SQEP Expectations Guide: T73S02 R6 Low Temperature Fracture Assessment

Last Update: 6th December 2012

References to R6 relate to Revision 4 with Amendments up to August 2007

Linear Elastic Fracture Mechanics (LEFM)

1.1 Describe the stress and strain fields near the tip of a linear elastic crack in terms of the stress intensity factor (SIF or K) and the distance from the tip (r).

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes13.pdf

The minimum expectation is that the Mentee will know that the stress sufficiently near the crack tip is proportional to $\frac{K}{\sqrt{r}}$. It is not unreasonable to expect the Mentee to know the actual value of the y-stress directly ahead of a crack in Mode I, i.e. $\sigma_y = \frac{K}{\sqrt{2\pi r}}$. The Mentee should be aware of the convention regarding the local x,y axes (i.e. x = direction in which the crack is pointing; y = the direction normal to the crack plane). This is important if the usual expression for J is to be understood (later). The Mentee should appreciate that the complete expressions for the LEFM fields involve angular dependence. As regards the strain fields, the Mentee should appreciate that these simply follow from the stress fields and Hooke's Law (in 3D) since linear elasticity is being assumed. They should really know what the 3D formulation of Hooke's Law is, at least roughly (though this is really the subject matter of T72S01).

Memorising the LEFM angular functions is not required. Being able to derive the LEFM fields is not required. However, I would expect anyone worth their salt to be able to give a rough indication of one of the methods by which they could be derived (i.e. they should be able to convince the Mentor that they had at least read through such a derivation). For derivations see http://rickbradford.co.uk/DerivationofLEFMFields.pdf.

1.2 State the base formulae for K in terms of applied stress and crack length for an embedded crack and an edge crack. Define the "compliance factor" or normalised SIF.

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes13.pdf

and http://rickbradford.co.uk/T73S02TutorialNotes14.pdf

Anyone who cannot immediately write down $K = \sigma\sqrt{\pi a}$ and $K = k\sigma\sqrt{\pi a}$ cannot be SQEP in this area. The Mentee should realise that the former applies to an embedded crack in an infinite plate, for which the crack length is 2a. The Mentee should know that, for an edge crack in a semi-infinite plate, the crack depth is 'a' and k ~ 1.12. (Both of these for uniform stress, of course).

The Mentee should realise that $K = k\sigma\sqrt{\pi a}$ is merely a convenient dimensionless form in which to express a stress intensity factor (SIF) solution, i.e. $k = K / K_0$ where $K_0 = \sigma\sqrt{\pi a}$. The Mentee might not be so old fashioned as to refer to k as a 'compliance factor', but s/he should appreciate that more compliant geometries are likely to lead to larger k. The Mentee might be asked to judge whether k is likely to be larger for an extended circumferential crack in a cylinder or for an extended axial crack in a cylinder, other things being equal.

The Mentee should be able to draw a qualitative graph of k versus normalised crack depth, a/t, for a crack in a wall of finite thickness, t. S/he should appreciate that the generic behaviour is that k is divergent as $a \to t$ (in cases where there is net load). A more challenging area of discussion is the behaviour of k under wall-bending. Whilst bending of plates (i.e. 2D geometries) conforms to the generic behaviour, i.e. divergent as $a \to t$, this is not the case for axisymmetric wall bending. Instead $k \to 0$ as $a \to t$. Engaged Mentees should appreciate that this is a consequence of axisymmetric wall bending stresses being self-equilibrating (secondary). Real show-offs might mention asymptotic methods, Bentham & Koiter, etc – as discussed in the context of deep cracks in cylinders here http://rickbradford.co.uk/DeepCracks1.pdf and here http://rickbradford.co.uk/DeepCracks2.pdf (the solutions due originally to Mike Heaton).

1.3 Describe the three modes of stress intensity factor (K_I, K_{II}, K_{III})

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes13.pdf

The minimum expectation is to be able to provide the definitions in terms of normal stressing, in-plane shear stressing and out-of-plane shear stressing respectively. The Mentee should also realise that the exact solution for an edge crack in a semi-infinite plate, under either Mode II or Mode III uniform shear, is $\tau\sqrt{\pi a}$. The physical reason why there is no k ~ 1.12 compliance factor in the case of shear would make an interesting discussion point. The Mentee should be able to deduce which of the Modes applies when presented with a given geometry under a given loading. Hence, s/he should recognise that a circumferential crack in a torque shaft is Mode III.

1.4 Define the energy release rate (G) and state the relationship between K and G in LEFM and for mixed mode loading

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes15.pdf

The definition of G can be restricted to the special cases of pure load controlled loading and pure displacement controlled loading. The Mentee should know that G is the 'rate' of decrease of the total strain energy with respect to crack area in the case of displacement control, $G = -\left.\frac{dU}{dA}\right)_D$. The Mentee should know that G is the 'rate' of increase of the total strain energy with respect to crack area in the case of load control and linear elastic behaviour, $G = \left.\frac{dU}{dA}\right)_F$. The Mentee should be able to illustrate the energy release on a graph of load-displacement for the structure. The more complete definition of G is dealt with later (2.2).

The Mentee should be able to write down $EG = \kappa\left(K_I^2 + K_{II}^2\right) + (1+\nu)K_{III}^2$, where $\kappa = 1$ if the in-plane constraint is plane stress, and $\kappa = 1-\nu^2$ if the in-plane constraint is plane strain. The mathematical derivation of this would not be expected, but the interested Mentee can find a derivation on this site, here http://rickbradford.co.uk/EnergyReleaseRate.pdf and also a more challenging (and hard to find) derivation for anisotropic material in Mode I here http://rickbradford.co.uk/K_G_anisotropic.pdf.

1.5 State the criterion for brittle facture

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes13.pdf

All that is really required is appreciating that brittle facture is controlled by a critical value for K, i.e. K_{Ic}.

However, the Mentee should appreciate that constraint plays a major role in the brittle fracture of metals. The Mentee should therefore be able to give a qualitative account of what is meant by 'constraint'. This is likely to provoke a lot of waffle. The concise answer is, "enhanced hydrostatic stress, other things being equal". Equally acceptable is, "a large principal stress to Mises stress ratio, other things being equal". In practice this comes down to "size matters", especially thickness (or, more generally, the length of the crack front). But in-plane section size matters also. In the presence of a crack, R6 defines a specific constraint parameter, β, (see 4.23)

Other issues could be brought in here, but are equally likely to be covered under other questions. For example, in ferritic materials the Mentee should know that brittle fracture can be of two distinct metallurgical types. Brittle behaviour arising due to low temperature will be cleavage, i.e. transgranular. In contrast, embrittlement of carbon-manganese steels due to neutron irradiation can often be inter-granular due to precipitation of sulphur and phosphorus on the grain boundaries. Another issue which might, optionally, be introduced is the actual ***cause*** of brittle fracture (though this risks exposing the fact that the Mentor does not know either). This encroaches on the local approach, the limitations of the LEFM fields for anisotropic, inhomogeneous crystalline materials, the survivability of very small regions under very high stress (Weibull statistics), etc. Good luck – or just avoid it.

1.6 Deduce how the critical crack size for brittle fracture varies with applied load or the size of the structure

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes13.pdf

The classic question here is, given that a body of section size L containing a crack of length 'a' fractures under a load F (or M), what is: (a)the fracture load if 'a' is doubled, or, (b)if L is doubled? Or, what crack size will fracture under half the load, F/2? Or, what maximum crack size will survive the same load if the material is embrittled and reduced to half the toughness? Specify that a << L to make the answers easy. A tension or a bending geometry can be specified. Play tunes on this as you wish. It all follows from $1.12\frac{F}{tL}\sqrt{\pi a} = K_c$ or $1.12\frac{6M}{tL^2}\sqrt{\pi a} = K_c$.

1.7 Discuss how a stress intensity factor could be determined from a finite element analysis

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes20.pdf

This question is not repeated under "PYFM" and hence post-yield fracture parameters are also addressed here (although rather out of place). The details are better addressed under an FE SQEP area. The Mentee should certainly know that the main tool is to evaluate K via the J integral. S/he should know that ABAQUS has an in-built capability to do this. Ideally s/he should appreciate that the contour integral is not evaluated in ABAQUS simply as usually written, but rather as a domain integral. The formulation in ABAQUS also involves a "virtual crack extension, VCE", which purports to provide the energy release for incipient crack extension at an arbitrary angle to the self-similar. The method is implicitly based on appeal to the vectorial $\bar{J}$ integral, though this is disguised because ABAQUS outputs a scalar $J \equiv \bar{J} \cdot \Delta\bar{a}$, where $\Delta\bar{a}$ is the vector of so-called "virtual crack extension". Users are likely to be left with the impression that this ABAQUS J is the energy release rate for a crack whose tip under goes a crack extension $\Delta\bar{a}$ at some arbitrary angle. However, I believe this is untrue (except in the case of self-similar extension). Actually the expression $\bar{J} \cdot \Delta\bar{a}$ gives the energy release under a transformation which is physically unrealisable, namely that the whole of the material within the contour displaces by $\Delta\bar{a}$. Hence, the crack develops a dog-leg at the point where it intersects the contour. This is all well beyond what a Mentee would be expected to know for accreditation. However, those interested can read a detailed account of what the vectorial $\bar{J}$ integral means in the notes on this web site, here http://rickbradford.co.uk/DerivationofJ.pdf.

(Note the ABAQUS VCE algorithm is completely different from the old BERSAFE methodology of the same name).

The Mentee should be able to offer other methods which might, in principle, be used to obtain K from an FEA, even if such methods are not implemented in practice. Methods include:-

(a)Using dP/dA (see 1.4 and 2.2): Run two cases with different crack lengths. Advantage: It's an accurate method. Disadvantages: It only gives the average J along the crack front, and at some vaguely defined average crack length. It does not resolve the separate the Modes. For mixed primary and secondary loading the software may not output the correct (potential) energy. I recommend this method when ever it is possible, as a QA check on the contour integral results.

(b)Alternative contour integral algorithms, e.g. Assessment WorkBench, JEDI, or Yuebao Lei's 2D code. Advantage: In some circumstances, e.g. if cyclic plasticity is modelled, the ABAQUS formulation is not strictly applicable. Some of these alternative codes are supposed to overcome this limitation. Disadvantage: Unknown reliability in some cases. In particular, JEDI is not recommended (a personal view which others may argue with).

(c)Fitting the crack tip fields. The most obvious way to find K, if all one knew were the algebraic expressions for the LEFM crack tip fields, would be to fit the FE stresses, or displacements, to these expressions. This is a very poor method in practice because it is most sensitive to the stresses/displacement nearest the crack tip, where they are least accurate.

1.8 State the 'Paris' law for fatigue crack growth, and explain its physical basis

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

The Mentee should be able to write down $\frac{da}{dN} = C\Delta K^m$ and should know that m is typically between 3 and 4. The Mentee should also understand the physical mechanism underlying fatigue crack growth, namely reversing plasticity. The Mentee should be aware that a mechanism for depositing energy into the material near the crack tip is necessary to cause the damage leading to fatigue crack growth, and that this mechanism is a cyclic plastic zone within the LEFM/PYFM crack tip fields. Ideally the Mentee should know that the form of the Paris Law derives from this mechanism. Being able to trot out the derivation is not essential, but the interested Mentee can find helpful notes on this site, http://rickbradford.co.uk/ParisLaw.pdf and http://rickbradford.co.uk/T73S02TutorialNotes22.pdf.

1.9 Define the fatigue threshold for crack growth, and explain its physical basis

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

The Mentee should know that, in cases where fatigue crack growth would be controlled by a Paris Law, the growth falls to zero below some non-zero SIF range, $\Delta K < \Delta K_{th}$. The Mentee should be able to demonstrate that even slight vibration would otherwise lead to rapid fatigue failures. The Mentee should know that the atomic nature of materials is the root cause of the existence of a fatigue crack growth threshold. The threshold is due to there being a minimum non-zero quantum of crack extension, namely the atomic spacing. In short, ΔK_{th} is a quantum effect, though it is not usually expressed in this way. It would be nice if the Mentee understood why ΔK_{th} is typically of order ~2 MPa√m. There are notes on this site which explain this, see http://rickbradford.co.uk/WhyFatigueGrowthThreshold.pdf

1.10 Discuss how the Paris law for fatigue crack growth might be modified for near-threshold cycling

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

$\frac{da}{dN} = C(\Delta K - \Delta K_{th})^m$, applicable in some limited range like $\Delta K_{th} < \Delta K < 2\Delta K_{th}$. This avoids a discontinuity between zero and non-zero growth at $\Delta K = \Delta K_{th}$

1.11 Discuss how the Paris law for fatigue crack growth should be modified when plasticity occurs in the cycle. Define how the combined effects of fatigue crack growth and stable tearing are assessed

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

The key issue is that, in the present of cyclic plasticity, the LEFM SIF range may need to be replaced by an effective SIF range based on the PYFM parameter ΔJ, e.g. $\Delta K_{eff} = \sqrt{E\Delta J/(1-\nu^2)}$. Alternatively, an empirical law which uses ΔJ directly may be available. The point is that the cyclic plastic strain enhances the effective SIF range. Methods for estimating ΔJ are discussed in Peter Budden's TD/SEB/REP/4088/92 (though there might be updated advice by now).

When the maximum J exceeds $J_{initiation}$, the interaction of fatigue crack growth and crack increments due to stable tearing can be addressed as described in R6 Section II.8.5.3.3. Alternative there may be a specific recommendation for the material, such as that in R66 Table 10.3 for CMn steels, which is based on the tearing modulus, dJ/da.

1.12 Discuss the possible environmentally assisted crack growth mechanisms and their potential interaction with fatigue

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

(i)Corrosion; (ii)Erosion-Corrosion, these days called Flow-Accelerated Corrosion, FAC (or flow-assisted corrosion, according to taste); (iii)Stress-Corrosion Cracking (SCC); (iv)Inter-Granular Attack (IGA); (v)Corrosion-Fatigue; (vi)Fretting Fatigue; (vii)Oxidation; (viii)Thermal Ageing/Service Ageing.

This is a huge field, to which I cannot possibly do justice in a paragraph. Corrosion, Erosion-Corrosion and Oxidation are predominantly mechanisms of material loss, and hence thinning. However corrosion or oxidation pits might also act as crack initiation sites. In addition, oxidation can lead to a virulent form of crack propagation known as "oxide wedging". This is generally associated with ferritic steels.

Inter-Granular Attack is a mechanism of crack formation due to corrosive attack at grain boundaries, usually associated with austenitic materials. Austenitics particularly dislike chloride ions. Stress Corrosion Cracking is a catch-all for various mechanisms in which both the chemical environment and stress play a part. It is generally though of as a crack propagation mechanism with a threshold stress intensity factor for growth (K_{ISCC}). Corrosion-Fatigue is a potentially virulent mechanism in which each successive fatigue cycle clears a small region of protective oxide near the crack tip, thus leaving it exposed to further corrosion. Many of these mechanisms are discussed in R6 (e.g. R6 Chapter II Section 8) and R66 (e.g. Section 10).

Fretting Fatigue does not relate to the chemical environment but rather the mechanical environment. It is a mechanism for the initiation of cracking due to rubbing.

Finally, thermal ageing is not one mechanism but a catch-all for many effects which alter material properties due to prolonged exposure to high temperatures. An example is low alloy ferritic steels which can suffer softening due to long exposure at high enough temperatures. This leads to increased plastic straining due to load cycles, thus exacerbating fatigue damage. This mechanism contributes to thermal fatigue cracking at the bore of thick section main steam pipes and headers made of low alloy ferritic materials ("TTIBC", thermal transient induced bore cracking). Conventional power plant has suffered badly from this mechanism over the last decade. Another example is thermal ageing of 300-series austenitic stainless steels which can markedly reduce the fracture toughness.

1.13 Describe stress corrosion cracking and intergranular attack. Explain the phenomenon of

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22.pdf

See above. "Sensitisation" of austenitic stainless steels is a thermally activated mechanism which renders the steel more susceptible to IGA/SCC. It can occur either due to exposure to heat during welding, or due to service temperature. The mechanism involves chromium-rich carbides being precipitated at the grain boundaries, leaving the material in the immediate vicinity of the grain boundary Cr-depleted. This material is then susceptible to chemical attack. The key reference on sensitisation for the 300-series of steels is E/REP/MATS/0042/AGR/02 (Rob Stevens).

Post-Yield Fracture Mechanics

2.1 Explain why the LEFM parameter K does not apply post-yield
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes15.pdf

Plasticity causes the large elastic stresses near the crack tip to reduce, and the strains to increase. The LEFM fields, which are related to the LEFM parameter K, no longer apply. In certain circumstances they are replaced by post-yield crack-tip fields called the HRR fields (see 2.5). This gives rise to post-yield fracture mechanics (PYFM). However, crack-tip fields in plasticity do not necessarily have the same generality as the LEFM fields for elastic materials, and caution is needed regarding the validity of PYFM approaches (see 3.4).

2.2 State the energy-based definition of the post-yield fracture parameter J. Illustrate by reference to the variation of the pseudo-strain-energy (elastic-plastic work integral) with crack length under: (a)load-control, (b)displacement controlled loading, (c)mixed loading
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes15.pdf

The energy-based definition of J in the special case of pure displacement controlled loading is the same as that of G in LEFM (see 1.4), except that the "strain energy" is replaced by the elastic-plastic work, defined as $U = \int \left(\int \sigma_{ij} d\varepsilon_{ij} \right) dV$. In the case of a non-linear elastic material, U ***is*** the strain energy. Thus, the Mentee should know that J is the 'rate' of decrease of U with respect to crack area in the case of displacement control, $J = -\left.\frac{dU}{dA}\right)_D$. However, the Mentee should know that, under load control, J does NOT equal the rate of increase of U with respect to crack area, as it would in the linear elastic case, i.e. $J \neq \left.\frac{dU}{dA}\right)_F$ for non-linear behaviour. The Mentee should be able to explain why this does not hold in load control using a load-displacement diagram. The Mentee should appreciate that, for non-linear behaviour and load control, the correct value of J is $J = -\left.\frac{dP}{dA}\right)_F$, where the 'potential' energy, P, is defined by $P = U - FD$, where F is the applied load and D the corresponding displacement.

The Mentee should appreciate that the general definition of J in terms of energy is therefore $J = -\frac{dP}{dA}$ with $P = U - \sum_i F_i D_i$, where the sum is carried out over the load-controlled loads, F_i, only. The notation allows for any number of applied loads, or distributed loads, e.g. pressure. This definition covers the case of mixed types of loading. The crucial distinction between U and P can be brought home to the Mentee by contriving a case with mixed loading which clearly has a non-zero J but for which $\frac{dU}{da} = 0$.

2.3 State the contour-integral based definition of the post-yield fracture parameter J. Explain the circumstances in which it is approximately contour independent
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes15.pdf

The Mentee should be able either to write down the contour integral definition of J, or be able to derive it. The definition is $J_1 = \int_{\Gamma_1}\left[W dy - \overline{T}\cdot\frac{\partial\overline{u}}{\partial x} ds\right]$. The Mentee should be able to explain what each term means:

W = elastic-plastic energy density, $\int \sigma_{ij} d\varepsilon_{ij}$; y is the Cartesian coordinate normal to the crack plane, whereas x is in the direction of self-similar crack growth; $\overline{T}$ is the traction per unit area acting over the length element ds of the contour Γ_1, and $\overline{u}$ is the displacement vector at this point in the body. The integral is rigorously path-independent for linearly elastic behaviour and for non-linear elastic behaviour. It is therefore also path independent for incremental plasticity provided that this approximates to the equivalent non-linear elastic behaviour. In practice this often means that J is path independent for plasticity so long as monotonically increasing loads are considered. However, the strict condition is that proportional loading applies. The value of J for plastic materials can depend upon the order in which two loads are applied (which could not be the case for non-linear elasticity).

2.4 State how the J parameter may be estimated in terms of K and the reference stress
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

R6 Sections I.6.1 to I.6.4 gives several methods for approximating the failure assessment diagram (FAD). The Mentee should appreciate that this is equivalent to providing an estimate of J as a ratio of its elastic value in the case of primary loading. This is because $K_r = f(L_r) = \sqrt{\frac{J_{el}}{J}}$ where $J_{el} = K^2 / E'$, and $E' = E/(1-\nu^2)$ in plane strain, or $E' = E$ in plane stress. Hence, the R6 approximation schemes for $f(L_r)$ also provide an estimate of J. Indeed, the two things are equivalent since the R6 FAD is actually a J-based assessment methodology. If the Option 1 FAD is used, this leads to an estimate of J which does not depend upon the material stress-strain curve! This is a very crude approximation for J. The Option 2 FAD estimates are better, and depend upon the stress-strain properties, as they should. The formulae resulting from R6 Equation (I.6.4) is generally known as the "reference stress approximation". When the reference stress approaches, or exceeds, the 0.2% proof stress, the reference stress formula can be approximated by $J \approx (1-\nu^2)\frac{\varepsilon_{ref}}{\sigma_{ref}}K^2$, or, $\frac{J}{J_{el}} \approx \frac{E\varepsilon_{ref}}{\sigma_{ref}}$. In this regime the reference strain will be much larger than the elastic strain (σ_{ref}/E) and so J is much larger than its elastic value. Hence, for primary loading, plasticity causes J to increase.

The above approximation schemes can also be used for secondary loading if the corresponding load resultants are known. These load resultants are then treated as if they were primary. Caution is needed, however, since the load resultants will change if further plastic strain occurs or if the crack grows.

2.5 Describe the stress and strain fields near the tip of a crack in a power-law hardening material in terms of J and the distance from the tip (r), assuming small-scale yielding
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

As a precursor, the Mentee would be expected to understand the qualitative effects of plasticity in modifying the crack tip fields compared with the LEFM fields. It should be appreciated that plasticity will tend to reduce the magnitude of the stress whilst increasing the strain. The Neuber rule of thumb that the product of stress and strain remains constant is a relevant concept. For the case of power law hardening, the Mentee should know that the elastic $1/\sqrt{r}$ singularities become singularities of strength $r^{-1/n+1}$ in stress and $r^{-n/n+1}$ in strain. The Mentee should know that the power law hardening crack tip fields (the HRR fields) play a comparable role in PYFM as the LEFM fields in the elastic fracture mechanics. They should have some familiarity with the HRR fields though remembering their specific algebraic form is not essential. For the power law $\frac{\varepsilon_{ij}^p}{\varepsilon_0} = \frac{3}{2}\alpha\left(\frac{\bar{\sigma}}{\sigma_0}\right)^{n-1}\frac{\hat{\sigma}_{ij}}{\sigma_0}$ the HRR fields are: $\frac{\sigma_{ij}}{\sigma_0} = \left(\frac{J}{\alpha\sigma_0\varepsilon_0 I_n r}\right)^{\frac{1}{n+1}}\tilde{\sigma}_{ij}(\theta)$ and $\frac{\varepsilon_{ij}}{\varepsilon_0} = \alpha\left(\frac{J}{\alpha\sigma_0\varepsilon_0 I_n r}\right)^{\frac{n}{n+1}}\tilde{\varepsilon}_{ij}(\theta)$.
The Mentee should understand that a potential limitation of the HRR fields is that they ***may*** not be sustained, even near the crack tip, once plasticity has become widespread. The Mentee should understand the relationship between this observation and the logical validity of PYFM.

2.6 State the criterion for ductile 'fracture' (i.e. crack extension due to overload, which may be stable)
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

Since the crack tip fields are controlled by J (within the limits of validity), if fracture (tearing) is a response to the local crack tip conditions it is virtually inevitable that fracture/tearing occurs at a characteristic value of J. This is a robust argument for the initiation of tearing in large structures or specimens meeting the validity size requirements. In these cases the crack tip fields are J-controlled. Hence, the criterion for the initiation of tearing is that $J = J_{init}$, where J_{init} is the same initiation toughness for specimens and structures alike.

After initiation, the assessment procedures take the following line: The crack remains stable provided that J remains less than J_R for the crack extension considered, where J_R is the resistance curve, or torn toughness (see also 3.3). The validity requirements for J_R in testing standards are generally similar to those for the initiation toughness, leading to a maximum valid tear length.

The status of the J_R curve as a true material property is less robust than that of the initiation J (in my opinion), due to the fact that universal fields of the HRR type, controlled by J, cease to apply after crack extension. The physical phenomenon of stable tearing actually rests upon this being so. The strain singularity at the crack tip is considerably weakened by crack extension. This is why the tearing ceases and the crack is stable. The toughness of ductile materials undergoing tearing relies mostly on energy absorption by the plastic wake left behind the advancing crack tip. There is a Note on this topic on this site, though readers should beware that the views expressed therein may be controversial, see http://rickbradford.co.uk/WhatIsStableTearing.pdf.

2.7 Explain how the effective shear modes, K_{II} and K_{III}, can be found from J integrals. Explain how the energy release rate can be found for crack extensions at an angle ($\theta \neq 0$)
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes16.pdf

What I had in mind here is the fact that the J-integral is a vector. The usual J is just the first (x) component of this vector. Thus $J = J_1$ is the energy release rate for a crack extending in a self-similar manner, i.e. the crack extension is in the x-direction (θ = 0). Determination of EJ_1 in a linear elastic case therefore provides the value of $\kappa\left(K_I^2 + K_{II}^2\right) + (1+\nu)K_{III}^2$, where $\kappa = 1$ if the in-plane constraint is plane stress, and $\kappa = 1-\nu^2$ if the in-lane constraint is plane strain. The second component of $\bar{J}$ evaluates in the linear elastic case to $EJ_2 = -2K_IK_{II}$. In suitable circumstances the third component evaluates in LEFM to $EJ_3 = -2K_{II}K_{III}$. Hence, finding all three components of the vector $\bar{J}$ permits the three modes of SIF, K_I, K_{II} and K_{III}, to be determined separately (up to one overall sign). This is a bit dodgy in 3D cases, but is robust in 2D cases (i.e. K_I and K_{II} only). Derivation of the expressions for the vector J in terms of the three SIFs is provided in a Note on this site, http://rickbradford.co.uk/DerivationofJ.pdf. However, this is not the method generally implemented in finite element codes these days. Instead the codes can find separately the three terms in the self-similar energy release rate, i.e. $\kappa(K_I^2)$, $\kappa(K_{II}^2)$ and $(1+\nu)K_{III}^2$. One method is the "crack closure" integral, which distinguishes between the x, y and z displacements required to close the crack near its tip. This provides the mode separation.

I should not have included the question on energy release due to a kinked crack extension ($\theta \neq 0$). It is too hard. As far as I am aware there is no closed form expression for the energy release of a crack extending in a non-self-similar fashion. In practice, such energy releases would only be found by finite element modelling. When I wrote the question, I thought (incorrectly) that the energy release rate at an arbitrary angle of crack extension was $\bar{J} \cdot \bar{a}$. But this expression actually represents the energy release when the whole of the region within the contour on which $\bar{J}$ is evaluated is displaced by $\Delta\bar{a}$. This is not a physically meaningful transformation. It leads to a crack with a dog-leg for non-self-similar displacements. ABAQUS purports to provide an automatic facility for considering virtual crack extensions at non-zero angle. But I believe that ABAQUS merely makes the same mistake.

The Mentee should be aware that there are competing theories for what controls the direction of crack extension (fracture). It may be the direction of maximum energy release rate (determined, perhaps, by FEA), or it may be that crack extension is in the direction of maximum tangential stress (σ_θ). The latter theory is easier to work with, and leads to a simple expression for the angle of crack advance in terms of K_{II} / K_I if LEFM crack tip fields are assumed. The HRR fields can be used to find how this cracking angle changes when plasticity is introduced.

In Mode I either criterion will give self-similar crack advance ($\theta = 0$). In contrast, pure Mode II loading will tend to promote crack growth in a kinked direction, and energy release is correspondingly maximum at a large value of θ. The exact angle depends upon the fracture theory adopted, but values around 70^o-75^o appear typical. I plan to include a short Note on the angle of crack growth on this site eventually (still not done as of Dec'12, sorry).

The Measurement of Fracture Toughness

3.1 Describe how the fracture toughness of a brittle material could be measured in principle. Explain why this methodology would not be applicable for a ductile material

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

The point here is that K_{Ic} for a brittle material can be measured by the straightforward method of determining the load at which a cracked specimen undergoes brittle fracture. This will be a well defined load for a brittle failure. Moreover, the value of the LEFM SIF can be evaluated at that load, if necessary by FEA, and that provides a direct measure of K_{Ic}. The Mentee need not provide further details of experimental procedures.

For a ductile material, neither of these conditions apply. That is, fast fracture, whilst it might occur eventually, does not indicate the salient load defining initiation toughness. Moreover, the LEFM SIF evaluated at any relevant load (initiation or failure) is not the indicative quantity.

The Mentee should also be aware of some of the older, empirical measures of toughness which do not directly produce a K_{Ic} value. The most important is Charpy energy, for which it is necessary to adopt a standard specimen and a standard impact loading device. The Mentee should be able to describe the standard Charpy specimen and how the impact test is carried out, and the absorbed energy determined.

3.2 Describe the methodologies for toughness testing of ductile steels, including both the multi-specimen and single specimen techniques. Describe what is measured and how the initiation toughness is obtained

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

The Mentee should read texts on this subject, and be familiar with the specimen geometries used, the testing machines used, and how the data is recorded, including typical instrumentation. The key difference compared with brittle materials is that toughness is defined via the "initiation" of stable tearing. The Mentee should be aware that "initiation" of tearing is often defined as a tear of 0.2mm, but that alternative definitions can be used (e.g. intersection with a blunting line). The Mentee should appreciate that the measured data consist of, (a)the load-displacement trace, and, (b)the crack tear length.

The quantity which is determined from the test is not K but J. This is obtained from the area under the load-displacement trace (i.e. the work done, U) via a correlation roughly like $J = \eta \frac{U}{A}$, where A is the area of the uncracked ligament ahead of the crack. The Mentee should know that the 'eta factor', η, is roughly 1 for specimens which are predominantly membrane loaded, and roughly 2 for specimens which are predominantly bending loaded, and preferably know why. The Mentee might also be aware that the empirical correlation between J and U is often rather more complex, involving subtractions of uncracked parts, and/or elastic parts, of U.

The Mentee should be aware that the Compact Tension Specimen is a predominantly bending specimen, despite the name.

The Mentee should know of several methods for determination of the tear length. The most expensive method is to use several specimens and to break each open at a different load (rarely used any more). In principle the tear length could be measured using ACPD or DCPD. Why is this rarely done in J-testing? The most common technique these days is "unloading compliance". The tear length is inferred from the decreased elastic stiffness, determined by unloading and then reloading. The Mentee must be aware that the outcome of a J-test is a plot of J versus tear length, Δa, and that the initiation toughness is obtained therefrom, if necessary by extrapolation. The J v Δa curve beyond the initiation point is the J_R tearing resistance curve.

The Mentee should be aware of the reasons for using side-grooved specimens (enhancement of constraint, and ensuring straight self-similar crack growth). How is 'A' defined for side grooved specimens? If there were no side-grooves, what would the torn crack front probably look like?

The Mentee should be aware of the use of fatigue pre-cracking.

3.3 Explain what is meant by "stable tearing" and how the tearing modulus is measured

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

This is implicit in 2.6 and 3.2 above. For ductile materials, both specimens and structures exhibit tearing of the crack rather than sudden brittle fracture. The structure can still absorb considerable energy after the point at which tearing first starts. In many cases the sustainable load will go on increasing after tearing has first started. However, the Mentee should also be aware that there will be a load or displacement at which fast fracture will occur. The mechanism may be ductile, but there can still be a big bang!

Assessment methodologies, and indeed the J_R resistance curve itself, give the impression that the stability of ductile tearing is due to the material becoming tougher after tearing. Although not a requirement for accreditation, the scientific nature, or otherwise, of this view makes for an interesting discussion. The physical nature of stable tearing, and why it is stable, is discussed in a Note on this site, but be aware that this may be contentious, see http://rickbradford.co.uk/WhatIsStableTearing.pdf.

3.4 Explain what is meant by the "validity" of a fracture toughness measurement, and state the validity criteria for ductile (J) tests. Explain the theoretical basis for these limits in terms of the crack tip fields. For typical pressure vessel steels and standard toughness specimen sizes, what is the typical limit on the valid extent of stable crack growth?

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

and http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

There are a lot of different fracture toughness testing standards but they share common features. The validity limit is usually expressed as a minimum size requirement for the test specimen (and this is usually a function of geometry, i.e. constraint). This size requirement is often that both the thickness and the remaining ligament should exceed some stated multiple of J_i / σ_y. For bending dominated specimens (e.g. CTS) this multiple might typically be x25. For tension geometries it will be much larger (perhaps x200) due to the lower constraint. The theoretical basis of these limits relates to the requirement that the crack tip fields in the specimens remain as they would be under small-scale yielding conditions for the same value of J. This is known as "J dominance" or simply as "validity". In power law hardening this implies that the crack tip fields would be of HRR form (see 2.5). The required multiples are determined from finite element analyses of the specimens.

The validity limits on the tearing resistance curve are usually in two parts: an upper limit on J_R and an upper limit on Δa. The upper limit on Δa may simply be a geometrical limit, e.g. 10% of the initial ligament. The upper limit on J_R is typically $B\sigma_f / 20$, where B is the specimen thickness and σ_f is the flow stress (average of 0.2% proof and UTS). Typical valid tear lengths for structural steels obtained from the most common specimen geometries/sizes are 0.6mm to 1.4mm.

3.5 Describe how the fracture toughness of ferritic steels vary with temperature, giving typical values
This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

In order of increasing temperature: lower shelf / transition region / upper shelf. For CMn steels the transition region can be, extremely roughly speaking, around room temperature. This might mean quite brittle behaviour at room temperature, or even slightly above. It might mean that the toughness of the steel varies markedly between ~20°C and (say) ~60°C. The transition region spans a range of temperatures, so the definition of a "transition temperature" is rather arbitrary. Some definitions are: (a)FATT = fracture appearance transition temperature. This is defined as the point at which a microscopic examination of a fracture surface appears to be ~50% cleavage and ~50% ductile. So it could be rather subjective. (b)An arbitrary Charpy energy level, usually 40J. (c)ASME uses NDTT = nil ductility transition temperature. This is obviously a lower temperature than the FATT, but tends to be more sharply defined.

The Mentee should appreciate that lower shelf tougnesses of structural ferritic steels are typically 20 – 40 MPa√m, whereas upper shelf toughnesses are typically >100 MPa√m.

The Mentee should also appreciate that austenitic steels do not exhibit transition or cleavage behaviour, and hence have superior toughness at low temperatures. (The reason relates to their fcc structure, as contrasted with the bcc structure of ferritics).

3.6 Describe the effects of environment and loading rate (e.g. dynamic loading) on the fracture toughness

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

Exposure to high temperatures for a sustained period can lead to degradation of toughness (thermal ageing). R66 Section 13 has a limited discussion of this, including explicit guidance for austenitic steels in Section 13.4.4. Note, however, that there is more recent advice for some of the 300-series stainless steels, for which the service degradation of toughness can be substantial (see the R66 User Queries database).

The presence of certain chemical species can seriously degrade toughness, the most famous being hydrogen and helium.

The Mentee would be expected to know that, for ferritic materials exhibiting a toughness transition, the toughness transition temperature is increased by dynamic loading. This means that the transition toughness is ***reduced*** by dynamic loading. In contrast, the upper shelf toughness is increased by dynamic loading, whereas the lower shelf toughness is unchanged. See R66 Section 13.7 for a quantitative relation between strain rate and transition temperature. Note that the strain rate must be measured near the crack tip (see R6 Section I.5.3).

3.7 Describe the effect of neutron irradiation on fracture toughness, and for which materials this may be more significant

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes18.pdf

The Mentee should certainly be aware that the broad effect is to increase strength whilst decreasing ductility, and hence decreasing toughness. This is broadly true for both ferritic and austenitic steels, but austenitics are substantially less sensitive than ferritics. Neutron irradiation can cause very large shifts in the toughness transition temperature of ferritics, e.g. by the order of hundreds of °C at doses experienced by Magnox reactor pressure vessels (RPVs), i.e. hundreds of 10^{-5} dpa. Consequently, a change in material behaviour from fully ductile to fully embrittled is perfectly feasible in strong neutron fluxes.

Dose-damage relations are a huge field. Detailed data fits are provided in R66 Section 15 for materials of relevance to AGRs. Note in particular that parent and weld may respond very differently due to differences in chemical composition, to which the irradiation embrittlement is very sensitive. For example, in carbon-manganese steels, embrittlement is far worse in steels with relatively high copper content. For this reason Magnox RPVs were challenged by the embrittlement of the submerged arc welds used in their fabrication, since these had high copper content. Embrittlement of CMn steels is also exacerbated by high levels of P or S, since these species can precipitate onto the grain boundaries under the influence of irradiation and lead to intergranular fracture. Note that the mechanism of brittle fracture can be different in irradiated CMn steels compared with unirradiated steels below their transition temperature (i.e. intergranular fracture rather than cleavage).

Generally, ferritic steels operating close to, or within, the transition region will be most sensitive to irradiation. The upper shelf toughness of ferritic steels is relatively insensitive to irradiation, although the tearing resistance can be substantially reduced.

R6 Assessment

4.1 Describe the Failure Assessment Diagram (FAD) and define its axes, Lr and Kr. Define the material properties used in their definition. Define 'reference stress'

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

For the details it is best to read R6. The following are just some brief observations. The FAD is a failure avoidance tool. Fracture is avoided if the assessed (Lr, Kr) point lies within the FAD. For ductile materials, reaching the FAD locus will generally correspond to stable tearing (see below). Kr is defined as the SIF (K) divided by a suitable lower bound toughness, but with certain adjustments if secondary stresses are present (see below). Lr is the primary load divided by a suitable collapse load based on the lower bound 0.2% proof strength.

The 'reference stress' is Lr multiplied by the proof strength used to define its denominator. Consequently the reference stress does not depend upon the material properties but only upon the loading and the structural geometry. In R5 both the true (primary) reference stress is used and also a pseudo-reference stress including allowance for the secondary stresses. In R6 Lr is defined strictly in terms of the true, primary reference stress.

4.2 State the position of the cut-off on the Lr axis. Define 'flow stress' (as used in R6)

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

The cut-off is Lr = flow stress/0.2% proof stress, where the flow stress is defined as the average of the 0.2% proof stress and the UTS. Both numerator and denominator in the definition of the cut-off should use best estimate data (despite Lr being defined using the lower bound 0.2% proof stress). The Mentee should appreciate that R6 is thus effectively basing plastic collapse on the lower bound flow stress (i.e., in the limit of a sufficiently small crack), or, more precisely, on $\sigma_{ref} = \left(\sigma_{0.2}^{LB} / \sigma_{0.2}^{BE}\right)\sigma_f^{BE}$.

4.3 Describe qualitatively the four methods, of increasing precision, for finding the FAD

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

The Mentee should be aware of all the following:-

The R6 FAD is really a J-based fracture criterion expressed in diagrammatic form. Given an analysis of any cracked structure, an FAD may be derived by plotting Kr defined as $\sqrt{J_{elastic} / J_{elastic-plastic}}$ against the normalised primary load, namely Lr. Note that, in applications, Kr is interpreted as K/K_{mat}. Why, then, is it valid to derive the FAD as $Kr = \sqrt{J_{el} / J_{ep}}$? The former involves the material property, K_{mat}, whereas the latter involves no material property at all, only the results of the analysis. The Mentee should be able to resolve this conundrum. The resolution is this: If fracture is controlled by J_{ep} reaching a critical value, namely J_{mat}, then the two expressions become the same, i.e., $\sqrt{J_{el} / J_{ep}} = \sqrt{J_{el} / J_{mat}} = K / K_{mat} = Kr$. You only need to imagine the structure made of a succession of different materials with different toughnesses, in which case the whole locus of the FAD can be swept out.

If a J-analysis is available for the structure and loading of interest, then deriving an FAD in this way constitutes an "Option 3" assessment. This is the most accurate method. However, in this case it may not be necessary to consider an FAD at all. The assessment can be carried out directly using the criterion $J = J_{mat}$.

The general purpose Option 1 FAD was devised originally by compiling a number of FADs derived for a range of geometries and loadings, and for a range of different materials. The Option 1 FAD was chosen to be reasonably representative of the spread of curves obtained, but biased significantly on the conservative side of the mean.

The Option 2 FADs are intermediate between the general purpose Option 1 and very specific Option 3 FADs. They allow the material stress-strain dependence of the FAD to be taken into account, but not the geometry or loading type. The more accurate of the two Option 2 FADs is actually equivalent to the reference stress approximation for J, i.e., $\frac{J}{J_{el}} \approx \frac{E\varepsilon_{ref}}{\sigma_{ref}} + \frac{\sigma_{ref}}{2E\varepsilon_{ref}}\left(\frac{\sigma_{ref}}{\sigma_{0.2}}\right)^2$. This can be used only when the stress-strain hardening curve for the material is known. In many cases one may know only the yield strength and the UTS of the material. R6 contains advice on the construction of an approximate Option 2 type FAD but using only these quantities as inputs.

Note that the utility of the Option 1 FAD lies in the fact that all materials and geometries have very similar FADs. This is because the bulk of the material and geometry dependence is "normalised out" by the use of appropriate definitions of Kr and Lr (and the Lr cutoff).

It can be debated, for certain classes of materials, which normalising strength leads to the most representative FAD. Thus, for CMn steels, R6 used to contain a different FAD with Lr replaced by Sr which was defined using the flow stress rather than the 0.2% proof strength. Equivalent FADs can effectively be reproduced in R6 Rev.4 by using the Option 2 FADs, if desired. However, R6 still contains advice broadly equivalent to the old Sr-based CMn FAD and this can be found in Chapter III.6.

4.4 Describe the two simplest methodologies within R6 for incorporating secondary stresses into a fracture assessment. State typical magnitudes of correction terms/factors, ρ and V

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

The Mentee should understand the requirement for a plasticity correction to the secondary SIF in the R6 approach. The reason is that, (a)most obviously, the elastic-plastic J differs from the LEFM J, and, (b)whilst the Lr axis accounts for the plasticity due to primary loading, because Lr is based on primary load only it cannot account for the plasticity due to secondary loads. Hence, an allowance for secondary plasticity is required by some other means.

It is also essential that the Mentee understand that the different treatment of primary and secondary plasticity within R6 reflects an important physical difference. The plastic straining due to primary loads will always increase J, i.e. $J_{ep} > J_{LEFM}$ (i.e. the FAD Kr is < 1). The same is not true for secondary plasticity, which can either increase or decrease J depending upon the circumstance. The Mentee should be able to give examples of both behaviours, illustrated using a load-displacement or stress-strain diagram.

The Mentee should be aware that there are two methods available for secondary plasticity correction in R6: one is additive and adds a term ρ to the secondary Kr, the other is multiplicative and replaces the secondary Kr with VKr. Hence, the ρ correction is a number small compared with unity, perhaps ~0.2, say, whereas the V correction is close to unity, perhaps 1.2, say.

R6 contains two different methods for estimating both ρ and V, an approximate method [R6 Section II.6.3.2] and a more detailed method based on look-up Tables [R6 Section II.6.4]. Both methods, and both the ρ and V formulations, share the important feature that the correction vanishes shortly after general yield due to primary loading. Thus, using the approximate method, for Lr > 1.05 the advice is ρ = 0 and V = 1. The same is true for the more accurate method, for some value of Lr just greater than 1, but not necessarily exactly 1.05.

The Mentee should understand physically why this vanishing of the secondary plasticity correction is to be expected (namely that, once Lr exceeds unity, the primary plasticity washes out the effects of secondary plasticity, i.e. they are not additive).

The Mentee should appreciate the benefit to be gained from using the more accurate method of [R6 Section II.6.4], namely that: (a)the secondary plasticity correction may be smaller using the refined procedure, and, (b)when Lr > 1, the refined method can result in a beneficial influence of secondary plasticity. The latter results from ρ becoming negative, or equivalently, V becoming less than 1.

4.5 Illustrate how the reserve factor on primary loading is found from a graphical construction on the FAD and for an initiation assessment: (a) for primary loading alone; and, (b) for primary plus secondary loading. In the latter case, contrast with the reserve factor on total loading

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

The Mentee should be aware that the obvious scaling from the origin to the FAD, as illustrated in R6 Figure I.10.1, only applies to the calculation of the reserve factor on a single primary load. For multiple loads, all of which are primary, the same construction applies but only provides the reserve factor with respect to all loads.

In the case of a primary plus a secondary load, the simple construction from the origin cannot be relied upon even to give the reserve factor on total loading. This is because the secondary plasticity correction does not scale appropriately (e.g. it becomes zero at around Lr ~ 1.05).

By scaling from a point representing the secondary loads acting alone, a reserve factor on primary loading can be estimated crudely – but again it is not accurate due to the behaviour of the secondary plasticity correction. It will give a reasonable approximation as long as the failure point on the FAD is at sufficiently small Lr. However, for safety, graphical construction is not recommended.

Graphical construction is not useful for finding the reserve factor with respect to one particular primary load when there is more than one primary load acting. This is because Lr depends in a non-linear manner on the two or more primary loads.

In short, in general it is best to find reserve factors by directly calculating the 'failure' loads of interest. This requires some iteration if done by hand.

4.6 Describe how a fracture assessment including stable tearing can be carried out using R6. Illustrate the process by the locus of assessment points on the FAD. Describe how the reserve factors may be found for a stable tearing assessment

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

There need be no artificial complication about a tearing analysis. It can be carried out by considering a range of small potential tears and repeating the R6 assessment taking account of: (a)the effect of the changed crack length on the collapse load; (b)the effect of the changed crack length on the SIF, and, (c)the increase in the toughness due to the tear, according to the material J_R curve. However, this assessment should not necessarily be carried out for the same loads as in the initiation assessment. The reason is that a sufficiently large load must be assumed in order to result in tearing. This would normally be some severe fault load. It would be unusual to predict tearing under normal loading conditions, or even frequent faults. Alternatively, an artificially high load may be postulated in order to derive the reserve factor with respect to this limiting load including tearing.

The Mentee should appreciate that even ductile materials exhibiting stable tearing are potentially susceptible to fast fracture. The fact that the metallurgical mechanism is ductile, and that possibly large amounts of tearing take place prior to final failure, does not make the fast fracture event any less destructive when it happens.

The Mentee should be able to illustrate the tangency construction for tearing instability. However, the Mentee should also appreciate that this is not the real locus representing a structure undergoing tearing due to slowly increasing load. The Mentee should therefore also be able to sketch the true locus (which hits the FAD and then runs down it as the tear length and the load are increased further). Stable tearing persists whilst an increment of tearing at constant load brings the assessment point back inside the diagram. Tearing instability (fast fracture) occurs when an increment of tearing at constant load takes the assessment point outside the diagram.

4.7 Describe how an R6 stable tearing assessment can be used to find the limit of tearing stability. Comment on the validity aspects of such an instability assessment

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes17.pdf

The first part is covered in 4.6, and the second part in 3.4.

4.8 State a selection of sources of advice on stress intensity factor solutions for common geometries and loadings

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes14.pdf

See the Reading List in the Mentor Guide.

4.9 State a selection of sources of advice on reference stress (collapse) solutions for common geometries and loadings

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

See the Reading List in the Mentor Guide.

4.10 Describe in outline the more detailed ρ and V methodologies for incorporating secondary stresses into an R6 fracture assessment

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

Covered under 4.4.

4.11 Describe in outline the three methods for calculating the effective elastic-plastic secondary stress intensity factor, K_J^S

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

K_J^S is the elastic-plastic fracture parameter defined by $K_J^S = \sqrt{E'J^s}$, where J^s is the elastic-plastic value of

J for the secondary loads alone. It is used within R6 Section II.6 as an input to the more detailed method for calculating ρ and V, and also as an input in the special provisions for large secondary stresses (see 4.12). The three methods for estimating K_J^S are: (i)FE analysis of the cracked body; (ii)FE analysis of the uncracked body, followed by the analysis described in R6 II.6.5.1; (iii)a simple approximation based on the small scale yielding adjustment to LEFM. In practice, (i) is unlikely to be used. This is because, if an FE analysis of the cracked body under secondary loads is available then it is likely that an analysis under combined primary and secondary loads will also be available. In this case, the methods of Section II.6 will not be required since the assessment can use the total J directly (i.e. an Option 3 assessment). The other two methods are defined in Sections II.6.5.1 and II.6.5.2 respectively. The estimate based on LEFM, (iii), is particularly simple and requires no extra information. However, it is conservative and fails to gain benefit from plastic relaxation of the secondary stresses when this is relevant.

4.12 State the special provisions which apply when the combined secondary stresses exceed yield and contain a contribution from welding residual stresses

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

Of course, FE analysis could be carried out. But a special provision applies within R6 when the sum of the residual stress and the (other) secondary load exceeds yield. This special provision recognises that residual stresses are problematical as regards identifying a suitable plastic strain. The provision, within Section II.6.8, allows the total K_J^S due to both secondary loads to be found as $K_J^S = K_J^{s,\sec} + K_I^{s,res}$, where the first term is the elastic-plastic K for the (other) secondary load alone, and the second term is the LEFM SIF for the residual stress. This eliminates the requirement to consider plastic strains for the residual field.

An alternative, but much more complicated, means of obtaining alleviation in such a situation is to deploy a local approach (see below).

4.13 State a selection of useful sources of advice on welding residual stress distributions for common weld geometries

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

See the Reading List in the Mentor Guide.

4.14 Describe how the stress intensity factor may be found for a through-thickness crack in a self-equilibrating secondary stress field

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes14.pdf

To be more precise, this relates to stress distributions which are self-equilibrating through the section thickness. A simple expression for the SIF of a through-wall crack in such a stress field is given in R6 Section II.6.7. It is unusual in that the SIF does not depend upon crack length, but instead upon wall thickness (w), $K^{s,res} = \lambda \sigma_{\max} \sqrt{\pi w}$, where the compliance factor is given in R6 and is less than 0.5.

4.15 Describe in outline the methodology within R6 for assessing displacement controlled loadings which are intermediate in character between primary and pure secondary stresses

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes19.pdf

This is an important provision within R6, and is less widely used than it should be. In practice, many loads which are displacement controlled are assessed as primary because the degree of elastic follow-up is unknown. For example, long-range thermal stresses arising in piping systems due to a change of temperature of the whole system are generally assessed as primary. Actually such loads are displacement controlled. They can be treated as such if a less conservative answer is desirable. The Mentee should be firmly disabused of the distressingly common notion that "displacement controlled" is synonymous with "secondary". Equally false is the idea that (say) long range thermal stresses in pipework are primary, "because the design codes say so". In truth, loads do not fall into two clear divisions: primary and secondary. This is merely a convenient simplification to permit simple assessment rules. Loads are generally intermediate in character, and displacement controlled loads are an example.

The methodology is outlined in R5 Chapter III.14. In essence the methodology simply recognises that it is valid to find the loads, or stresses, which actually correspond to the applied displacement (or rotation), and then to carry out the assessment using these true loads/stresses as if they were primary. The true loads or stresses are less than the elastic loads or stresses calculated for the uncracked body due to the additional compliance arising from, (a)the presence of the crack, and, (b)the effects of plasticity. To benefit from this approach it is therefore necessary to be able to deduce the extra displacement for a given load arising from one or both of these sources. In fact, the additional compliance due to the crack can be found from the SIF or J solution, but numerical integration over crack size is required. R6 Equation (III.14.5) is one such expression. The general formulation of this approach is given in my paper "A Structural Approach to the Calculation of J", *Engineering Fracture Mechanics*, Vol.29, No.6, 683-696, 1988.

4.16 Explain the difference between a full Leak-before-Break (LbB) assessment and a "Detectable Leakage" argument

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

This is covered in R6 Chapter III.11. A full leak-before-break (LbB) argument starts with a real or hypothesised part-penetrating defect, usually assumed semi-elliptical in shape. The growth of the defect in both the depth and length directions must then be assessed. This requires a credible quantification of the crack growth rate, by whatever mechanisms are active, e.g. fatigue, creep, stress-corrosion, etc. The critical (semi-elliptic) crack size is evaluated so that the time at which the defect breaks through the wall can be found. This also provides the defect length at break-through, though this is subject to the R6 re-characterisation rules (see 4.18). The resulting leaking crack length must then be shown to be less than the critical through-crack length under whatever fault conditions might be deemed relevant. However, the defect length at break-through must also be long enough to result in a detectable leakage.

The key distinction between a LbB argument and the reduced version, or "detectable leakage" argument (DLA), is that the latter does not attempt to follow the development of the part-penetrating crack before break-through. Instead the DLA seeks to demonstrate that the length of through-crack which will confidently be detectable via leakage is smaller than the critical through-crack length by a suitable margin. If leak detection can be claimed only after some time interval (e.g. between plant walkdowns) then an allowance for crack growth must be included.

The DLA only provides a safety argument if it can also be claimed that any defects which arise will be less than the critical length when they break through the wall and leak. R6 is careful to stress this. However, in my experience, a DLA claim is sometimes made in safety cases without there being any clear argument as regards the possible length of real defects at break-through. The argument is often abused.

The full LbB assessment would take care of this shortcoming, in principle. However, in 30 years I have never seen one made. One reason for this is that it is very difficult to have confidence in the assessment of the growth of the starter defect, and usually the starter defect itself is very uncertain. But I suspect that the main reason is that the known growth mechanisms tend not to lead to a prediction of leakage. The growth is too slow. In other words, the assessment provides a "no leakage" case, and hence LbB does not arise.

In truth, the logic underlying arguments based on leaking cracks in real applications tends to be rather variable and tailored to the specific case. A typical case would be, "we predict that there will be no leakage, but if there were then here's a detectable leakage argument". Strictly such an argument is outwith R6, since the DLA in R6 requires the assessor to ensure that, "appropriate mechanisms of localised degradation exist for the development of a detectable through-wall defect and that a long surface defect cannot arise which would lead to gross failure of the pressure boundary".

4.17 Describe the major steps in a full LbB assessment, including the required knowledge about the initial state

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

This list is given in R6 Chapter III.11.5.

4.18 Explain the relevance of the re-characterisation rules for an LbB assessment, and how these may be modified for leakage rate assessment

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

The Mentee should be aware that R6 contains rules for idealising flaw shapes in Section II.3. Part of this advice is the re-characterisation of defects when an initially part-penetrating defect breaks though the wall to becoming a through-crack. Figures II.3.4 and II.3.5 are particularly pertinent. These rules should be deployed to determine the size of the leaking defect and hence whether it remains stable under the required fault condition. The intent of the general purpose re-characterisation rules is to provide a conservative over-prediction of the crack length for the structural assessment.

However, as regards ensuring that the leakage rate will be detectable, over-estimating the leaking crack length is non-conservative. It is therefore desirable to use a more realistic through-crack length & shape at break-though for the purposes of leak rate calculation. This is discussed in R6 Section III.11.6.3 and guidance provided in R6 Figure III.11.7. Note that leaking cracks are generally much shorter on one surface than the other.

4.19 Describe how the crack opening area is calculated. How might this be affected by: (a) crack shape; (b) welding residual stresses; (c) through-wall bending stresses

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

The Mentee should be aware of the advice in 6 Section III.11.6.4. Equations are given for simple geometries, and suitable references for others. The Mentee should appreciate that including plasticity effects could increase the leakage area significantly. It should also be appreciated that the true shape of the crack at break-through, with a much shorter inside surface length, considerably complicates the calculation of crack opening area (COA).

The Mentee should also be aware that the tapering of the crack opening in the thickness direction can have a very marked effect in throttling the fluid flow through it. Such tapering can arise if wall-bending stresses are large, which is common. Since residual stresses are self-equilibrating, they may cause tapering and reduce flow rates. In fact, sufficiently large wall bending stresses can prevent a crack leaking at all. This is not mere theory. There have been instances of through-cracks in valve body castings which have apparently not leaked at all in service. It is quite common for large wall bending stresses or residual stresses to undermine attempts at a DLA.

4.20 Describe the methods and computer tools available for calculating the fluid leakage rate through a crack. Distinguish between single phase and two-phase flows

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

The Mentee should know that: (a)The recommended code for single phase flow is DAFTCAT, which is available within R-code, and (b)The recommended codes for two-phase flows (steam and water mixtures) are SQUIRT or PICEP. In my view it is more desirable that the Mentee has carried out a flow rate calculation by hand than necessarily have had experience in using these codes. There may be enough information in R6 Section III.11.6.5.1 to do this. If not, Dave Ewing's single-phase methodology, upon which DAFTCAT is based, can be used as a source of the equations etc. (This is reference III.11.60 in R6 Chapter III.11).

The Mentee should appreciate the significance of the crack taper and the friction factor. The Mentee should preferably recall the basic mass flow rate formula for single phase flow, R6 Equation (III.11.5), and hence be able to explain what is meant by a discharge coefficient.

4.21 State what factors are most significant in estimating the lower bound and upper bound fluid leakage rates. Is a large fluid leakage rate a good thing or a bad thing?

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

The most significant factors determining the flow rate are: (a)the smaller of the crack opening areas on either surface, (b)the mean crack opening area, (c)the friction factor. (a) is most important in the sense that if it is zero then there is no leak. The alternative way of expressing this is if the crack divergence parameter is greater than or equal to the mean crack width ($d \geq W$). Hence, (a) is equivalent to recognising the importance of crack taper, which in turn relates to wall bending stresses. The crack surface roughness is also important, but this is implicit in the friction factor, (c). The Mentee should be aware that the credible range of friction factors can make an order of magnitude difference to the predicted leakage rate.

The leak rate is required to be sufficiently fast to be detectable, both by a LbB argument or by a DLA. However, large leaks may directly challenge safety. Examples include steam leaks inside a reactor. These can lead to rapid increases in gas-side pressure and hence challenge the primary circuit integrity. On the economic front, they can make a mess inside the reactor which is difficult to clear up.

A personal opinion: The importance of demonstrating the detectability of leaks is rather over-played, in my view. In my experience, leaks can generally be detected easily. Steam leaks in reactors tend to be spotted through CO_2 moisture levels at the 'pin hole' stage. Quite small CO_2 leaks in air can be found with sensitive sniffing equipment. The more important issue is generally to ensure that very large leaks do not occur – sufficiently large to challenge safety.

4.22 Discuss how to estimate the length of time for which a leaking crack might remain stable. What key factors might be difficult to quantify?

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

R6 Sections III.11.7.4,5 refer to the creep mechanism. In principle the rate of growth of the penetrating crack by creep can be assessed (using R5, and hence outwith this SQEP sub-role). Note that gross failure may occur prior to the crack length reaching the critical length based on R6 assessment, due to creep rupture.

Hence creep rupture of the net ligament must also be assessed (and for a growing crack, involving suitable integration). Creep crack growth rates and rupture times are subject to large uncertainties and hence bounding data is required.

In the case of a steam leak, it may be rather naïve to assume that a survival time based on creep alone will provide a secure safety assessment. The steam is likely to be doing nasty things to the crack surface and crack tip area, especially if it is superheated steam. At ~520°C and 2300 psi, steam lancing can chew through steel very effectively. Even if the leak is cold water, or wet steam, corrosion effects may be the limiting issue.

4.23 Describe physically the influence of constraint on fracture behaviour. Define the T-stress and the Q-stress, and when they apply. Define the constraint parameter β. Describe the effect of positive and negative constraint on the effective fracture toughness in the cleavage and ductile regimes

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes21.pdf

The first question should have been, "what is meant by 'constraint'?". See 1.5 for an answer in the absence of a crack. The Mentee must know this, because constraint is one of the most important concepts in fracture. Do not accept vague metallurgical waffle. Constraint is quantifiable. However, in the presence of a crack, R6 also defines a normalised parameter, β, which quantifies constraint.

High constraint increases the likelihood of fracture, other things being equal. Standard specimen geometries and toughness testing validity rules are designed to ensure that high constraint conditions prevail, thus rendering the measured toughness valid. But a given material will exhibit higher apparent toughness in a geometry, or a loading condition, which is less constrained. Consequently, the application of a toughness measured on a valid, constrained, specimen geometry to a low-constraint plant structure will be conservative. Generally that is quite acceptable. But sometimes, when margins are hard to demonstrate, the assessor may which to quantify the degree of conservatism and appeal to the low constraint structure explicitly in an assessment.

R6 Chapter III.7 provides procedures for taking constraint effects into account in assessments.

The T-stress is the constant term in the expansion of the elastic crack-tip stress fields as a power series in √r. The first term is, of course, proportional to $K/\sqrt{2\pi r}$, i.e. the LEFM crack tip field. The next term is T, which acts in the direction parallel to the crack (the x-direction). Hence, the T-stress depends upon the remotely applied x-stress, as well as upon the applied (Mode I) y-stress.

Whereas the T-stress relates to the elastic crack tip field, the Q-stress relates to the elastic-plastic crack tip field. For the full definition of the Q-stress see R6 Section III.7.5.2(b). Roughly, the Q-stress is the amount by which the hydrostatic stress a small distance ahead of the crack tip exceeds that for the datum zero constraint (i.e. T = 0). Then Q is defined by the Q-stress normalised by the yield stress.

The structural constraint parameter, β, is defined either as $\beta = Q/Lr$, or as $\beta = T/\sigma_y Lr$.

Note that a constraint parameter of zero (β = 0) does not mean that the structure is unconstrained. Quite the contrary: it means that the structure is sufficiently constrained for valid toughness data to be directly applicable without correction. On the other hand, lower values of the constraint parameter (i.e. negative values of β) mean that the structure is less constrained and, in assessments, an enhancement of the standard valid toughness is applicable. Positive values of β mean that the structure is highly constrained, but valid toughness data can still be used to perform an assessment.

In order to carry out an assessment which appeals to the benefit of a low constraint geometry it is necessary to know two things: Firstly, the cracked structure must be analysed to quantify the constraint in terms of T or Q, and hence β. Secondly, the effect of this level of constraint on the material's toughness must be quantified. The latter requirement is generally an obstacle to application of constraint-based assessments. It requires materials testing results to be available on at least two different specimen geometries with sufficiently differing constraint. This is rarely available "off the shelf". Hence, constraint arguments are deployed only when the importance is sufficiently high to motivate a bespoke testing project, and only then if time permits.

The benefits of constraint are most marked for materials in the cleavage regime. For ductile materials, R6 advises that the benefit is small as regards initiation toughness, though more significant for tearing.

4.24 Describe the benefits to be gained from a strength mismatch assessment of a weldment and broadly how this is accomplished within the R6 procedure

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

This is covered in R6 Section III.8. The Mentee would be expected to appreciate that a conservative R6 assessment of a multi-material structure would normally use the weakest material component to estimate the collapse load, and hence Lr. The Mentee should also appreciate that a more accurate collapse solution is potentially applicable for a multi-material situation. The essence of the procedure of R6 Section III.8 is to take advantage of a more accurate collapse solution for the multi-material situation. If available, this collapse solution may then be used both to define Lr and also to devise an equivalent stress-strain curve for the composite material by interpolation. The latter can then be used to define an appropriate FAD via the Option 2 procedure.

The yield strengths of both materials must be known in order to apply the procedure. Advice on multi-material collapse solutions is available in R6 Chapter IV.2. However this covers only a very limited range of simple geometries. Nevertheless, it may be possible to scale the collapse solution for a more complex geometry on the basis of the most appropriate of these simple geometries.

4.25 Describe in broad outline the aims of the local approaches to fracture, the range of models available, and how they are used in practice

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes21.pdf

The Mentee would be expected to know that such things as local approaches exist, and to describe in broad principle what they are about. A detailed knowledge of specific models would not be expected. However, R6 Section III.9 makes an interesting read. It is confined to homogeneous and isotropic materials.

Local approaches are specific models of the fracture process taking place within the 'process zone', i.e. near the crack tip. Rather than merely appealing to K or J control, these models postulate fracture criteria based on the process zone stresses or strains – generally in the form of some damage integral. R6 Chapter III.9 discusses four specific models, one for cleavage fracture and three for ductile fracture.

The Beremin cleavage model, which is based on a Weibull "weakest link" approach, is a lot of work to implement. The Rousselier and Gurson ductile models require specialist finite element codes to implement, since they involve model-specific constitutive relations.

The Beremin ductile model is probably the simplest to implement. It is based on the Rice and Tracy model of ductile void growth. The void size is expressed in terms of an integral over a function of the hydrostatic:Mises stress ratio, with the plastic strain as the independent variable. The fracture criterion is then a critical void size (roughly). The critical value is determined by performing an elastic-plastic FE analysis of fracture specimens. Elastic-plastic analysis is essential because the damage function is an integral over plastic strain. The same type of FE analysis is also required for the structure. Sufficient refinement is required in both specimen and structure meshes to capture the true behaviour within the process zone. This may be the difficult part.

It is moot to ask what advantages the local approaches have with respect to, say, a conventional fracture mechanics approach which includes other refinements such as taking benefit from low constraint (see 4.23). In the case of cracks, I do not know whether there is additional benefit in the general case. However, one advantage is that the local approaches may be used to assess fracture from notches, i.e. large but finite stress concentrations. It is clear that notches with sufficiently small root radii make a structure more susceptible to fracture, especially for brittle materials. But assessing this situation is problematical with conventional fracture mechanics.

A further area where local approaches have proved beneficial in applications, in this case in the assessment of ductile materials, is where two large secondary stresses were acting – both in excess of yield magnitude. Whilst the other facilities within R6, such as negative ρ (or $V < 1$), see 4.4, and the special provisions for residual stresses (4.12), do give some alleviation in such a case, the local approach has proved to provide a greater benefit.

4.26 Describe the key steps in a 'proof test' argument

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

This is covered in R6 Chapter III.10 The proof test argument has a venerable history, having formed the main safety assessment for Magnox reactor pressure vessels for many decades. The main attraction of the proof test argument is that it does not require input from inspections. The approach is,

i. Evaluate the largest/deepest crack which would just survive the proof test;

ii. Calculate how such a crack would grow in-service up to the assessment time;

iii. Assess whether this grown crack survives the worst relevant fault condition.

Step (i) effectively takes the place of inspection, providing a bound on the crack size. The disadvantage is that the proof test will generally have been a long time ago. So crack growth may be substantial. The proof test argument tends to be useful, therefore, only in cases where degradation mechanisms are slight. Also, it can only be useful if the proof over-pressure factor exceeds the degree of overstressing in the worst fault.

The proof test argument tends to turn usually conservative assumptions upside down. Thus, using lower bound materials data throughout a proof test argument will be non-conservative in general. This is because, for conservatism, the crack surviving the proof test must be as large as possible (hence maximising subsequent growth). So the use of best estimate or even upper bound data is motivated. It may be necessary to employ upper bound data during the proof test, but best estimate, or even lower bound, data during the fault condition. This further erodes margin and may be fatal to the case. Similarly, in ductile materials, the largest defect to survive the proof test really should take account of stable tearing. So the defect postulated may end up being very deep indeed, making it difficult to demonstrate any residual margin.

4.27 Describe the effect of warm pre-stressing on the subsequent fracture resistance of a structure

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes20.pdf

Here appears to be a distrust of warm pre-stressing arguments, though I do not know why. I suspect they could be deployed more widely.

The Mentee should be able to describe the key feature of the warm pre-stressing (WPS) phenomenon. It is this. Suppose we have a ferritic material exhibiting a brittle-ductile transition. Suppose that at some low temperature T1 the material is relatively brittle, and a certain defective structure would sustain a load F1 before fracture. Suppose that the same structure at some higher temperature, T2, could sustain a significantly larger load, F2 > F1, because the material is then ductile. The question arises: suppose we load the structure at temperature T2 to a load in excess F1, but less than F2 to avoid fracture. Keeping this load constant, we now decrease the temperature to T1. What happens? Naively, the structure should apparently fracture. But it does not, in general. The WPS phenomenon is, very crudely and slightly inaccurately speaking, that you cannot induce fast fracture simply by reducing temperature at constant load.

R6 Chapter III.9 provides an assessment procedure incorporating the WPS effect applicable in more general situations than that described above.

The mechanisms underlying WPS are crack tip blunting and strain hardening within the process zone, and, in cases where unloading and reloading has occurred, the formation of compressive residual stresses in the process zone.

4.28 Describe the phenomenon of crack arrest and when it might occur

This is addressed in http://rickbradford.co.uk/T73S02TutorialNotes22B

The Mentee should be aware that R6 Chapter III.12 exists and that it deals with assessing the conditions under which dynamically propagating brittle fracture may nevertheless arrest before catastrophic failure of the structure occurs. I would not expect much more since these arguments are rarely deployed. The Mentee might like to offer reasons why arrest might occur.