in line with SRP-3.6.3 referred to above), and 2) the "full LBB procedure" whereby a known or postulated defect is initially considered which has not yet penetrated through the wall. The second of these is more complex and involves crack growth calculations through life for the initially part-penetrating defect. The R6 approach is heavily based on sensitivity analyses within deterministic calculations and does not apply specific safety margins.

The LBB argument in R6 can usually only be used as part of a defence in depth argument and may not usually be applied when there are multiple defects (consistent with most of the other approaches). Allowances for some more complex situations, i.e. to allow for high temperature creep and/or complex geometries, are included.

RCC-MRX Appendix 16

Country of origin: France

Organisations using procedure: CEA (France)

Appendix 16 of RCC-MRX [45] includes details of how to implement an LBB analysis. As with the other methods, LBB estimates are made by comparing the crack size that allows for the leak to be detected and that which causes component failure. This comparison is quantified by a reserve factor of 2 in the two crack sizes and a factor of 10 on the detection capability (i.e. detectable leak rate). Some account for the crack dimensions are provided by accounting for different internal and external crack lengths.

Japanese Procedure

Country of origin: Japan

Organisations using procedure: JNESO (Japan)

LBB is applied to austenitic stainless steel piping in BWR, PWR and FBR in Japan. The approach to LBB in Japan is based on a deterministic approach. A probabilistic approach is not used. Guidance for applying LBB to nuclear piping systems was published by the Japan Electric Association (1998) [46]. Only austenitic stainless steel piping can be evaluated according to this guidance.

Fatigue crack growth analysis is recommended for a postulated semi-elliptical flaw with initial depth of the larger of 0.2*t* (*t* being wall thickness) or 3mm, and the half length the larger of 1.0*t* and 15mm. The fatigue crack growth rate is calculated by the Paris law using the stress intensity factor solutions by Newman and Raju (1981) [47] with minor corrections at the surface point. The calculation is continued beyond the applicable range of the solutions until the crack penetrates the wall thickness.

The next step is a leak rate evaluation using crack opening displacement formula, in conjunction with thermo-hydraulic models for PWRs or BWRs as appropriate. The effect of the surface roughness of crack

surfaces can be taken into account in the leak rate calculation by giving a standard value of the surface roughness.

Crack stability analysis is recommended for a through-wall crack with length equal to the larger of either (i) inner-surface length obtained by fatigue crack growth analysis,

or (ii) crack length estimated to provide 5gpm by leak rate evaluation.

4.3.3 Regulatory position

The regulatory position of each country is generally to adhere to one of the five general categories of LBB procedures outlined above. Where procedures are adopted from other countries, there is normally little modification. For example in the case of TECNATOM, Spain, both the BSC / IC and SRP 3.6.3 are adopted, depending on the country of origin of the NPP. Where there are differences, this is normally in the absolute margin required, the amount of sensitivity analyses allowed, the detection requirements and the level of In Service Inspection (ISI) stipulated.

Of the five main LBB approaches outlined above, the BSC / IC is a legal requirement in Germany and codified in KTA3206 [56], SRP 3.6.3 is contained within the US Nuclear Regulatory Commission (NRC) standard review plan and the UK Nuclear Safety Directorate (NSD) outlines high level recommendations for LBB in a Technical Assessment Guide. The NRC review process for LBB analyses of plant-specific piping system submittals is laid out in SRP, Section 3.6.3, and NUREG-1061 (Vol. 3) [48] gives detailed discussion of the technical bases for LBB analyses and acceptance criteria. An NRC-approved LBB analysis permits the licensee to remove protective hardware such as pipe whip restraints and jet impingement barriers, and other related changes in operating plants.

The Lithuanian Nuclear Power Safety Inspectorate (VATESI) position on LBB application is that the LBB concept was used as a compensatory measure in the Ignalina RBMK NPP and that modifications to ISI strategy may be made on the basis of LBB justifications. According to LBB results, the simplification of ISI (physical and radiation) can be made.

The main source of information in Finland regarding national practices concerning deterministic and/or probabilistic LBB approaches for NPP components is the regulatory body Radiation and Nuclear Safety Authority of Finland (STUK). Among the variety of document series types they publish are Regulatory Guides, which in turn are divided into those on radiation safety (ST Guides) and nuclear safety (YVL), respectively. The main references on the subject of LBB are YVL Guide 3.5 - Ensuring the firmness of pressure vessels of a NPP (in Finnish only, date 5 Apr 2002), and YVL Guide 2.6 - Seismic events and nuclear power plants (date 19 Dec 2001), respectively. The YVL Guides are considered as regulatory requirements in Finland.

At present, in Hungary, the LBB concept is not applied in practice. The authority recommendation is that LBB is not necessary since the Paks NPP operates the so called “box system”, instead of containment. The box system is where several small, closed rooms which contain the primary circuit equipment, these rooms are connected by tunnels to a cooling tower. This, in essence, isolates the primary circuit and negates the need for LBB.

In Japan, LBB for austenitic stainless steel piping was endorsed by the Nuclear Energy Safety Committee of the Japanese Government in March 1992.

Since 1993, the Russian Regulatory LBB practice has been established in procedure M-LBB-01-93 [49]. The application of LBB to Russian plant is made on the basis of evaluations and considerations from design and R&D activities on a case-by-case basis for each piping system.

No official position was provided regarding the use of RCC-MRX Appendix 16 in France from a regulatory point of view.

4.3.4 Evolution

The evolution of the different LBB procedures commenced in 1979 with the German BSC, which subsequently led to the BPC in 1989 as part of a defence in depth argument. Also during the 1980s, the US NRC undertook a number of research programmes to assess the use of LBB, which culminated in SRP 3.6.3. As mentioned above, SRP 3.6.3 was developed to allow the removal of pipe-whip restraints, not specifically as part of a defence in depth argument in an operational safety case. In the UK, also over the same period, the use of LBB was being advanced for application to UK reactor systems and was subsequently included to R6 in 1990, intended as part of a defence in depth approach. The method in R6 has been updated on three further occasions, in 1996, 2004 and 2010, and the German BSC has been updated twice and it became the IC in 2007.

Evolution of note in other countries can be summarized as follows:

Netherlands - During modifications under a specific national programme in 1997, the concept of Break Preclusion was applied to the Borssele Nuclear Power plant (NPP) according to German methodology. The method has been revalidated for Long Term Operation justification of the Borssele NPP.

Finland - The deterministic LBB analysis procedures considered applicable in Finland have largely been developed elsewhere. However some development work has been undertaken within Finland, such as that undertaken by VTT associated with the leak rate assessment code CRAFLO.

USA - Given recent advances in probabilistic methodologies, it is believed in the US that performing a probabilistic analysis of primary system piping that fully addresses and quantifies uncertainties and directly can demonstrate regulatory compliance is appropriate. Recent efforts by the NRC, nuclear industry and national laboratories have been aimed at developing a robust, modular-based probabilistic software tool to facilitate meeting that goal. Based on the terminology of GDC-4, this effort is titled eXtremely Low Probability of Rupture (XLPR) [50]. The US NRC and industry expect that the robust probabilistic software tool will be used in the future to facilitate meeting the requirements of GDC-4, and will result in improvement in licensing, regulatory decision-making, and design. The multi-year xLPR Project began with a focus on development of a viable method and approach to address the effects of stress corrosion cracking and on defining the requirements for a modular-based assessment tool. The initial study phase focused on stress corrosion cracking in pressurizer surge nozzles. Later phases have been broadened to include all primary-piping systems in PWRs and BWRs. An incremental approach is being used that incorporates the design requirements and lessons learned from previous iterations. The NRC's general schedule is to complete the overarching modular code by 2017.

Japan - LBB for stainless steel piping components was endorsed in 1992. LBB Guidance, JEAG 4613 [46], for stainless piping components was published by the Japanese Electric Association in1998. The Guidance gives the design methodologies for determining such aspects as crack opening area, fatigue crack growth and failure modes. The LBB Code, JSME S ND1-2002 [51], for austenitic stainless, carbon and low alloy steel piping components was published by The Japanese Society of Mechanical Engineers in 2002.

4.3.5 Application

There have been fairly extensive applications of LBB in the various countries over the years. A common theme is that LBB has been applied to both ferritic and austenitic piping where fatigue is the main degradation mechanism. Pipe diameters usually have to be at least 50 mm for LBB to be considered.

In the BPC the materials composition include ferritic, ferritic with austenitic cladding and austenitic piping systems. It is also conditional that the piping system should be restricted to high energy lines operating over 2% of their operational lifetime. Therefore, the LBB approach within the BPC is generally applied to main coolant lines, surge lines, main steam lines and main feedwater lines.

Likewise, when adopting the US SRP 3.6.3 approach, the piping system should be high energy class 1 or class 2 piping. This therefore includes the reactor coolant system, surge lines, parts of the accumulator lines, ECCS and the Residual Heat Removal (RHR) system. Under certain conditions, LBB also may be applied on other types of high energy piping.

Application of LBB in the UK has been applied as a defence in depth argument to several systems and components. Typical components where LBB has been applied include primary vessels of Magnox reactors, primary circuit components and piping of Advanced Gas-cooled Reactors (AGR), pressure tubes and steel drums in the Steam Generating Heavy Water Reactors (SGHWR), reactor cooling pump casing in Sizewell B Pressurised Water Reactor (PWR) and a number of components within the Prototype Fast Reactor (PFR). It has also been considered for primary pipework components for future PWR plants.

In France, LBB has been applied to fast-breeder reactors, particularly the PHENIX reactor.

4.3.6 Past and present research activities

A large number of research activities have been conducted in European countries over the last 30 years associated with LBB. Within these programmes a number of experimental validation tests and research programmes have been performed including;

- A broad range of experiments to consider validation of LBB within the BPC by Siemens KWU in Germany.
- Helium environment experiments for gas fast reactors in France.
- Large-scale plate experiments to assess crack shape development and breakthrough in the UK.
- Critical crack length, Crack Opening Area (COA) and leakage rate studies in the UK.
- A research programme ”OKKFT” (Országos Középtávú Kutatás-Fejlesztési Terv - National Middle Range Research and Development Plan) including experimental validation, to consider LBB, noise analyses and leak detection in Hungary.
- Leak detection and validation of the PICEP and SQUIRT computer codes and development of BASLBB and FATLBB computer codes, large scale experiments for studying crack behaviour and leak detection experiments on an experimental loop in the Czech Republic.
- A number of full and part scale tests performed to contribute to initial LBB criteria in the US, plus development of the SQUIRT computer code.

The level of research recently and/or currently being performed on LBB is much less than that previously conducted and is generally confined to the activities outlined below.

- Research into LBB in Germany is currently focused on quantifying the back-fit and replacement of components.
- In the UK, substantial development work has been undertaken in recent years in order to extend the COA solutions for R6. For high temperature operations, the effect of creep strain on COA has been studied using FE models.
- A recent university doctorate project in Hungary considered LBB applied to austenitic welds.
- Industrial participants in Spain, as part of an EPRI programme, are considering LBB applied to Dissimilar Metal Welds (DMWs) which are susceptible to Primary Water SCC.
- Analyses of COA, and general fracture mechanics approaches, are being performed in the Netherlands for different geometries.
- A probabilistic approach is being developed called eXtremely Low Probability of Rupture (xLPR) in the US.

4.3.7 Future plans

Future research and plans into LBB include the following:

AREVA (Germany) - Focused on back-fitting / replacement as new NPP are not foreseen. Any other plans will be dependent on political decisions.

CEA (France) - LBB is being considered for the safety demonstration of the European Pressurised Water Reactor (EPR) design and is planned to be used in the next fast breeder reactor, ASTRID.

AMEC (UK) - Continued analytical work in support of the further development of the high temperature creep LBB procedures and the evaluation of COAs for appropriate cylindrical geometries and relevant loading situations. Improved methods for evaluating leakage rate are also being planned.

AEKI (Hungary) - LBB is being reconsidered for use on new planned units, subject to regulatory agreement. A likely outcome will be that it will be used as a supplementary approach in a defence in depth approach.

UJV (Czech Republic) - LBB research funding levels have recently been reduced after the initial effort of creating the LBB methodology for application to VVERs. Recently, some attempts have been made on applying LBB to feed water and steam lines. LBB methodology for VVERs within the IAEA has been prepared (VERLIFE). Routine LBB application is still in progress. The main focus now is to improve existing information, i.e. the materials database.

TECNATOM (Spain) - The plan is to apply LBB to components which are not suitable for ISI but include Weld Overlay (WOL). This would require re-assessing LBB approaches used.

NRG (Netherlands) - In the framework of the R6 development programme, investigations on constraint effects on COAs are being undertaken. These developments are likely to be extended in the near future to other LBB related investigations.

ORNL (USA) - There is a plan to perform full probabilistic analyses of primary circuit and develop and validate the use of the xLPR computer code.

4.3.8 Summary of commonalities and differences

As detailed above, the main distinction between different countries is in the actual procedure adopted. The two main LBB procedures adopted are the BSC/IC of Germany [43], codified in [56] and SRP 3.6.3 [44] of the USA. The BPC/IC is further adopted by Spain, where the NPP is of German design, and the Netherlands, whereas SRP 3.6.3 is further used by the Czech Republic and Spain, where the power plant is of USA origin for the latter. The UK uses the R6 [7] assessment procedure and France includes LBB arguments within Appendix A16 of RCC-MRX [45], but there is no regulatory advice on its use. LBB approaches are currently not adopted in Hungarian NPPs.

All methods adopt a similar approach in making the basic case for an LBB argument. The argument simply states that a crack should be large enough so that the loss of fluid escaping the through wall crack can be detected, whilst remaining small enough that structural failure of the pipe does not occur. Clearly there are many assumptions which are used within this assessment which differ by country, some of which are dependent on the national regulatory position. Examples of such variations are the required margins between detectable crack size and critical crack size, detector requirements, flaw geometry and assumed flow rates.

Some further commonalities, which arise from the need to meet these stipulations, are that there is generally a minimum pipe radius requirement and a restriction to high pressure piping. This generally limits the use of LBB to the main coolant lines, surge lines, accumulator lines, the high pressure portion of ECCS, steam lines and feed-water lines . Note that these stipulations are implicit in R6 by meeting the requirements whereas the other procedures are explicit. There are also variations within these requirements, where the BSC/IC stipulates the smallest allowable pipe diameter (50 mm) .

The use of LBB in both the R6 and BSC/IC approaches share many similarities in that they are adopted as defence in depth approaches; i.e. the approach is not the main argument against failure but combined with a number of other assessments and assumptions makes the likelihood of failure acceptably low. This means that the use of deterministic calculations is acceptable, rendering probabilistic assessments unnecessary. The approaches can also be applied to a wider range of piping than the other approaches as both ferritic and austenitic piping can be considered, at a number of locations including welds and cladding. The R6 procedure is more general again as it allows for more complex scenarios, such as creep effects (as required for the UK Advanced Gas Cooled Reactors), and recommends the use of sensitivity analyses as opposed to specifying margins.

The SRP 3.6.3 LBB procedure is more rigorous than either the BSC/IC or R6 approaches, with the RCC-MRX approach falling somewhere between. The SRP 3.6.3 approach can not be applied to individual components or welds and can only consider cases where some additional failure mechanisms are excluded. The procedure also includes safety factors on the defect size and detection capability.

Historically, all of the approaches were developed through the 1970s and 1980s with initial versions of the existing procedures in place between 1979 and 1990. A number of programmes, computer codes and experiments performed during this development have been identified.

Current and future plans on LBB research are limited and primarily confined to the critical crack size assessment and crack opening area (COA) determination. Two additional research areas are identified as application to Dissimilar Metal Welds (DMWs) and Weld Overlays (WOLs), as well as probabilistic assessments, potentially allowing LBB approaches to be negated.

It is also clear that, especially in countries where LBB is not currently adopted, future plans include the use of LBB for new-build power plants.

4.4 Overview of fracture mechanics assessment methods applied to STYLE mock-up experiments

4.4.1 General

As previously noted, the STYLE project was largely focused on three large scale mock-up experiments (Mock-Up 1, Mock-Up 2 and Mock-Up 3). The mock-ups were representative of plant locations containing welds or cladding and contained an initial circumferential defect, either through-wall or surface breaking, and were loaded under four point bending conditions. The three different plant geometries represented by the mock-ups were 1) a dissimilar metal weld, 2) a repair weld, and 3) a clad ferritic pipe.

Benchmarks, based on each of these experiments were launched to investigate fracture assessment procedures used throughout Europe and how the application of these methods compare to each other and to the experimental data. The output information required from the benchmark were:

- Prediction of crack opening displacement (COD) values against applied bending moment (applied load).
- Prediction of crack initiation applied bending moment (applied load).
- Prediction of critical crack size at given value of applied bending moment (applied load).
- Prediction of limit load (limit bending moment) for the initial crack size.
- Evaluation of applied bending moment (applied load) versus various amounts of ductile tearing.

The following organizations took part: AREVA GmbH (Germany), CEA (France), AREVA-F (France), AMEC (UK), AEKI (Hungary), UJV (Czech Republic), TEC (Spain), and NRG (Netherlands). Not all organizations were engaged in every benchmark though.

4.4.2 Details of experiments

Details of the mock-up experiments on which the benchmarks were based, were as follows:

Mock-up 1
The test piece used for Mock-up 1 was a dissimilar metal TIG weld which joined an austenitic pipe with a ferritic one (Figure 12). The austenitic pipe was made from 316L steel, and the ferritic pipe was 18MND5. These were TIG welded together using inconel 52 weld metal, and the inside of the ferritic pipe was clad with 309L steel. Extension arms were welded on either end of the test piece to facilitate the loading of the test specimen under four point bending conditions.

The test piece had a wall thickness of 40.5 mm, an outer diameter of 352 mm and a length of 873 mm. The weld was deposited with 28 beads and the cladding on the inside of the ferritic portion of the test piece was 8.5 mm thick.

The extension arms were a sufficient length to allow four-point bending with an outer span of 7200 mm. The extension arms were different depending on which side of the test specimen they were attached to. On the ferritic side the extension arm had a wall thickness of 50 mm, an outer diameter of 355.6 mm and a length of 4000 mm. On the austenitic side the extension arm had a wall thickness of 65 mm, an outer diameter of 355.6 mm and a length of 3500 mm.

The specimen was machined and pre-cracked to include a through-wall defect with a central crack angle of 90°. The crack was produced on the interface between the inconel 52 and the 18MND5 ferritic materials.

Mock-up 2
The test piece used for Mock-up 2 was a similar metal circumferential butt weld with a repair (Figure 13). The pipe was made from Esshete 1250 steel, and the weld made from Esshete 1250 weld material. Extension arms were welded on either end of the test piece to facilitate the loading of the test specimen under four point bending conditions.

The test piece had a wall thickness of 35 mm, an outer diameter of 180 mm and a length of 600 mm. The repair weld had a depth equal to 70% of the wall thickness and a circumferential angle of 54.8°.

The extension arms were a sufficient length to allow four-point bending with an outer span of 5000 mm. The extension arms had a wall thickness of 40 mm, an outer diameter of 180 mm and a length of 3500 mm.

The specimen was machined and pre-cracked to include a through-wall defect with a central crack angle of 47.4°. The crack was produced in the centre of the repair weld.

Mock-up 3
The test piece used for Mock-up 3 was an internally clad ferritic pipe. The pipe was made from 20MnMoNi 5 5 material (Figure 14). Extension arms were welded on either end of the test piece to facilitate the loading of the test specimen under four point bending conditions.

The test piece had a wall thickness of 36 mm (including 5 mm of cladding), an outer diameter of 424 mm and a length of 525 mm.

The extension arms were a sufficient length to allow four-point bending with an outer span of 4000 mm. The extension arms had a wall thickness greater than the specimen, and an outer diameter of 424 mm.

The specimen was machined and pre-cracked to include a semi-elliptical defect with a central crack angle of 72° and an initial depth of 20.5 mm (15.5 mm beyond the cladding).

For each of the benchmarks, tensile properties were provided by way of stress-strain curves and fracture toughness properties were provided in the form of J-R curves.

4.4.3 Benchmark results

Mock-up 1
The methods and approaches used by the participants and the results obtained are compared in Tables 1 to 3. Results are also contained in Figures 17 to 20, whereby Figures 18 to 20 present the maximum and

minimum values evaluated by the various participants for crack initiation, critical crack size and limit load respectively. As would be expected, most of the participants used methods based on the codes and procedures of their relevant countries. In this regard, AMEC used the R6 method, and AREVA GmbH along with UJV used methods based on plastic collapse evaluation. In particular, AREVA GmbH applied two KTA methods based on two flow stress definitions, and UJV applied an approach based on Z-factors which are dependent on welding type.

For COD evaluation, an elastic approach was used by both AMEC and UJV. In these, Young's modulus for the weld metal was used, since it is conservative from a LBB point of view (higher Young's modulus value provides lower value of COD). CEA performed various calculations, considering different combinations of tensile and fracture properties. AREVA-F used FE modelling incorporating the actual material configuration of the DMW. UJV used conservative material properties aimed at producing a lower bound COD.

Concerning material assumptions for assessing limit load and critical crack size (specified to be determined for a bending moment value of 302.4 kNm which approximately corresponded to 5% of the experimental maximum capacity load), AMEC performed evaluations for several combinations of tensile and fracture material properties, UJV and AREVA GmbH used 316L material, which is in accord with their methodologies based on plastic collapse. AREVA-F used FE modelling and CEA's approach was based on A16 Appendix of the RCC-MRx code (for determination of critical crack size) [45]. Also, several combinations of tensile and fracture properties were considered in the CEA analyses.

For evaluation of crack initiation load, AREVA GmbH and AMEC both used the R6 method [7], AREVA-F used FE modelling, CEA used A16 Appendix of the RCC-MRx code and UJV used a simple method based on comparison of elastic stress intensity factor with initiation fracture toughness.

Residual stresses were generally only considered in the evaluations undertaken by AMEC.

Comparison of the COD results are provided in Figure 17. It can be seen that in the elastic region (corresponding to applied force up to approximately 170 kN), the evaluated COD versus Force curves (AMEC, UJV and AREVA-F) only differ to a small extent, the differences mainly being associated with the various formulations associated with the methods used. All these curves are conservative from a LBB point of view however, since they are lower than the experimental curve.

Regarding the evaluations of crack initiation load (Table 3 and Figure 18), the result of UJV (355.3 kN applied load) is seen to be the closest to the experiment (300 - 375 kN applied load). This good agreement between assessment and experiment was obtained despite the fact that a very simple method was used as described above. However, it may be noted that the LBB load safety margin of 1.4, as required by the UJV method, was not actually included in this assessment. It is interesting to note that if this margin were to be included, the initiation load would then be, F_{init} = 255 kN, a value close to the AREVA-F result of F_{init} = 261 kN. Values of crack initiation load obtained by AREVA GmbH cannot actually be directly compared with those of other partners since different tensile properties were actually used. The AMEC crack initiation load values, for the case of no residual stresses, vary in the range from 223 to 410.6 kN dependant on the combination of material properties used, and, for the case of when residual stresses were included, they vary in the range from 163 to 410.6 kN (again dependant on material property assumptions). The CEA initiation load results vary in the range from 87.5 kN to 204.8 kN dependant on the combination of tensile and fracture properties used. The value closest to the experimental result is 204.8 kN which was obtained when both tensile and fracture properties of the Inconel material were considered. From the results in general, it may be noted that using 316L tensile properties in the assessments generally resulted in initiation load significantly lower than the experimental value.

Table 1: Methods used in STYLE assessments (Mock-up 1)

	AMEC	AREVA GmbH	UJV	AREVA-F	CEA
COD	Linear elastic approach [52]	not performed	Linear elastic approach [53]	FE	A16 [45]
Crack initiation	R6 [7]	R6 [7]	One criterion approach	FE	A16 [45]
Critical defect size	R6 [7]	Plastic limit load (PLL), Flow stress concept (FSC) [36]	Engineering formulae based on plastic collapse approach [53]	FE	A16 [45]
Limit load for initial defect size	R6 [7]	Plastic limit load (PLL), Flow stress concept (FSC) [36]	Engineering formulae based on plastic collapse approach [53]	FE	A16 [45]

Table 2: Material assumptions used in STYLE assessments (Mock-up 1)

	AMEC	AREVA	UJV	AREVA-F	CEA
COD	Various evaluations	Not performed	316 L HAZ and Inconel 52 (same *E*)	Real configuration in FE.	Various evaluations
Crack initiation	Various evaluations	316L	18MND5	Real configuration in FE.	Various evaluations
Critical defect size	Various evaluations	316L	316L	Real configuration in FE.	Various evaluations
Limit load for initial defect size	Various evaluations	316L	316L	Real configuration in FE.	Various evaluations

Table 3: STYLE assessment results (Mock-up 1)

	Force vs. COD curve	Crack initiation load (kN)	Critical crack size (i.e. critical central angle) of through-wall crack at given load of **302.4 kN** (Degrees)	Limit load for initial crack size (kN)	Advanced EAM:	
					Applied load versus various amounts of ductile tearing (Yes/No)	Evaluation of residual stresses (Yes/No)
AMEC	Fig. 17	Range[7] 223.9 – 410.6[1] Range[7] 163.0 – 410.6 [2]	Range[7] 98.8 - 146.3[1] Range[7] 8.7 – 15.5[3] Range[7] 52.9 - 146.3[2]	Range[7] 219 – 541	Yes	Yes
AREVA GmbH	-	493[1] (not comparable) 192.5 (PLL)	119.7[5] 82.3[6]	280.6[5] 192.5[6]	No	Yes
UJV	Fig. 17	355.3	130.6	301.4	No	No
CEA	Fig. 17	Range[7] 87.5 – 204.8	101.7	Range[7] 192.6 – 235.4 (most likely, with crack growth)	No	No
AREVA-F	Fig. 17	261	178	429.8	No	No
Experiment	Fig. 17	300 - 375	-	435 (max. load, with crack growth)	not yet available	-

Notes: [1]Without residual stresses

[2]with trough-wall bending residual stresses

[3]considering membrane residual stresses equal to the yield stress of the base metal (ferrite HAZ)

[5] denotes FSC method

[6] denotes PLL method

[7] range is due to combinations of tensile and fracture properties of DMW materials, used in evaluations

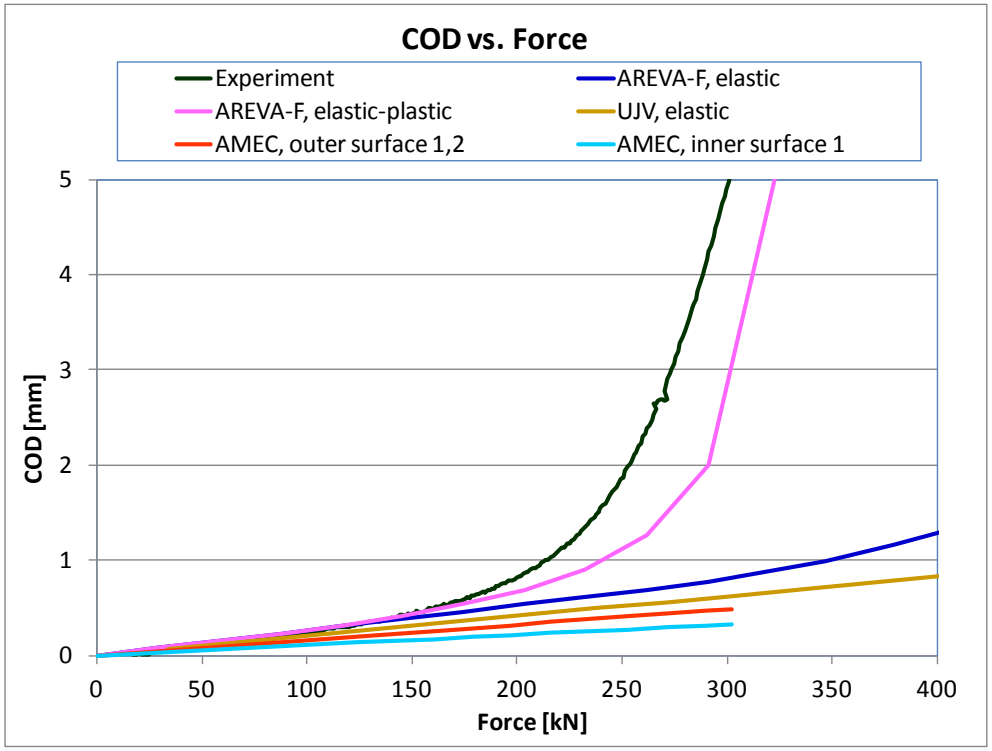


Figure 17: Comparison of COD assessments (Mock-up 1)

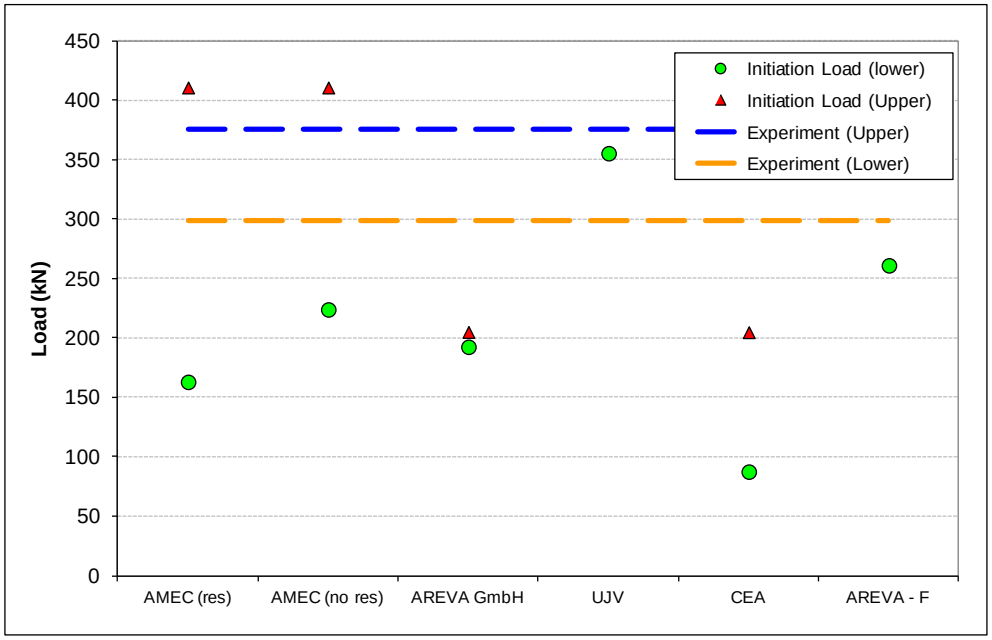


Figure 18: Maximum and minimum assessment values for crack initiation (Mock-up 1)

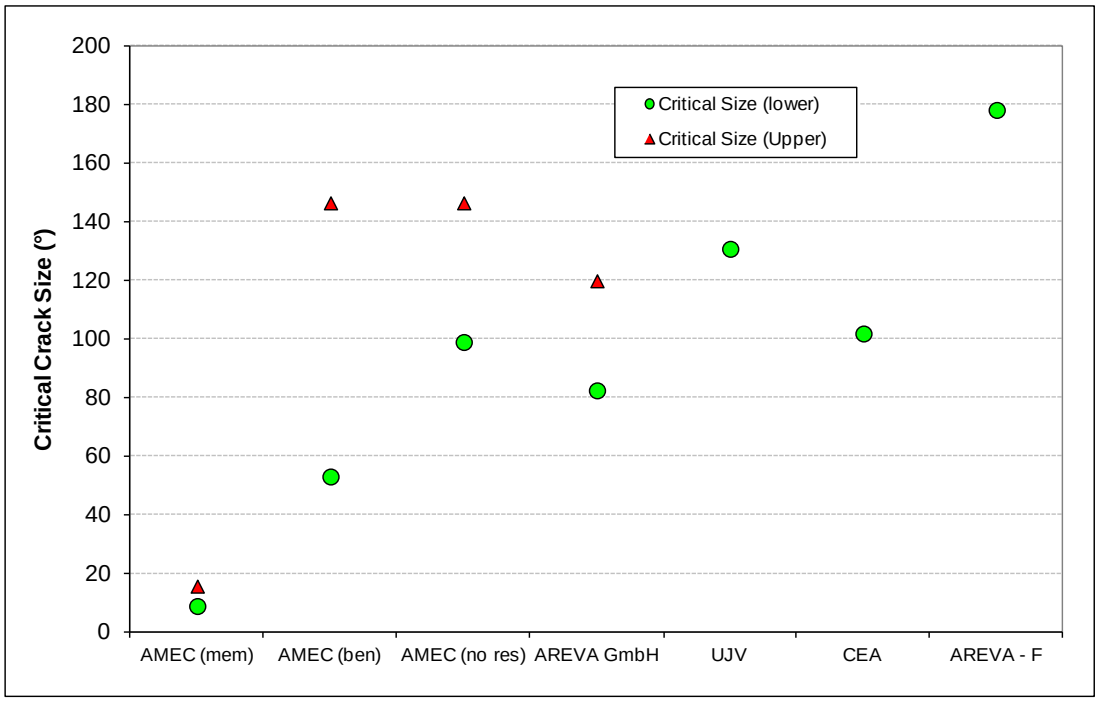


Figure 19: Maximum and minimum assessment values for critical crack size (Mock-up 1)

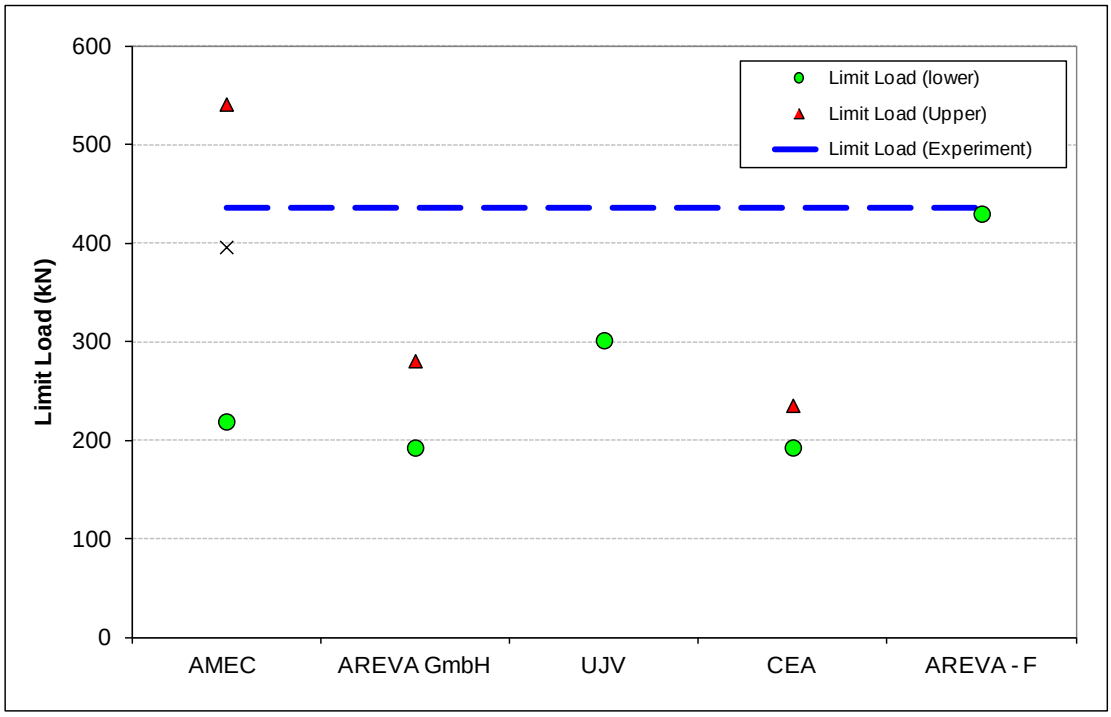


Figure 20: Maximum and minimum assessment values for limit load (Mock-up 1)

Significant scatter was also found in the critical crack size evaluations (Table 3 and Figure 19), which is again associated with the various combinations of tensile and fracture properties used in the calculations and assumptions made on residual stresses. When not taking residual stresses into account, the assessment values of all partners varied in the range from 98.8° to 178°. When taking residual stresses into account, the AMEC results varied in the range from 8.7° – 15.5°, if membrane residual stress was considered, and from 52.9 ° - 146.3 °, if through-wall bend residual stress was considered. Since the evaluations of critical crack size at a given load cannot of course be compared directly with experimental results, it is not really possible to make any definitive statement on the conservatism of the various methods considered.

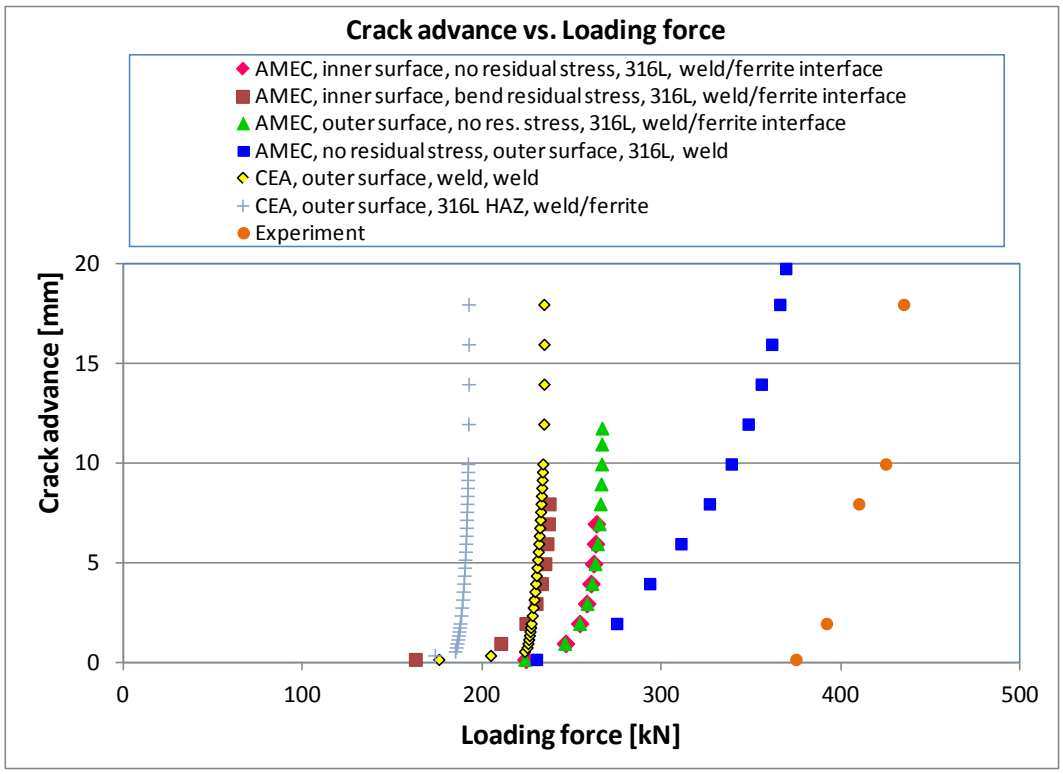


Figure 21 Ductile tearing assessment values (Mock-up 1)

On the limit load assessments (Table 3 and Figure 20), the result obtained by AREVA-F by finite element evaluations (429.8 kN applied load) is the closest to the experimental value of maximum load (435 kN). The second closest value to that of the experimental maximum load was that obtained by UJV giving a value of 301.4 kN. In this evaluation, the LBB safety margin of 1.4 was applied. AMEC and CEA provided a set of results dependant on the combination of tensile and fracture properties considered. Their results varied in the range of 219 – 541 kN (AMEC) and of 192.6 – 235.4 kN (CEA). The CEA assessments, however, were performed under assumption of a growing crack which would explain why the values are significantly lower than those of AMEC. It is noted though that the CEA values are much lower than the experimental maximum load.

Assessments of ductile tearing versus applied load were provided by AMEC and the following findings can usefully be noted (Figure 21): (1) at the inner surface, considering bending residual stress significantly reduces the applied load required for given amounts of ductile tearing; (2) when residual stresses are not included, the evaluations of crack growth significantly depend on assumed fracture properties; (3) the amounts of ductile tearing at the inner and outer surfaces are very similar when the same tensile and fracture material properties are used at both locations. The experimental results are also included in Figure 21 as are the finite element evaluations of CEA. The figure indicates that all the assessment

results are conservative with respect to the experiment results, with the CEA results being generally more conservative than the AMEC results.

Mock-up 2

The methods and approaches used by the participants and the results obtained are compared in Tables 4 to 7. Results are also contained in Figures 22 to 26, whereby Figures 24 to 26 present the maximum and minimum values evaluated by the various participants for crack initiation, critical crack size and limit load respectively. The methods used by the participants, AMEC, AREVA-GmbH and UJV, are the same as they used for the assessments of Mock-Up 1 highlighted above.

Table 4: Methods used in STYLE assessments (Mock-up 2)

	AMEC	AREVA GmbH	UJV
COD	Linear elastic approach [52]; Second approach: FEA	not performed	Linear elastic approach [53]
Crack initiation	R6 [7]; Second approach: FEA	R6 [7]	One criterion approach
Critical defect size	R6 [7]	PLL, FSC [36]	Engineering formulae based on plastic collapse approach [53]
Limit load for initial defect size	R6 [7]; Second approach: FEA	PLL, FSC [36]	Engineering formulae based on plastic collapse approach [53]

For the COD assessments, elastic approaches were used by both AMEC and UJV. In both cases, the range of validity of the formulae was exceeded due to the small R/t ratio of 2.07 for this case. It can be seen from Figure 22 that the COD results of AMEC (EAM) and UJV (EAM) are quite different from one another, bearing in mind that the ”AMEC, EAM, inner surface” line in Figure 22 practically coincides with the “UJV” line which actually relates to the outer surface. AMEC also provided evaluation of COD based on FE analysis, for both outer and inner surfaces and with and without residual stress included. From Figure 22 it can be seen that the AMEC EAM values are a little higher than AMEC FE results in the elastic region. This is not that surprising bearing in mind that the residual stresses were modelled in the FE analysis but they were not taken into account in the AMEC EAM assessments. With the exception of the AMEC EAM outer surface values, all COD evaluations are below the experimental COD values, at least in the elastic region, which is conservative.

Since experimental COD values were measured only at locations “South” and “North”, i.e. at locations of 12° away from the centre of the crack, the COD evaluations, which were performed for the centre location (0°), could not be compared to the experiment. Therefore, only FE predictions for the location of 12° away from the centre of the crack were compared with the corresponding experimental values (“South” location was selected). The result of the comparison is seen in Figure 23. It is seen from this figure that in the linear region, the COD assessment results are below the corresponding experimental values, i.e. they are conservative. For comparison, the COD vs. Force curve calculated for the centre location is also included

in this figure. Overall, the accordance between the FE analysis and experimental values of COD is fairly good.

Table 5: Material assumptions used in STYLE assessments (Mock-up 2)

	AMEC	AREVA GmbH	UJV
COD	Eshete parent	not performed	Eshete weld
Crack initiation	Fracture properties of Eshete weld, tensile properties on outer diameter both Eshete parent and weld.	Eshete weld	Eshete weld
Critical defect size	Fracture properties of Eshete weld, tensile properties on outer diameter both Eshete parent and weld.	Eshete parent	Eshete parent
Limit load for initial defect size	Fracture properties of Eshete weld, tensile properties on outer diameter both Eshete parent and weld.	Eshete parent	Eshete parent

Table 6: Influence of residual stress consideration in STYLE assessments (Mock-up 1)

	AMEC without/with residual stress	AREVA GmbH without/with residual stress	UJV without/with residual stress
COD	Not considered	Not performed	Not considered
Crack initiation load	142.7 kN/<0	138.7kN/25.8kN	Not considered
Critical defect size	47.5°/7.81°	Not considered as method not dependent on residual stress	Not considered as method not dependent on residual stress
Limit load for initial defect size	Not considered	Not considered as method not dependent on residual stress	Not considered as method not dependent on residual stress

Concerning evaluation of crack initiation load, there is good agreement between the AMEC assessment and AREVA-GmbH assessment values for when residual stresses are not taken into account (range of 135.6 kN – 142.7 kN). Comparing the AMEC FE results with the AMEC engineering assessment results, it is seen that the latter value is higher than the corresponding former value. This may be explained by the

fact that residual stresses were modelled in the FE analysis, while in the EAM approach, they were not taken into account (since as can be seen from Table 6, taking residual stresses as membrane yield in the evaluations was shown to result in crack initiation by such stresses alone). The UJV evaluated value of crack initiation load is very high (303 kN), which is potentially non-conservative. This high value is due to the fact that an elastic criterion was used in the evaluations by way of merely comparing elastic stress intensity factor against material fracture toughness (AMEC and AREVA-GmbH having used the elastic-plastic based R6 methodology). Comparison of evaluated crack initiation load with that of the experiment has not as yet been possible, since further post-processing of the experimental data is required.

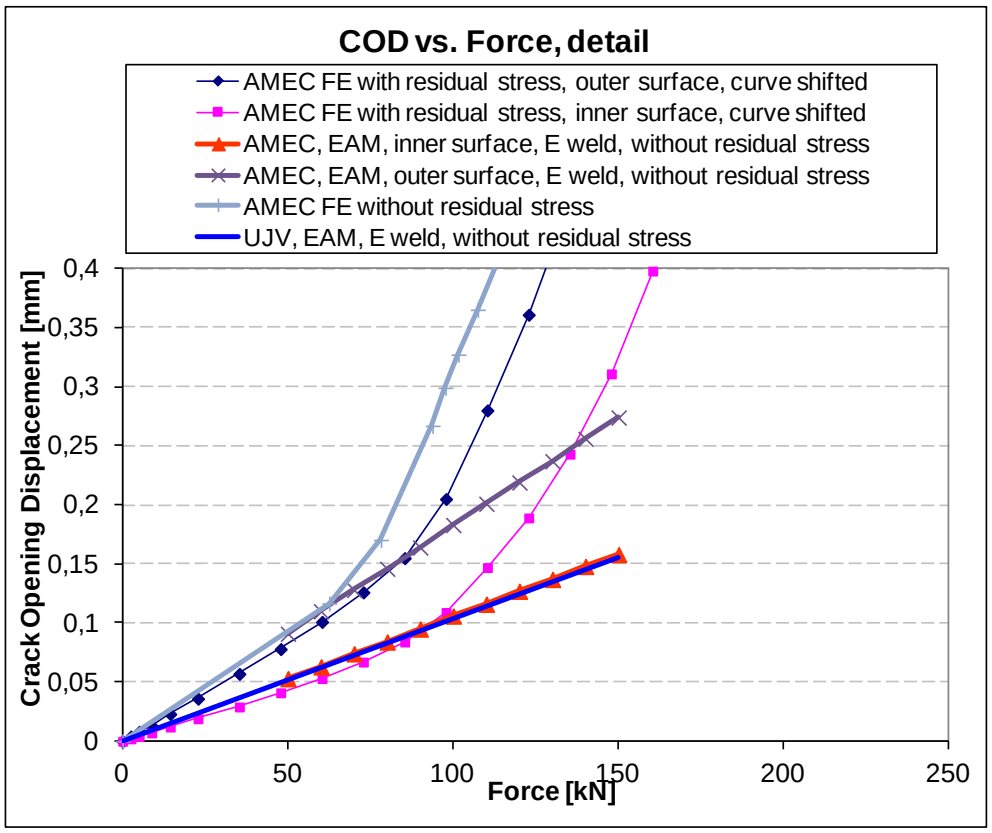


Figure 22: Comparison of COD assessments (Mock-up 2)

Concerning critical crack size evaluations (specified to be calculated for an applied bending moment of 150 kN/m), there is large scatter in the results. Ignoring the two highest UJV values (the first one is not conservative from an assessment point of view, and the second one is not realistic since the repair weld is not likely to be a TIG weld), the range of results can be considered not to be excessive (30.4 ° – 67.8 °). The lowest value was obtained by the PLL method, and the highest by the FSC method.

As far as the assessments of limit load for initial crack size are concerned, the scatter of results is similar to the critical crack size results. In general, quite a large scatter in the results was evident. However, if not considering the two highest UJV values (for the same reasons as above), then the scatter is within a reasonable band (135.6 kN – 180 kN). Again, the lowest value was obtained for the PLL method, and the highest value for the FSC method. Comparing the AMEC engineering assessment value (162 kN) with the AMEC FE value (129.5 kN) implies that the engineering assessment value is not conservative. While the reason for this difference requires a distinct explanation, it may be noted that the FE evaluation was based on a material with “equivalent tensile properties” (originally developed for the purpose of simulating the welding process to evaluate residual stresses) whereas the engineering assessment calculations were

based on parent material tensile properties. In comparing the calculated limit load values (based on the initial crack size) with experiment, it has to be remembered that the maximum load in the experiment was subsequent to ductile tearing having occurred meaning that the crack was longer than that assumed in the evaluations. However it may be noted that, with the exception of the highest UJV value obtained, all the calculated values of limit load were lower than the maximum load of 213.1 kN achieved in the experiment.

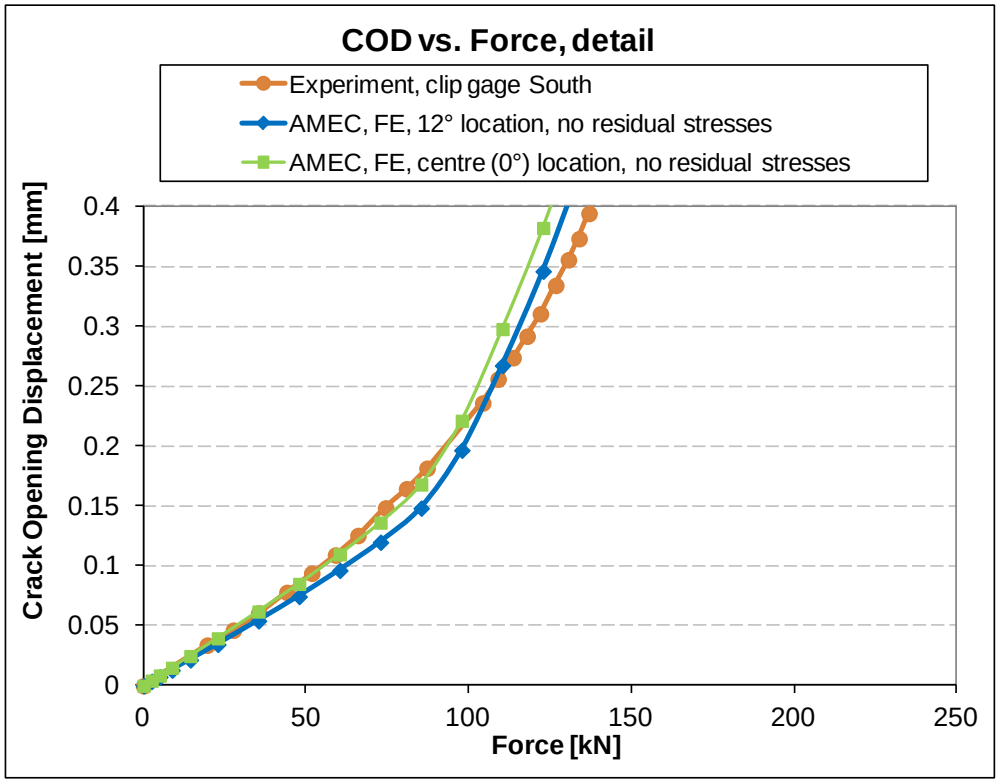


Figure 23: Comparison of COD values obtained by FE analysis (12° location and centre (0°) location, AMEC), with corresponding experimental values (measured at South clip gage location (12°)) (Mock-up 2)

In terms of evaluating amount of ductile tearing, only AMEC provided the results of such a calculation. Comparison of the results against experiment are provided in Figure 27. The comparison could be made for the outer surface only and merely one experimental point is currently available. However, it can be seen that the assessment results are conservative since ductile crack growth (tearing) has been evaluated for significantly lower load levels than occurred in the experiment.

Table 7: STYLE assessment results (Mock-up 2)

	Force vs. COD curve	Crack initiation load [kN]	Critical crack size (i.e. critical central angle) of through-wall crack at given load of **150 kNm** [Degrees]	Limit load for initial crack size [kN]	Advanced EAM:	
					Applied load versus various amounts of ductile tearing (Yes/No)	Evaluation of residual stresses (Yes/No)
AMEC	See Fig. 22	142.7, 197.4 (FEA)	47.5	162, 163.6 (FEA)	Yes	Yes
AREVA GmbH	No	138,7 (R6,), 25.76 (with residual stress), 135.6 (PLL)	67.8[1] 30.4[2]	180[1] 135.6[2]	No	No
UJV	See Fig. 22	303	118[3] 80.2[4] 55.0[5] 37.2[6]	264[3] 188.8[4] 157.3[5] 141,0[6]	No	No
Experiment	See Fig. 23	Not available	Crack angle at maximum load: 52°	213.1 (Maximum load)	Yes	-

Notes: [1] denotes FSC method,

[2] denotes PLL method,

[3] denotes assessment without any margin,

[4] denotes assessment for TIG weld and load margin 1.4,

[5] denotes assessment for SMAW and load margin 1.4,

[6] denotes assessment for SAW and load margin 1.4

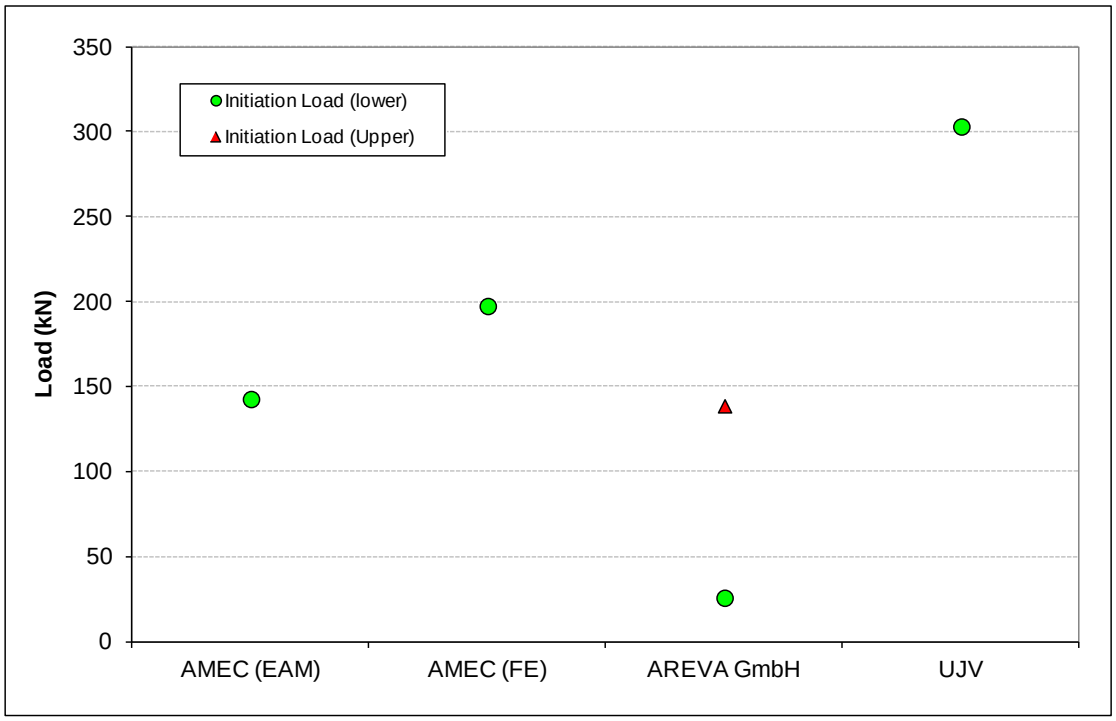


Figure 24: Maximum and minimum assessment values for crack initiation (Mock-up 2)

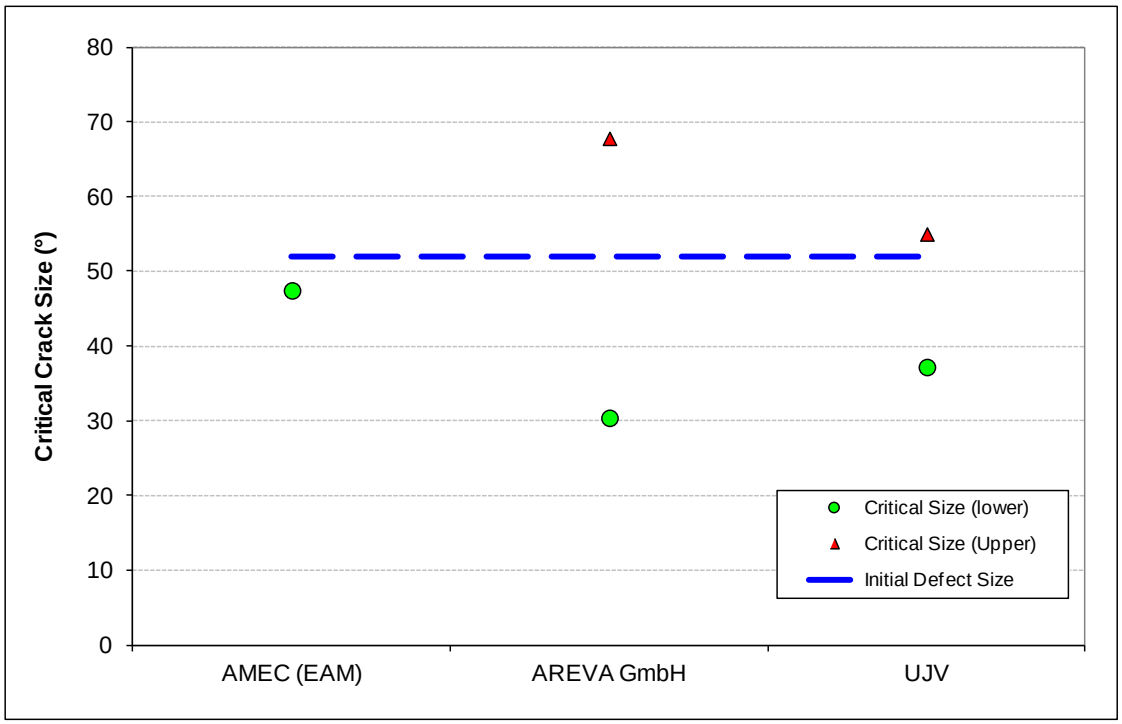


Figure 25: Maximum and minimum assessment values for critical crack size (Mock-up 2)

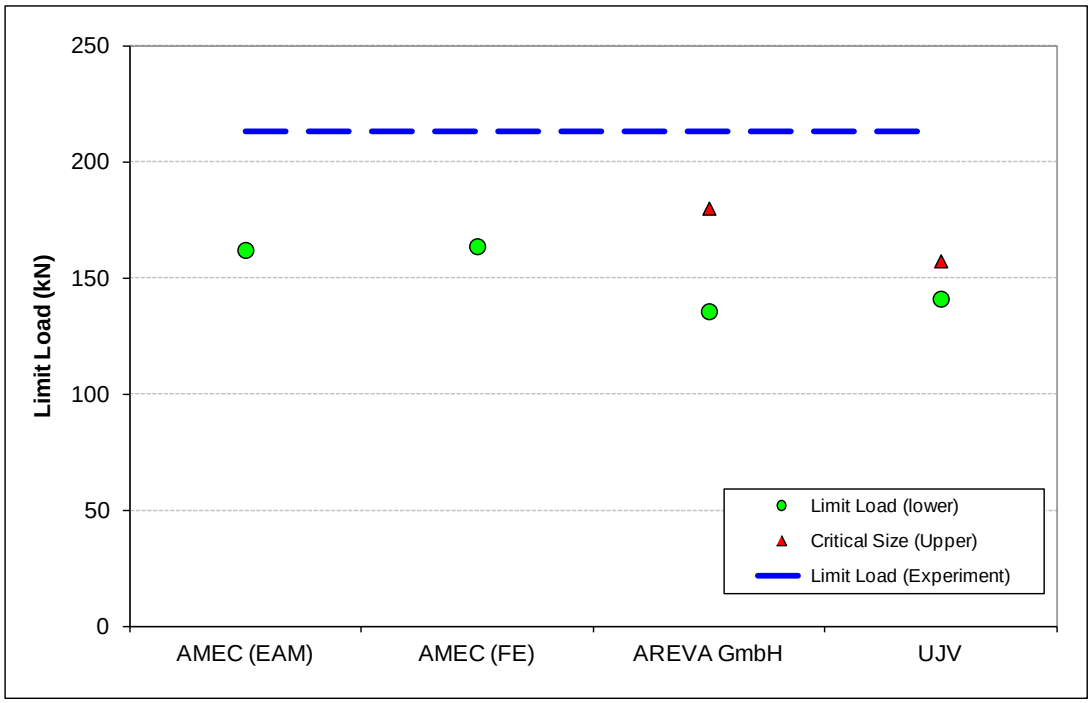


Figure 26: Maximum and minimum assessment values for limit load (Mock-up 2)

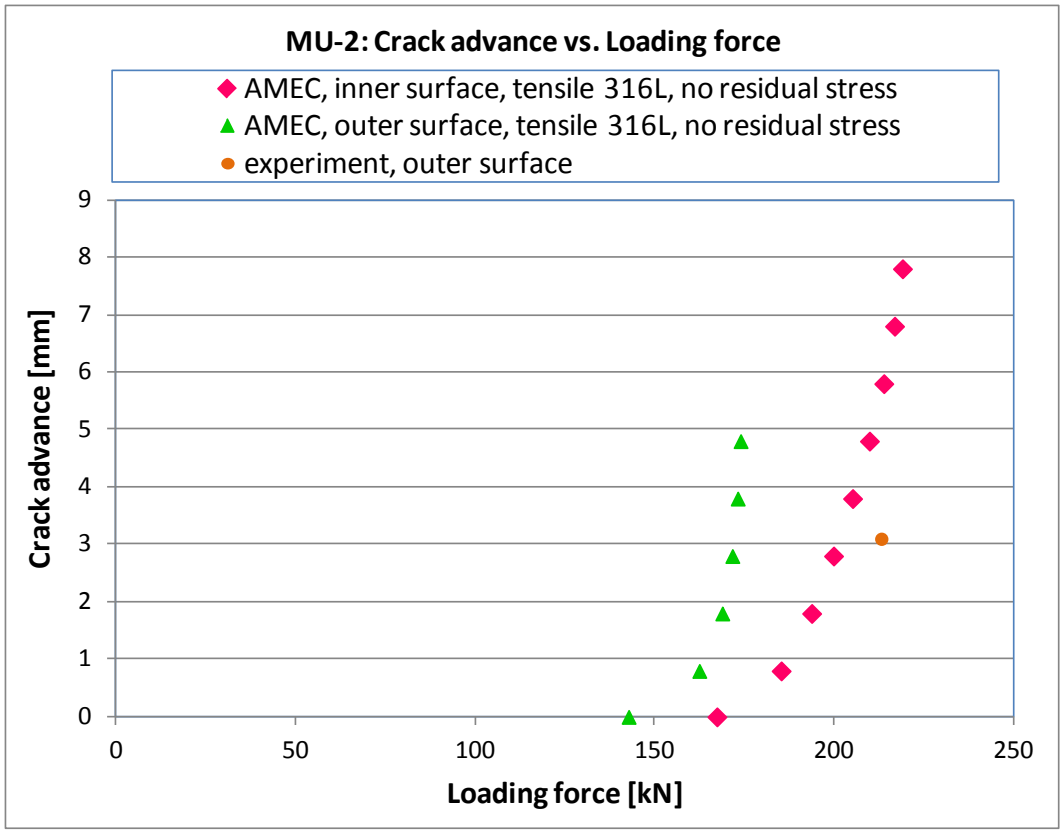


Figure 27: Ductile tearing assessment values (Mock-up 2)

Mock-Up 3

As in the case studies for the other mock-up experiments, AMEC generally used R6 [7] and considered engineering formulae by TADA et. al. [13] for the COD evaluation. AREVA GmbH mainly used methods based on two concepts of flow stress (FSC, PLL) [36] and also the R6 method. They also carried out detailed finite element analysis based on local approach methodology. NRG also used R6 and formulae of Connors [54]. CEA used methods contained in the A16 Appendix of the RCC-MRx code [45]. UJV used the Zahoor handbook [53]. A list of the approaches and methods used by the participants is presented in Table 8. Residual stresses were only included by AMEC in some of their evaluations.

Table 8: Methods used in STYLE assessments (Mock-up 3)

	AMEC	AREVA GmbH	NRG	CEA	UJV
COD	Tada et. al. [13]	Not performed	Not performed	A16 [45]	Not performed
Crack initiation	R6 [7]	R6 [7], PLL [36], FE Local Approach	R6 [7]	A16 [45]	Not performed
Critical defect size	R6 [7]	PLL, FSC [36], FE Local Approach	R6 [7]	Not performed	Zahoor [53]
Limit load for initial defect size	R6 [7]	PLL, FSC [36], FE Local Approach	Connors [54]	A16 [45]	Zahoor [53]

The results provided by individual partners for Mock-Up 3 are again compared with each other and with experiment in Table 9. Results are also contained in Figures 28 to 32, whereby Figures 30 to 32 present the maximum and minimum values evaluated by the various participants for crack initiation, critical crack size and limit load respectively.

In evaluating COD, two participants provided results based on EAM methods. AMEC performed elastic analysis, and CEA used the elastic-plastic approach based on A16 Appendix of the RCC-MRx code [45]. In addition, AREVA GmbH provided COD results based on elastic-plastic FE analysis. The comparison of the three evaluated curves and the experimental curve is provided in Figures 28 and 29. From these figures (mainly from Figure 29) it is seen that in the elastic region, all COD evaluations are lower than the experimental results, i.e. they are conservative from a LBB point of view. In the elastic region there is in fact only small scatter, the lowest values being those provided by AMEC. When considering the elastic-plastic evaluations, it can be seen that the AREVA GmbH FE calculations are in best agreement with the experiment which is what would be expected.

Table 7: STYLE assessment results (Mock-up 3)

	Force vs. COD curve	Crack initiation load (kN)	Critical crack size (i.e. critical central angle) of through-wall crack at given load of **2045.4 kN** [Degrees]	Limit load for initial crack size (kN)	Advanced EAM:	
					Applied load versus amount of ductile tearing (Yes/No)	Evaluation of residual stresses (Yes/No)
AMEC	Figs. 28, 29	3153[1] 3062[2] 3503[3] 3453[4]	45.9[1] 21.1[2] 83.3[3] 79.8[4]	3172	Yes	Yes
AREVA GmbH EAM	No	2707[5] 2714[6]	56.7[7] 76.9[6]	2411[7] 2714[6]	No	No
AREVA GmbH FE	Figs. 28, 29	3692	120	3809[8] 3355[9]	No	No
NRG	No	3266[10] 3543[11]	102[10] 115[11]	3445[10] 3820[11]	No	No
CEA	Figs. 28, 29	2350	No	3081		
UJV	No	No	102.6	3354	No	No
Experiment	Figs. 28, 29	3500		4100		

Notes: [1] denotes no residual stress consideration, ESIS J_Δa definition,

[2] denotes residual stress consideration , ESIS J_Δa definition,

[3] denotes no residual stress consideration, ASTM J_Δa definition,

[4] denotes residual stress consideration, ASTM J_Δa definition,

[5] denotes R6 method

[6] denotes PLL method

[7]denotes FSC method

In the AMEC evaluations, initiation fracture toughness was determined from the provided J-R curve information from both the methods outlined in the ESIS [17] and ASTM [18] testing standards. A significant difference was found between the AREVA GmbH EAM assessment of crack initation load (2707 kN) and the corresponding AMEC ones (3062 and 3503 kN, for the ESIS and ASTM definitions of initiation fracture toughness, respectively), although the R6 procedure was used in both cases. Another surprising result is that although the PLL (AREVA GmbH) may be expected to give a lower applied load value in comparison to the R6 method, in this case, the PLL and R6 methods provide practically the same result. Comparing the assessment results with the experimental value (3500 kN) it can be seen that the AMEC evaluations based on the ASTM initiation fracture toughness definition, are in very good agreement with the experiment (practically identical values were obtained, if residual stresses were not taken into account, and conservative evaluations were obtained, if residual stresses were considered). The NRG values (3266 kN if cladding is not considered and 3543 kN if cladding is considered) are also in relatively good agreement with the experiment (3500 kN), but with the latter value (3543 kN) being slightly non-conservative. NRG actually used initiation fracture toughness according to the ASTM standard. That is to say that their assessment should be comparable with that of the relevant AMEC evaluation, which is reasonably confirmed (AMEC value of 3503 kN when excluding residual stresses compared to the NRG values of 3266 kN and 3543 kN).

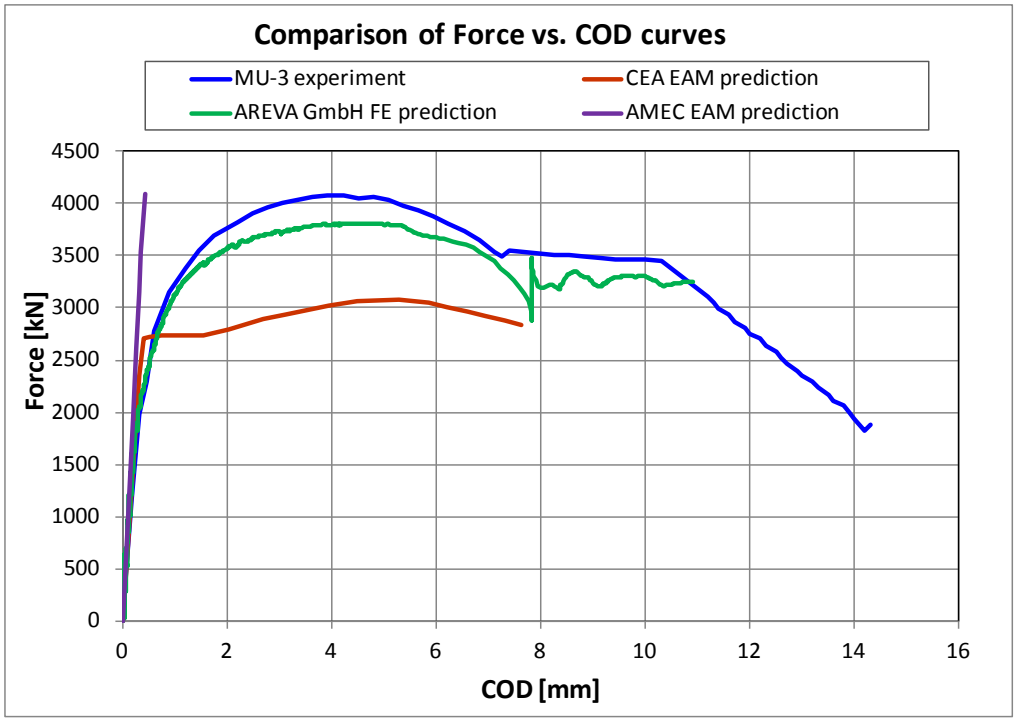


Figure 28: Comparison of COD assessments (Mock-up 3)

For evaluations of critical crack angle at given value of applied load (specified as 2045.4 kN, which corresponds to approximately 50% of the experimental maximum capacity load), a certain amount of scatter in the results is seen from Table 7 (even disregarding the scatter in results of AMEC variant calculations). If not taking residual stresses into account, the corresponding critical crack angle range is 45.89° - 120°.

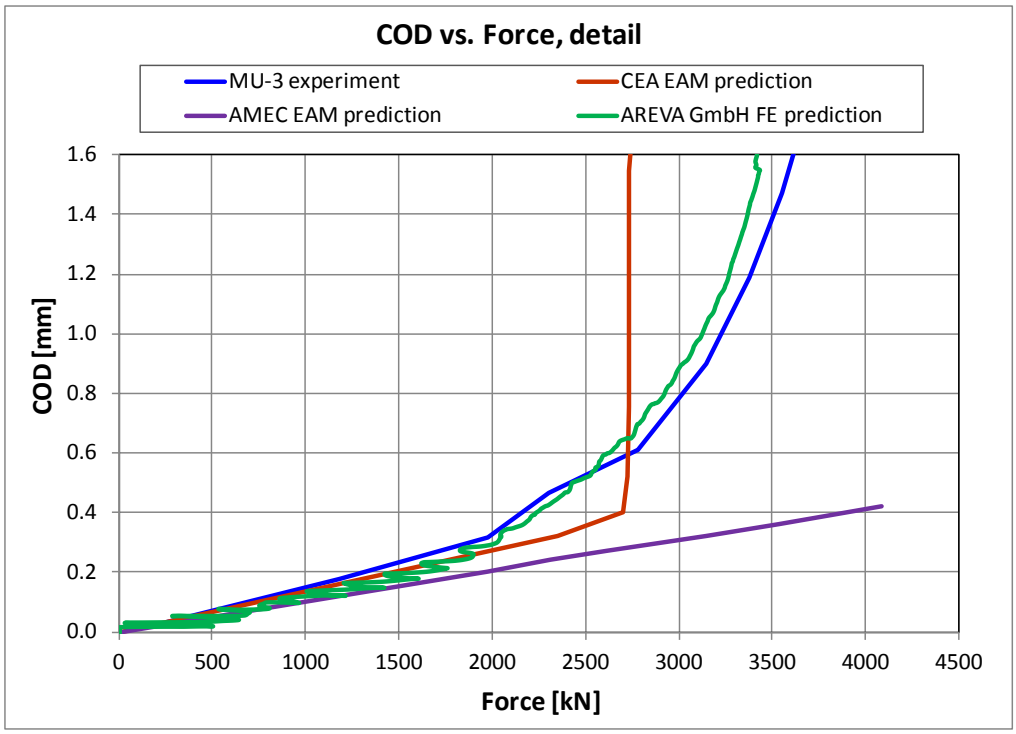


Figure 29: Comparison of COD assessments for small COD values (Mock-up 3)

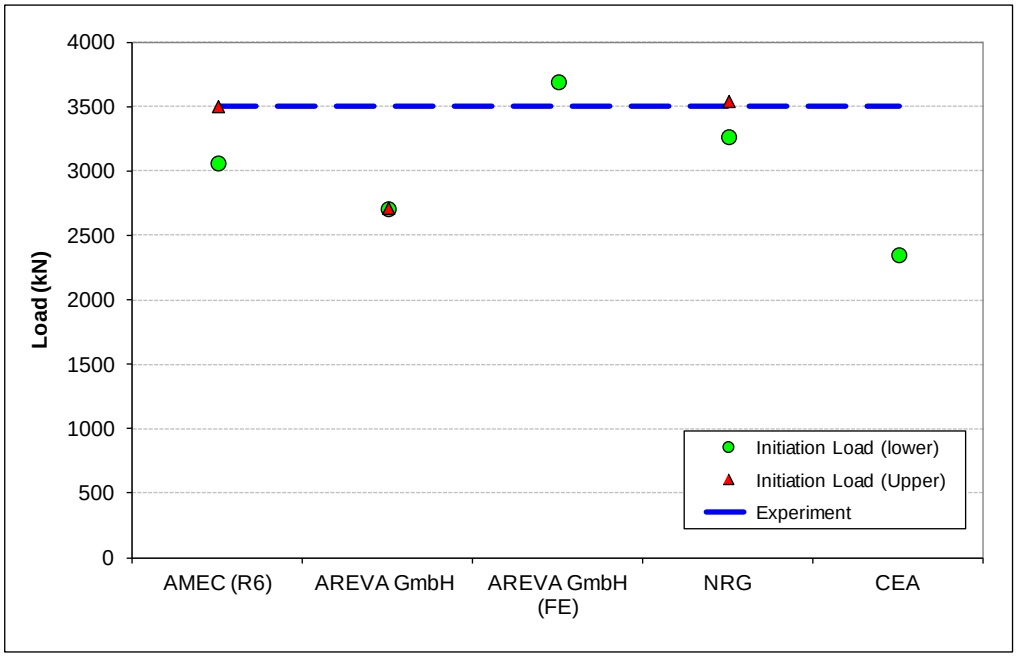


Figure 30: Maximum and minimum assessment values for crack initiation (Mock-up 3)

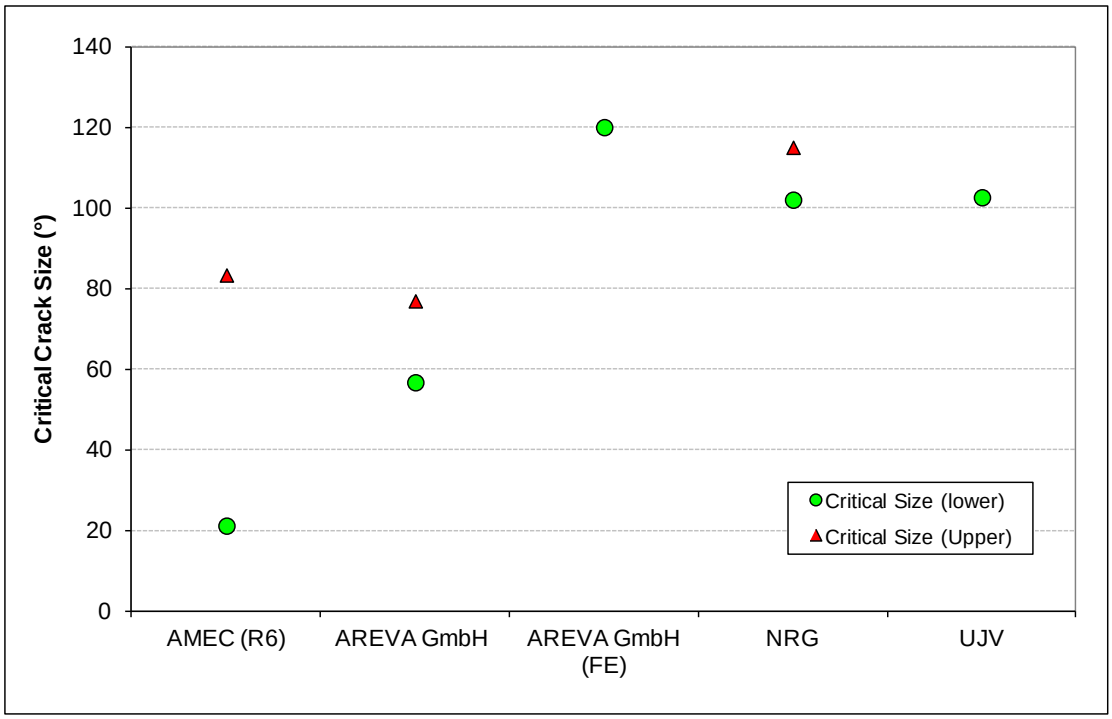


Figure 31: Maximum and minimum assessment values for critical crack size (Mock-up 3)

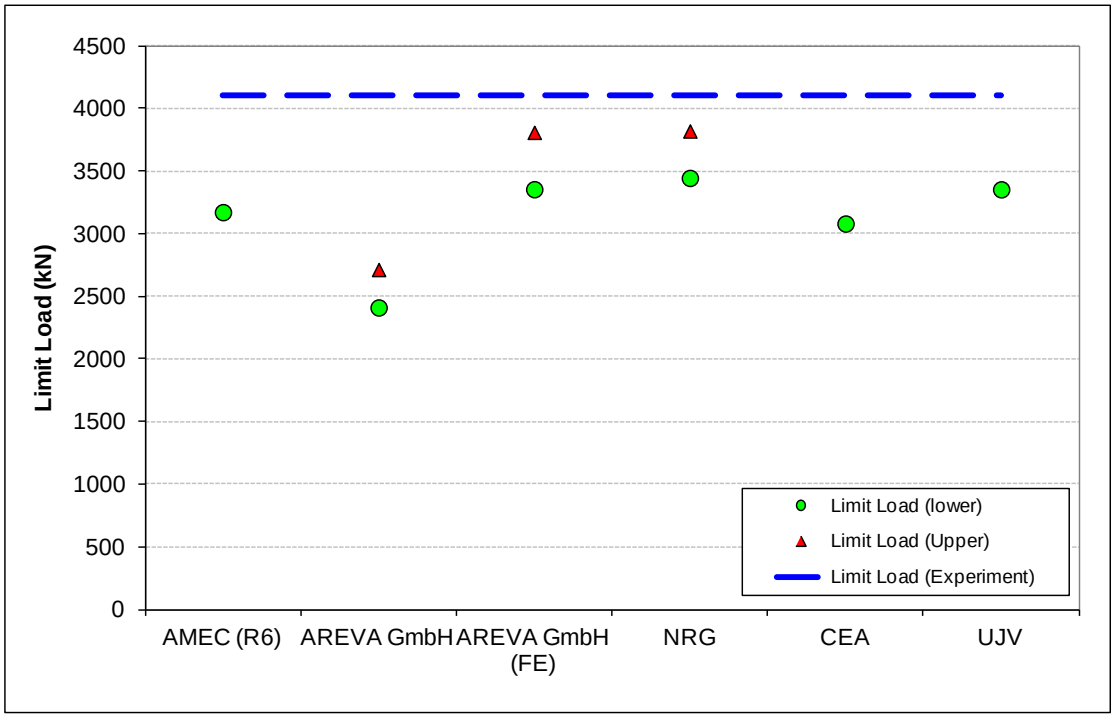


Figure 32: Maximum and minimum assessment values for limit load (Mock-up 3)

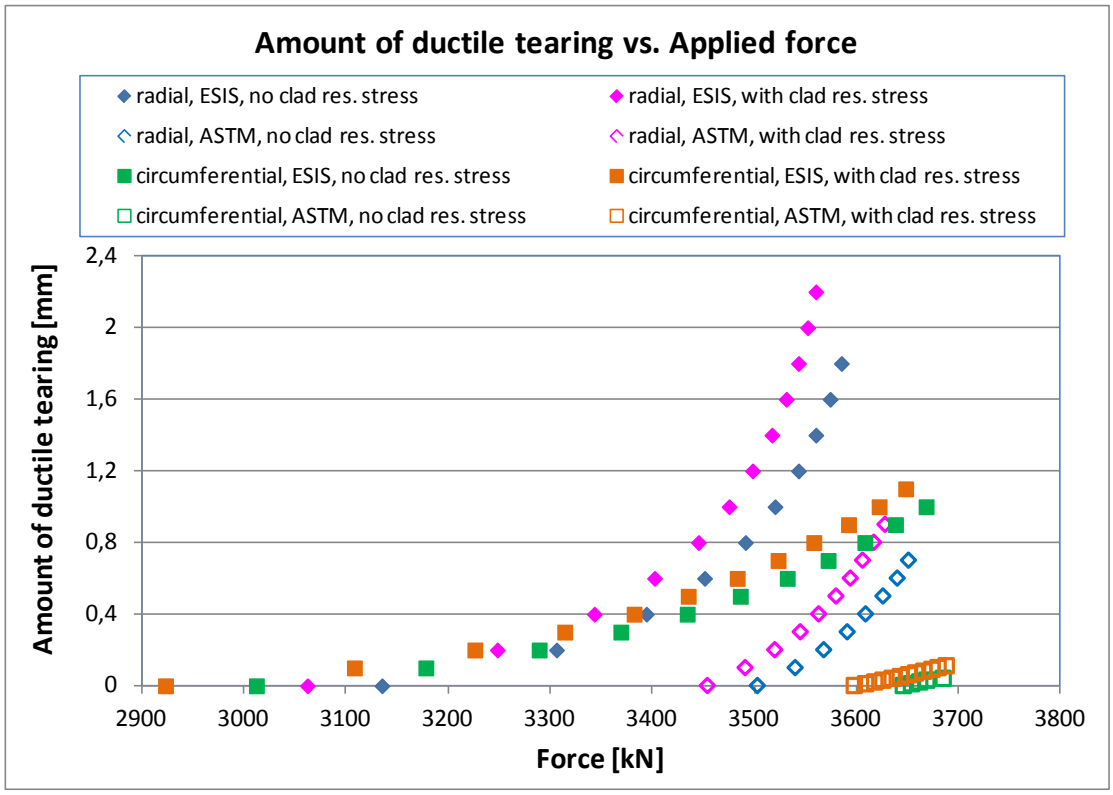


Figure 33: Ductile tearing assessment values (Mock-up 3)

Evaluations of limit loads do not exhibit significant large scatter. As expected, the AREVA GmbH calculations (2411 and 2714 kN, for FSC and PLL methods respectively) are the most conservative ones (i.e. lowest values), and the FE based evaluations by AREVA GmbH (3809 and 3355 kN) are in best agreement with the experimental value (4100 kN). It is also evident that the NRG value obtained with taking cladding into account (3820 kN) is quite close to the experimental value. All values are conservative with respect to the experiment.

Evaluation of crack growth versus applied load was performed only by AMEC (Figure 33). The evaluations correctly indicated crack growth would occur initially in the radial direction. The evaluations based on the ESIS definition of initiation fracture toughness are more conservative than those based on the ASTM definition as would have been anticipated. Direct comparison of crack growth evaluations with experiment was unfortunately not possible, since experimental data on crack growth had not been available for this mock-up.

<u>General Summary</u>

The results of the evaluations performed by the participating organisations have generally been shown to be conservative with respect to the respective three Mock-Up experimental results, particularly for evaluating such aspects as Crack Opening Displacement and applied load for initiation of tearing . Not surprisingly, the input data which can have a significant influence on the assessment results are the tensile and fracture properties used and various assumptions on weld residual stresses.

5 Conclusions

5.1 Relating to general aspects

The following conclusions can be drawn from the information contained above in relation to general aspects of fracture mechanics:

- There are considered to be no specific issues in relation to **defect characterisation**, including re-characterisation rules for multiple defects. In relation to the latter, the requirement to assess multiple defects lying in close proximity to each other is not thought to be great anyway.

- Although there are some differences in **stress intensity factor solutions** between the various codes, there is generally no major concern regarding being able to evaluate this parameter with a reasonable degree of accuracy. The extension of the solutions available in the various codes and procedures to include more geometrical features, such as attachments and further nozzle cases would likely prove advantageous however.

- In some circumstances it may be advantageous to use mode II and mode III fracture and fatigue crack growth properties, rather than mode I, which could be excessively conservative. With this aim in mind, the development of unified European guidance on **mixed mode loading** could usefully be considered.

- Unified European guidance on **weld residual stresses** could usefully be developed. This should be heavily focused on a more scientific approach, taking into account the reliability of methods and techniques used to generate experimental data such that a statistical approach can be applied to remove outliers and move towards more realistic profiles being developed. The guidance should also include the effects of PWHT, mechanical treatments and load history and temperature effects.

- It would be advantageous for unified European guidance to be developed on **characterisation of stresses** (into primary and secondary). This could usefully include rules in order to establish the extent to which elastic follow-up is significant.

The following conclusions can be drawn from the information contained above in relation to fracture:

- It would be advantageous for unified European guidance to be developed on **plastic collapse solutions**. This would include guidance on “global” and “local” solution considerations, extending current available solutions to more complex geometries (nozzles, attachments bends etc.), extending solutions for combined loading (e.g. membrane plus global bending) and the extension of current mis-match solutions.

- Unified guidance on the general mechanics of undertaking **ductile tearing analyses** should not be difficult to achieve. The development of unified European guidance on such aspects as how much tearing can be considered and under which loading levels may be much more difficult however because of the differing regulatory views and requirements in the different countries. One particular aspect of tearing analyses where unified guidance would be beneficial is the degree to which a J-R curve can be extended beyond the J-controlled limits as specified by the materials fracture toughness testing codes.

- There has been significant work undertaken in recent years relating to **crack-tip constraint** aspects and many countries have applied such methodology albeit mainly, but not exclusively, in R&D projects and studies. Unified detailed guidance could usefully be developed that would include a testing procedure for evaluating fracture toughness under low crack-tip constraint conditions (e.g. shallow cracked bend specimens), and on ensuring that the “failure” curve is constructed so as to be unique for a given material and temperature irrespective of whether the

loading in the structural component is of primary stress, secondary stress or a combination of the two.

- Whilst further developments in the treatment of **combined primary and secondary stresses** tend to be specific to individual national codes and procedures, results of supporting analytical (mainly finite element analysis) and experimental studies could usefully be shared in a unified way across EU organisations, particularly for validation purposes for their own developments in this area. Aspects where further development work could usefully be undertaken and unified guidance provided are on load order effects and the ability to be able to take elastic follow-up into account.

- Developments on the fracture assessment of **non crack-like defects** (e.g. lack of fusion, porosity, mechanical damage and design features) could usefully be extended to the European level. For some types of non-sharp defects, developments in NDE methods will inevitably be required before the procedures can be used with full confidence.

The following conclusions can be drawn from the information contained above in relation to fatigue crack growth:

- There is much scope for unified European guidance to be developed on **counting, combining and applying transient cycles**, including consideration of the position on the structural component where the stresses should be located in order to establish stress ranges.
- Further work and consideration of **crack closure effects** based on modifications to the Paris Law relationship could usefully be undertaken at the European level in order for unified guidance to be developed.

- European-wide guidance on fatigue crack growth could usefully include consideration of the threshold and near final failure regions so that the **low and high ΔK regimes** can be adequately taken into account.

5.2 Relating to RPVs

The following conclusions can be drawn from the information contained above in relation to RPVs with respect to fracture mechanics:

- Whilst it is probably true to say that **Pressurised Thermal Shock (PTS)** procedures and guidelines applied in different countries are generally equivalent to one other, there will inevitably be differences at the detailed level, some of these differences being associated with specific regulatory requirements. It would thus be of benefit for such differences to be properly understood and evaluated with a view to eventually establishing unified European guidance on the subject.

- There has been a significant amount of experimental work over the years to demonstrate **Warm Pre-Stress (WPS)** effects and the various models have generally been well developed and validated. Open questions are more data on irradiated materials and component like tests to assure the transferability of the results coming from specimens to the components.

- There is currently no clear consensus in Codes and Standards for assessing the influence of **austenitic cladding** in RPV assessments and this is an area where the development of suitable guidance at the European level would be advantageous.

5.3 Relating to piping and associated components

The following conclusions can be drawn from the information contained above in relation to piping and associated components, particularly associated with various aspects coming out of the STYLE European project with respect to fracture mechanics:

- For assessments in general relating to structural integrity evaluations of dissimilar metal welds, repair welds and clad ferritic pipes, several commonalities and differences are evident. The commonalities include the use of a commonly adopted assessment code or methodology, usually supplemented by detailed finite element analyses, the use of conservative material properties and in the case of clad pipes, excluding the actual cladding in the evaluations which is considered to be conservative. The differences include the definition of flow stress, the provision of guidance for treating weld residual stresses and the incorporation of higher level methodology such as weld strength mis-match and crack-tip constraint effects.
- For LBB assessments, the main distinction between different countries is in what procedure is adopted. The two main LBB procedures adopted are the LBB procedure as a part of the Break Preclusion Concept/Integrity Concept (BSC/IC) of Germany and SRP 3.6.3 of the USA. LBB procedures contained in the R6 and RCC-MRX procedures are also used, but only generally in the UK and France, respectively. Whilst there are several commonalities and differences in the detail, all the procedures adopt a similar approach in making the basic case for an LBB argument. The argument simply states that a crack should be large enough so that the loss of fluid escaping the through wall crack can be detected, whilst remaining small enough that structural failure of the pipe does not occur.
- Deterministic assessment methods for evaluating fracture conditions in complex geometries like dissimilar metal welds, repair welds and clad ferritic pipes can provide a variety of results depending on the method and the selection of input data used. The assessment results have generally been shown to be conservative with respect to the Mock-Up experimental results, particularly for evaluating such aspects as Crack Opening Displacement and applied load for initiation of tearing. Not surprisingly, the input data which can have a significant influence on the engineering assessment results are the tensile and fracture properties used and various assumptions on weld residual stresses. Where possible, it is therefore useful for sensitivity studies to be included in fracture mechanics (including LBB) assessments in order to be able to evaluate the significance of the results of varying the input data, particularly for defects located in a region of composite material and/or of significant residual stress.
- Supplementary assessments, finite element analyses and analysis of the STYLE Mock-up experimental data ideally need to be undertaken in order to pave the way for developing general unified guidance for undertaking structural integrity assessments in complex geometries like dissimilar metal welds, repair welds and clad ferritic pipes. In association with the developed guidance, work is required in order to be able to recommend conservative (but not too conservative) weld residual stress profiles for the structural features under consideration.

6 Recommendations

6.1 Relating to general aspects

The following recommendations are made in relation to general aspects of fracture mechanics:

- The extension of **stress intensity factor solutions** available in the various codes and procedures to include more geometrical features, such as attachments and further nozzle cases should be considered with a view to developing a European consensus of the most appropriate solutions to use.
- The development of unified European guidance on **mixed mode loading** should be considered.

- Unified European guidance on recommended **weld residual stress** profiles should be developed, including a more statistical approach and the consideration of PWHT, mechanical treatments and load history and temperature effects.

- Unified European guidance should be developed on **characterisation of stresses** (into primary and secondary), particularly with regard to being able to establish the extent to which elastic follow-up is significant.

The following recommendations are made in relation to fracture:

- Unified European guidance should be developed on **plastic collapse solutions** to include guidance on "global" and "local" solution considerations, solutions for complex geometries, combined loading and strength mis-match solutions.

- Unified European guidance should be developed on undertaking **ductile tearing analyses** which should include the degree to which a J-R curve can be extended beyond the J-controlled limits as specified by the materials fracture toughness testing codes.

- Unified European guidance should be developed on **crack-tip constraint** that should include a testing procedure for evaluating fracture toughness under low crack-tip constraint conditions and on ensuring that the "failure" curve is constructed so as to be unique for a given material and temperature irrespective of whether the loading in the structural component is of primary stress, secondary stress or a combination of the two.

- Further development work on **combined primary and secondary stresses** should be undertaken and unified guidance provided, particularly with regards to load order effects and the ability to be able to take elastic follow-up into account.

- Developments on the fracture assessment of **non crack-like defects** should be extended to the European level. For some types of non-sharp defects (e.g. lack of fusion and porosity), developments in NDE methods are required before the procedures can be used with full confidence.

The following recommendations are made in relation to fatigue crack growth:

- European guidance should be developed on **counting, combining and applying transient cycles**, including consideration of the position on the structural component where the stresses should be located in order to establish stress ranges.
- Further work and consideration of **crack closure effects** based on modifications to the Paris Law relationship should be undertaken at the European level in order for unified guidance to be developed.

- European-wide guidance on fatigue crack growth should include consideration of the threshold and near final failure regions so that the **low and high ΔK regimes** can be adequately taken into account.

6.2 Relating to RPVs

The following recommendations are made in relation to RPVs:

- Differences in **Pressurised Thermal Shock (PTS)** procedures and guidelines applied in different countries should be properly understood and evaluated and unified European guidance on the subject should be established.

- Further studies on **Warm Pre-Stress (WPS)** effects should include situations for longer life time of the RPVs using material for end of life conditions.

- Suitable guidance at the European level should be developed on assessing the influence of **austenitic cladding** in the assessment of RPVs (and other clad piping components).

6.3 Relating to piping and associated components

The following recommendations are made in relation to piping and associated components:

- Where possible, sensitivity studies should be included in fracture mechanics (including LBB) assessments in order to be able the evaluate the significance on the results of varying the input data, particularly for defects located in a region of composite material and/or of significant residual stress.

- Supplementary assessments, finite element analyses and analysis of the STYLE Mock-up experimental data need to be undertaken in order to pave the way for developing general unified guidance for undertaking structural integrity assessments in complex geometries like dissimilar metal welds, repair welds and clad ferritic pipes.

- In association with the developed guidance, work needs to be undertaken in order for conservative (but not too conservative) weld residual stress profiles to be recommended for the structural features under consideration.

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