T73S04 (R5V2/3) – Session 35: Fatigue

Last Update: 12/4/15

Relates to Knowledge & Skills items: 1.32, 1.33, 1.34, 1.35, 1.36, 1.37

Fatigue strain range with/without creep; Fatigue endurance, S-N curves, correction for size effects; FSRF/WSEF/WER;Definition of D_f; Mises versus Rankine strain range; Influence of cycle sequence on fatigue endurance; R5 combination of creep and fatigue damage versus other codes; Sub-surface damage/initiation: the scope & limitations of R5 methodology; Beyond R5 (R2): High cycle fatigue, sensitivity to mean stress (Goodman diagram, etc);

Qu.: Does R5 Assess Gross Fatigue Failure or Fatigue Crack Initiation?

- R5V2/3 assesses creep-fatigue crack initiation, and hence the fatigue part necessarily relates to crack initiation.
- R5V6: The fatigue assessment methodology for transition joints in R5V6 makes no mention of, or allowance for, crack initiation and must be regarded as a gross fatigue failure assessment.
- R5V7 of course does not assess fatigue (because it covers steady loading only).

Qu.: But doesn't R5V2/3 use fatigue endurance data?

Yes.

The input data used in R5V2/3 is from fatigue tests taken nominally to 'failure'. However, 'failure' in fatigue tests does not always mean that the specimen breaks into two parts. Most often fatigue tests are conducted in uniaxial strain control, and 'failure' is defined as the maximum load decreasing by 25%. However, this is taken to be equivalent to the specimen breaking if the test had been carried out at constant stress range.

Qu.: So how does R5V2/3 assess fatigue crack initiation?

An adjustment is carried out to the endurance data according to the depth of crack, a_0, which the User wishes to regard as "just initiated". This adjustment is specified in R5V2/3 Appendix A10, Section A10.1, and is summarised here:-

<u>Crack Initiation Size Adjustment to Fatigue Endurance</u>

Fatigue endurance data is obtained from specimens typically of diameter 6-10mm. The 'failure' of such a specimen is taken to consist of,

(a) a number of cycles, N_i, to initiate a very shallow crack of depth 0.02mm, and then,

(b) a further N_g cycles in which the crack grows to 'fail' the specimen.

Suppose the endurance of the specimen is N_l cycles at the strain range of interest. Then $N_l = N_i + N_g$. The number of cycles to initiate a 0.02mm crack is taken to be the same in your structure as in the test specimens, and is given explicitly in terms of the total endurance, N_l (e.g., from R66 data) by,

$$\ln N_i = \ln N_l - 8.06 N_l^{-0.28} \qquad (1)$$

and hence N_g is found as $N_l - N_i$.

If your chosen a_0 is less than 0.02mm, then the initiation-endurance to use in the assessment is just N_i. In general, though, your chosen a_0 will be greater than 0.02mm, perhaps 0.2mm or some suitable percentage of the wall thickness. In this case some allowance for crack growth from 0.02mm to a_0 is required – an extra N'_g cycles. This is given by $N'_g = MN_g$ where the fraction M is,

For $a_0 < 0.2mm$ $$M = \frac{a_0 - 0.02}{0.2\ln(a_l / 0.2) + 0.18} \quad (2a)$$

For $a_0 > 0.2mm$ $$M = \frac{0.2\ln(a_0 / 0.2) + 0.18}{0.2\ln(a_l / 0.2) + 0.18} \quad (2b)$$

where a_l is the diameter of the test specimen (probably 6mm-10mm). The initiation-endurance to use in the assessment is then $N_0 = N_i + N'_g$, and the fatigue damage per cycle is $\Delta D_f = 1/N_0$.

Qu.: Does the above size correction apply to parent or weld material?

Generally the above process is applied to parent endurance data in order to obtain the size-corrected endurance applicable to the assessment of a parent feature.

However, in principle the same process could be applied to fatigue endurance data obtained from tests on weld material – so the resulting size-corrected endurance would be appropriate for the assessment of a position within weld material. In practice weld material fatigue test data is not often available so that other means of estimating weld endurance exist within the procedure (FSRF, WSEF, WER – see below).

Qu.: How are different cycle types assessed?

Generally Miner's Law is used, i.e., the damage is given by $D_f = \sum_j \frac{n_j}{N_{0j}}$. (3)

However, in truth, the damaging effect of a mix of different cycle types can depend upon their order. R5 suggests that order effects might result in the fatigue damage differing from that calculated by a factor of up to ~2. Some advice on this is offered in R5V2/3 Section A10.3. However, so long as D_f is small this will not be of great concern. Moreover deterministic assessments will usually use the lower bound endurance. The factor by which the lower bound endurance differs from the mean will usually be far more than a factor of 2, so cycle order effects need not undermine your assessment. (It may be more significant in probabilistic assessments where the attempt is made to be more realistic).

Qu.: What is the Fatigue Strength Reduction Factor (FSRF)?

The FSRF is the traditional means by which the reduction of fatigue endurance due to the presence of a weldment is taken into account in an assessment. It is a factor used to increase the strain range.

The FSRF is ***not*** a factor on stress range nor on cycles.

Qu.: Does R5V2/3 employ a FSRF?

No. It did until the 2014 when R5V2/3 Issue 3 Revision 2 included a revised Appendix A4 for weldments which replaced FSRFs with WSEF and WER. This, at long last, incorporated the main elements of Manus O'Donnell's 2005 report, M.P.O'Donnell, "Proposed Changes to R5 Volume 2/3 Appendix A4: Treatment of Weldments", E/REP/BDBB/0067/GEN/05, March 2005.

In practice I have been advising not to use the older, FSRF based, procedure for many years. This advice is now official. However, use the new Appendix A4 not the O'Donnell report, there will be differences of detail.

Qu.: Why does a weld degrade fatigue endurance?

There are two reasons why welds degrade fatigue endurance,

(i) The local stress concentration at the weld toe, the weld root or any unfused land;

(ii) The intrinsically poorer fatigue resilience of weld/HAZ material (which may be due to many metallurgical factors, but one is the likely presence of microscopic flaws).

The FSRF incorporates both the above effects since it is obtained by testing welded specimens or features. The FSRF is defined as the ratio of the plain parent strain range to the welded feature (remotely applied) strain range giving the same endurance.

Qu.: How does the new weldments procedure address the above issues?

Rather than incorporate both the above issues (i.e., both the geometrical SCF and the materials effect) into one factor – the FSRF – the new procedure separates the two effects.

- The Weld Strain Enhancement Factor (WSEF) accounts for the geometrical SCF;
- The Weld Endurance Reduction (WER) accounts for the weld material effect.

The reason for this separation is that the two factors are used in very different ways in the (proposed) revised procedure.

The combined effect of the WSEF and the WER should be the same as the FSRF as regards fatigue. However, the new procedure differs from the older procedure in that only the WSEF affects the assessed creep damage - and so the revised procedure will generally be less onerous than the older procedure. (In the older procedure the whole of the FSRF affected the assessed creep damage).

Qu.: How is the WSEF used in the procedure?

The WSEF is used within the hysteresis cycle construction procedure. This will be covered in full in Session 37. In brief the procedure is,

- The elastic-plastic strain range evaluated using parent properties for a given half-cycle (via the Neuber construction) is factored by the WSEF to estimate the strain range for the weldment;
- The corresponding stress range for the weldment is found from the parent Ramberg-Osgood cyclic stress-strain curve at the above factored strain range.

Consequently the WSEF has two effects,

- It directly increases the strain range – and hence reduces the fatigue endurance, and,
- It affects the stress range and hence will, in general, affect the start of dwell stress and hence the creep damage.

The latter aspect is peculiar to the R5V2/3 procedure: the SCF due to the geometrical features of a weld will increase the ***creep*** damage. Note that the WSEF is derived from fatigue tests, but is being used in the procedure to enhance the creep damage (as well as the fatigue damage).

Qu.: What values are advised for austenitic WSEFs?

WSEFs for austenitic weldments have been derived by re-analysing fatigue data for austenitic weldments. The advised values from R5V2/3 App.A4 Issue 3 Rev.2 Table A4.1 are reproduce as Table 1 below against the following weldment categories,

Type 1. A butt weldment of full penetration joining two plates which are nominally parallel and of equal thickness at the joint. The back side may be inaccessible but backing strips should not be permanent.

Type 2. A fillet weld or T-butt weldment of full penetration joining two plates which are nominally perpendicular and may be of different thicknesses. The back side may be inaccessible but backing strips should not be permanent;

Type 3. A fillet weld or T-butt weldment which may be of partial or zero penetration and joins two plates which are not restricted in nominal direction and may be of different thickness. The back sides may be inaccessible.

Table 1
WSEFs to be applied to austenitic weldments for thicknesses less than 25mm*

R5 TYPES	RCC-MR TYPES	WSEF
1	I.1, I.2, I.3, II.1	1.16
2	III.1, III.2	1.23
3	V, VI, VII	1.66

*For weldments with an undressed weld toe present and nominal plate thicknesses greater than 25mm and up to 150mm, the above WSEFs should be multiplied by $(t/25)^{0.25}$; however, in recently issued design advice#, it is noted that the factor of $(t/25)^{0.25}$ relates to fatigue cracking from the toe of a weld and, therefore, is not required for weldments without this type of detail; also, for a weldment with a weld toe present, the thickness adjustment is only necessary when assessing the weld toe location itself. #CEN Standard on Unfired Pressure Vessels, EN 13445, Part 3 Section 18, para. 18.10.6.1 [A4.8].

Qu.: What values are advised for ferritic WSEFs?

The re-analysis of ferritic weldment fatigue data to extract WSEFs is an outstanding work area. In the interim, R5V2/3 App.A4 Issue 3 Rev.2 Table A4.2 has included the older FSRFs to be used as WSEFs. This will be substantially pessimistic. Their values are given in Table 2 below.

Table 1
WSEFs to be applied to ferritic weldments for thicknesses less than 25mm*

R5 TYPES	RCC-MR TYPES	WSEF
1	I.1, I.2, I.3, II.1	1.5
2	III.1, III.2	2.5
3	V, VI, VII	3.2

**Same footnote as Table 1.*

Qu.: What is the "weld toe SCF"?

The new Appendix A4 procedure advises that, for Type 2 and 3 weldments, a stress concentration factor (SCF) is applied to the elastic stress range prior to Neuberising, etc. It is only required if the weld cap angle exceeds 30^o for undressed welds, or 39^o for dressed welds. Its value is,

$$SCF = \sqrt{\theta/\psi} \quad \text{and} \quad \begin{array}{ll} \psi = 39^\circ & \text{for dressed welds} \\ \psi = 30^\circ & \text{for undressed welds} \end{array}$$

No such SCF is required for Type 1 welds. (At least, that's my reading of App.A4, though it is not entirely clear). The weld toe angle in question relates to the attachment side of the weld.

Note: this SCF is applied to the elastic stress range. In contrast, the WSEF is applied to the elastic-plastic strain range.

Q.: Should best estimate or lower bound endurance data be used?

There was potential for confusion in the previous issue of App.A4 in that certain FSRFs were advised to be used with best estimate endurance in order to produce a lower bound weldment endurance (i.e., the bounding nature was within the FSRF itself). This is no longer the case in Rev.2. The WSEFs of Tables 1 or 2 will give the best estimate endurance if best estimate endurance data is used. Conversely, they will give the lower bound endurance if lower bound endurance data is used.

Qu.: What is the WER and how is it used in the procedure?

The WER is applied to parent fatigue data.

The Weld Endurance Reduction consists simply of ignoring the incubation phase in the size correction procedure. In the procedure of R5V2/3 Appendix A10, §A10.1 described above, the endurance to be used becomes just N'_g rather than $N_i + N'_g$.

If weld material fatigue endurance data is available then the WER need not be applied since it is implicit in the data. In fact the procedure strictly requires the lower endurance obtained from parent data with the WER applied or from weldment data with no WER applied. The unavailability of weldment fatigue data in many cases will mean that parent data with the WER applied is the only option.

Note, however, that even if the WER is not required, i.e., for weldment endurance, it is still necessary to apply the size correction, as described above (from R5V2/3 Appendix A10, §A10.1).

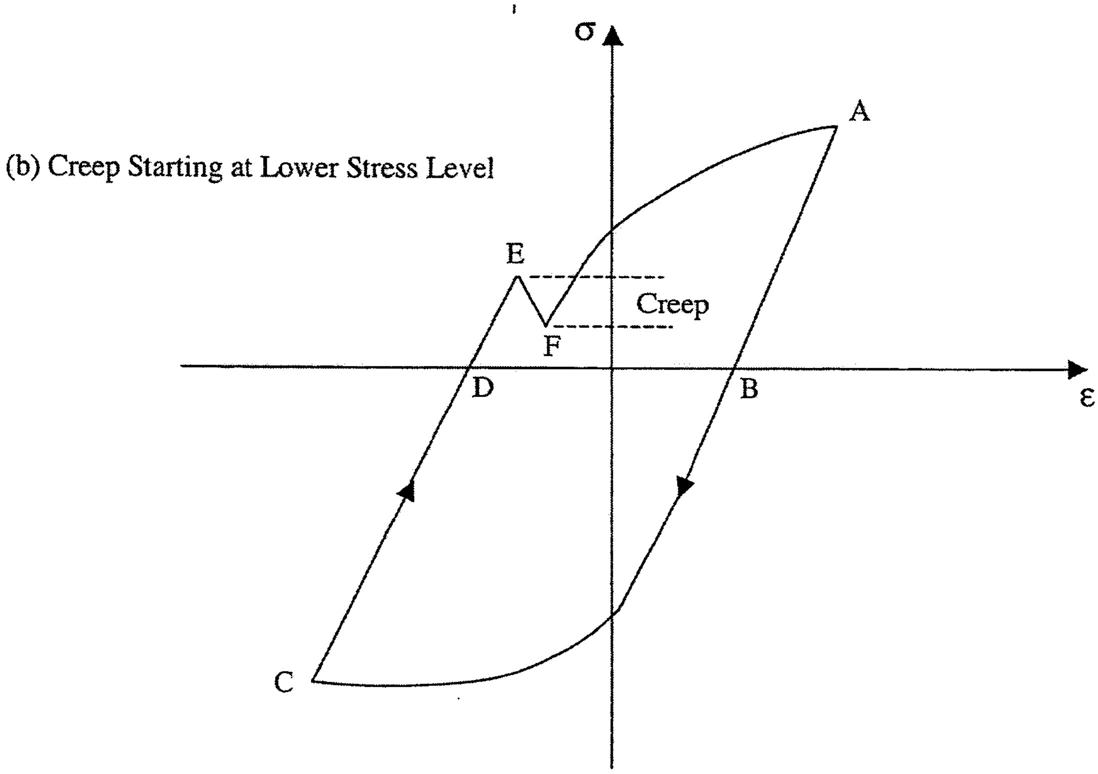
Qu.: In FE models, should the detail of the weld be included in the mesh?

The Rev.2 version of App.A4 states that,

- For dressed weldments, geometric modelling should be an accurate representation of the (smooth, dressed) weld profile.
- For undressed weldments, the nominal geometry of the weldment, but not the fine details of the weld profile, should be modelled. A conventional profile deduced from the shape of the weld preparation may be used. Undue mesh refinement in the region of surface singular points (e.g., the weld toe or an unfused land) should be avoided as the stresses at such points do not tend to a finite value as the mesh is increasingly refined.

This leaves the stress for an undressed weldment rather ill-defined in my opinion. However, this has always been the case. The same problem is found in, for example, FE reheat cracking models.

Figure 1: Definition of Points on Hysteresis Cycle



Qu.: What is the final strain range for fatigue damage estimation?

The Mises strain range is the larger of $\Delta\varepsilon'^{ABC}_{ep}$ for the half-cycle without creep and $\Delta\varepsilon'^{CEA}_{ep}$ for the half-cycle with creep, noting the additional volumetric correction, where,

$$\Delta\varepsilon'^{ABC}_{ep} = \Delta\varepsilon^{ABC}_{ep} + \Delta\varepsilon_{vol} \qquad (4a)$$

$$\Delta\varepsilon'^{CEA}_{ep} = \Delta\varepsilon^{CEA}_{ep} + \Delta\varepsilon_{c} + \Delta\tilde{\varepsilon}_{vol} \qquad (4b)$$

The detailed derivation of these quantities was covered in Session 33.

The manner in which the SCF and WSEF is incorporated to alter (expand) the hysteresis cycle and increase the strain range (and also affect the dwell stress) will be spelt out in detail in Session 37.

Qu.: Does fatigue damage depend upon stress triaxiality?

Yes.

When the assessment is close to uniaxial, the Mises equivalent strain range can be used to find the endurance, N_l, which feeds into the procedure stated above, i.e., Equs.(1-2).

If the assessment is shear dominated this procedure can still be used but will be conservative. For reduced conservatism use the multiaxial procedure defined in R5V2/3 Appendix A10, §A10.2.

Conversely, if there is significant biaxial or triaxial tension, the uniaxial methodology using the Mises strain range may be non-conservative and the multiaxial route should be followed.

Qu.: What is the multiaxial fatigue methodology?

Fatigue tends to correlate better with Tresca or Rankine strain ranges than with Mises strain range. This provides an alleviation to a Mises based assessment in the case that the first and third principal stresses are of opposite sign (including the case of shear dominance). However, in the case of biaxial or triaxial tension, the use of the Mises strain range may be non-conservative.

I am rather confused by the advice in R5V2/3 §A10.2.1. One reason is that the strain range that is advised is based on an elastic stress range divided by a secant modulus, a quantity which has the potential to be grossly in excess of any actually occurring strain range, and hence unphysical.

I offer the following interpretation of what might be the intended spirit of §A10.2 in the case of biaxial or triaxial tension:-

- Evaluate the principal elastic stress range $\Delta\sigma_1$ and the Mises elastic stress range $\Delta\bar{\sigma}$ between the bounding top and bottom peaks of the cycle;
- Estimate the Rankine strain range as $\Delta\varepsilon_R = \frac{\Delta\sigma_1}{\Delta\bar{\sigma}}\Delta\bar{\varepsilon}$, where $\Delta\bar{\varepsilon}$ is the Mises strain range which follows from the hysteresis cycle construction, e.g., for a cyclelike Fig.1, from Equs.(4a) or (4b), including the SCF and WSEF effects;
- Employ $\Delta\varepsilon_R$ to find the fatigue endurance – including all the corrections, factors, etc, as discussed above.

Qu.: Is the multiaxial methodology commonly employed?

No.

It should be, but it isn't.

One reason is it has been widely misunderstood (well, by me anyway). Perhaps the above re-interpretation might help.

Qu.: What about initiation of cracks sub-surface?

R5V2/3 was originally written with surface crack initiation in mind. Thanks to reheat cracking and the Spindler fraction formulation, the creep damage part of the assessment can now be done equally for a sub-surface point (at least when a Spindler fraction fit exists for the material in question). The above referenced methodology for fatigue under triaxial stress completes the picture and hence permits a sub-surface point to be assessed.

However, R5V2/3 Section 11.15 notes that, "*Under multiaxial loading it is probable that the two types of damage (i.e., creep and fatigue) will develop on differently oriented planes hence reducing the possibility of a strong interaction*". I interpret this to mean that procedure is conservative.

Qu.: What do S-N curves look like?

The data in R66 is confined to high strain, and relatively small numbers of cycles – mostly not exceeding ~10^5 cycles. This is usually adequate for our purposes since major plant cycles (reactor cycles) are of the order of a few hundred, and even refuelling cycles are only a few thousand. But note that fatigue endurance data commonly extends to far larger numbers of cycles. Test data up to ~10^8 is common.

Beyond R5 – High Cycle Fatigue & Other Matters

Qu.: Is there really a fatigue limit?

Figure 2: Some materials have a fatigue limit, some don't…maybe?

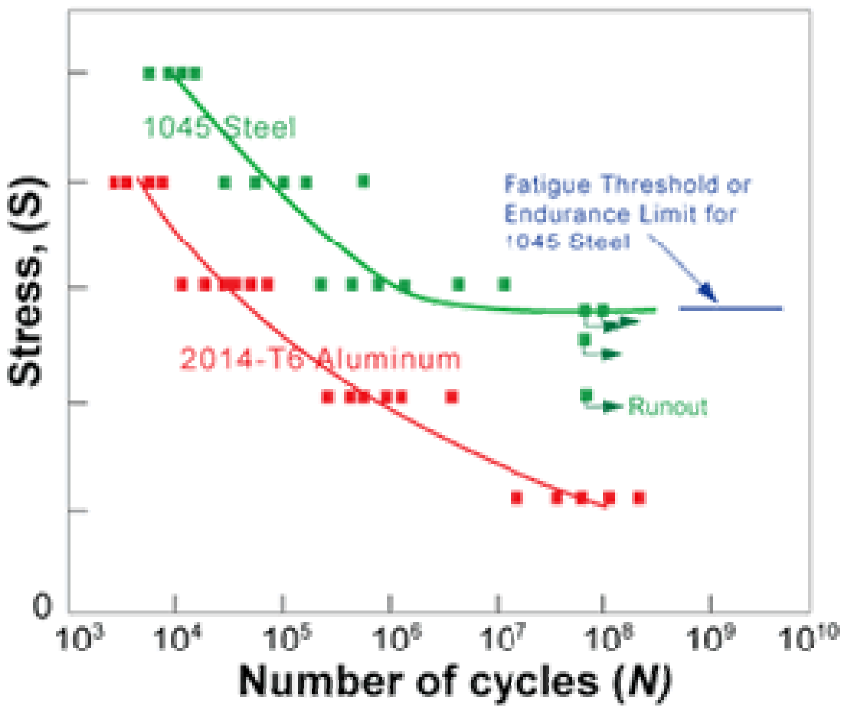
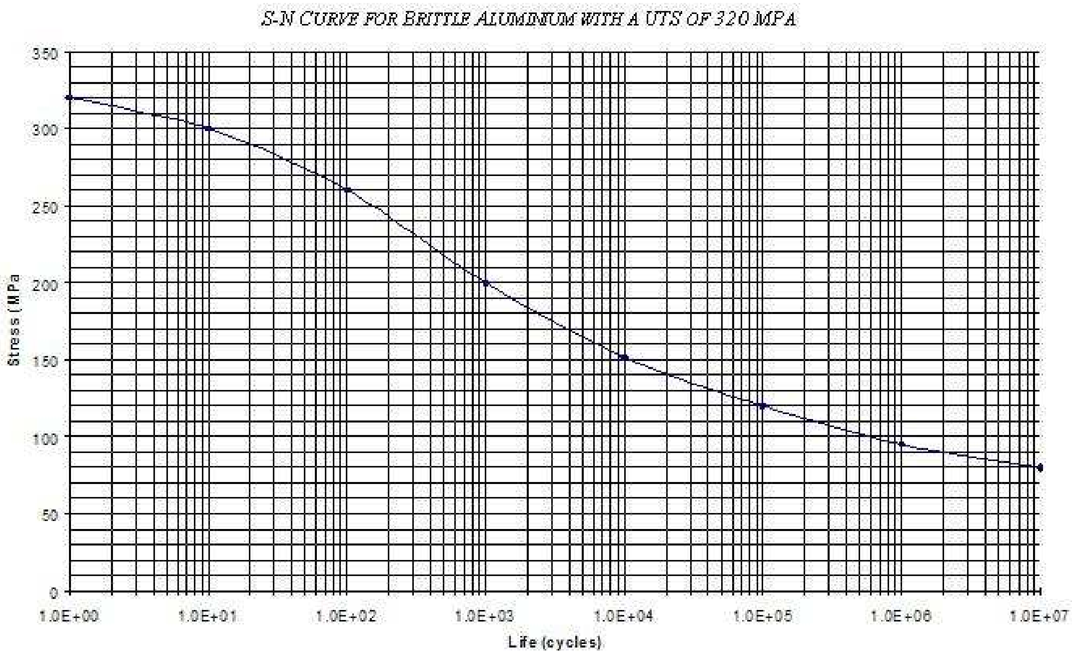


Figure 3: Aluminium Fatigue Endurance



It is generally taught that some materials, e.g., steels and titanium, have a "fatigue limit", i.e., a stress range (or strain range) below which the fatigue endurance is infinite. In contrast, other materials, e.g., aluminium and copper, do not exhibit a fatigue limit.

If there is a fatigue limit, it takes at least 10^7 cycles – and probably 10^8 cycles - to get there.

The existence of a fatigue limit even in steels is not beyond doubt. Periodically people challenge the idea.

Figure 4

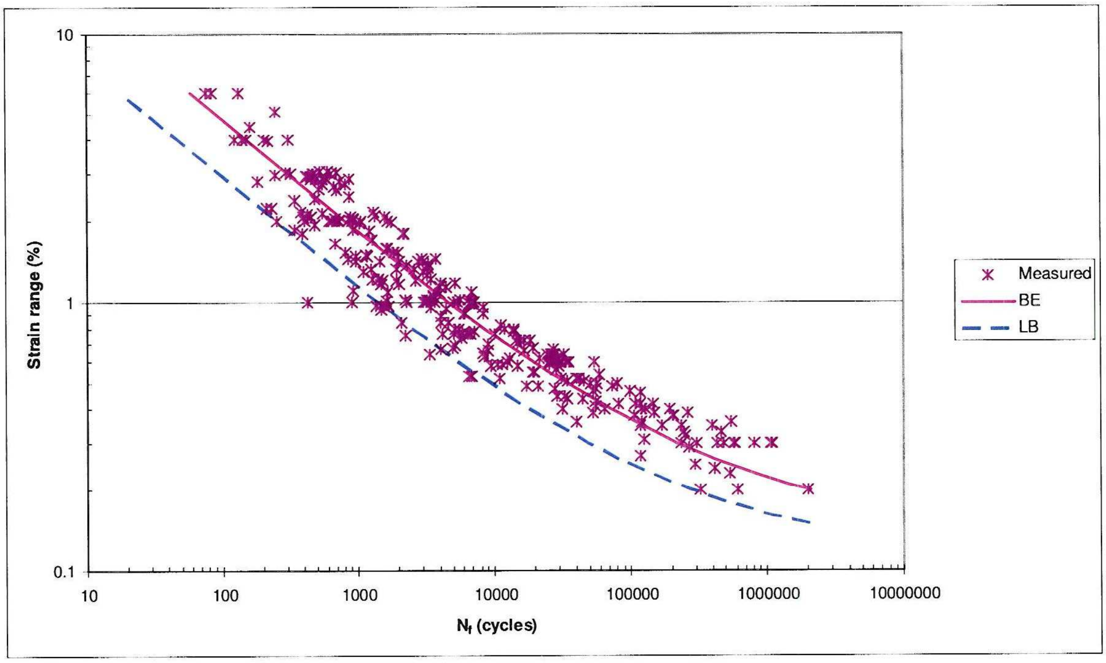


FIGURE 9.1 FATIGUE ENDURANCE OF C/C-Mn STEEL (RT-400°C).

Qu.: Can fatigue occur if there is no plasticity?

No.

It the material cycles strictly elastically then it absorbs no energy – so how can any damage be done?

But…

Qu.: What is the effect of microscopic flaws?

Suppose that you have applied stresses to a specimen within the yield stress – even within the proportionality limit stress. The cycling is apparently purely elastic. So, can there be any fatigue damage?

The answer is, in principle, yes – if there are small flaws within the material. These flaws will generate plasticity, and possibly cyclic plasticity. So in reality there is a mechanism for energy absorption and hence damage. But you do not detect this from the gross applied stress and displacement which looks linear.

Qu.: In practice does fatigue occur when there is no (gross applied) plasticity?

For numbers of cycles less than ~10^6, no.

For this purpose it is best to express the S-N data using strain range rather than stress range, as is done in R66. The R66 data has no failures below a strain range of 0.2%, implying that a "fatigue limit" in steels can be identified with elastic cycling. It would be better to say than the fatigue endurance is very large for strain ranges less than 0.2%, say greater than 10^6 cycles. There probably is no strict fatigue limit – and any apparent limit will depend upon the population of microscopic flaws.

Qu.: Surface condition

Fatigue endurance, especially at low stress ranges and large numbers of cycles, is extremely sensitive to surface condition. Rough machined specimens will display markedly inferior endurance, with cracks initiating at the machining marks.

Standard fatigue endurance tests specify the surface condition required.

Qu.: Is the mechanism of fatigue endurance always surface crack initiation?

It is often assumed that fatigue failure starts with the initiation of a surface crack. But this appears to be untrue in general.

The sensitivity to surface condition implies that surface crack initiation will be the mechanism when the surface is in a poor condition.

But what about specimens prepared with the required, standardised, surface condition? The R5V2/3 procedure, which partitions the endurance into a crack initiation part and a crack growth part, does imply that crack initiation and growth is the fatigue mechanism. But it does not necessarily follow that the cracks in question are surface cracks. Could cracks also be initiating sub-surface?

An interesting paper presented at PVP2010 suggests that sub-surface damage of some sort is just as significant as surface damage. An extract from my conference notes:

"(M.Kamaya, Japan): A nice piece of work this, addressing an issue I'd not thought of before: Is fatigue endurance determined by surface damage/crack initiation – or is the sub-surface material just as damaged? It would appear that the latter is the case. This was explored (using 316ss) by taking uniaxial specimens to 50%-80% of N_f and then machining off the outer layer. The endurance of the prior cycled sub-surface material was then investigated. Different experiments employed either the same or different strain ranges cf. the prior damage tests. The endurance was certainly reduced by the prior cycling, and it appeared that Miner's Law held. In other words, it appears that the sub-surface material was just as damaged as the surface material. This was confirmed by metallurgical examination which showed sub-surface micro-cracks had indeed initiated. The nucleation sites were found in many cases, and were very tiny. They reminded me of the initiation sites in the cracked HY2/TOR GC impellers."

Qu.: I thought that mean stress could reduce fatigue endurance?

Yes, it sure does.

Qu.: Does R5 include allowance for mean stress effects on fatigue?

No.

Qu.: Should it?

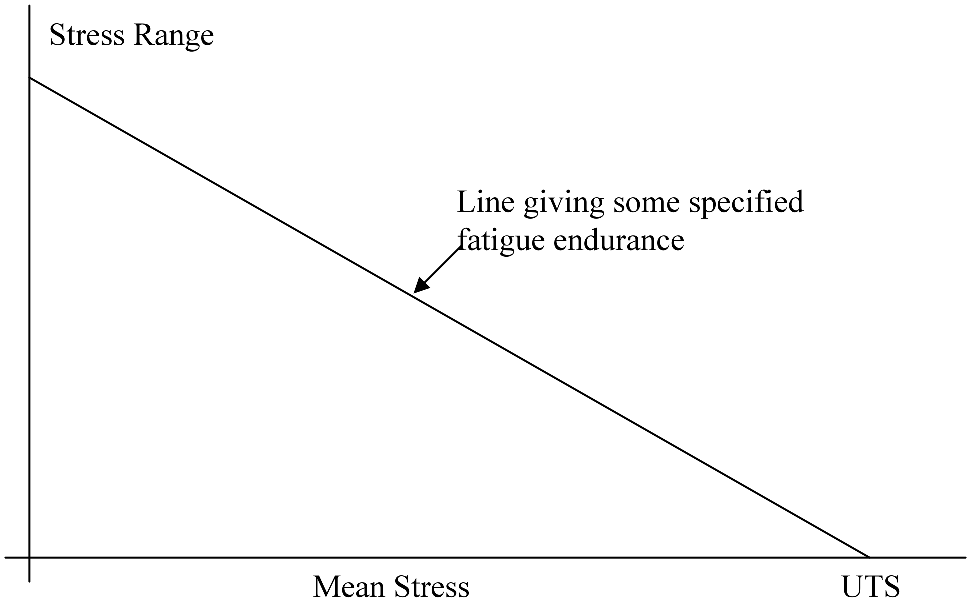
In my opinion, yes.

But for low cycle applications it is probably not so important. It can become very important in vibration fatigue. Vibration stress amplitudes might be quite modest but still be damaging if superimposed upon a high mean stress. The Torness / Heysham 2 gas circulator impeller failures are an example.

Qu.: How can the effect of mean stress be assessed?

One way is to use the Goodman diagram…

Figure 5: The Goodman Diagram: Effect of Mean Stress on Fatigue Endurance



Qu.: What is the stress ratio, R?

Here R is the ratio of the minimum to maximum stress in the cycle. Hence if $\Delta\sigma$ is the stress range and σ_m is the mean stress then,

$$R = \frac{\sigma_{\min}}{\sigma_{\max}} = \frac{\sigma_m - 0.5\Delta\sigma}{\sigma_m + 0.5\Delta\sigma} \quad (6)$$

Hence fatigue tests conducted using fully reversed loading have R = -1 and a mean stress of zero.

Tests conducted by repeatedly applying and removing a load would have R = 0 and a mean stress equal to half the maximum stress.

Tests with very high mean stress will have R approaching unity.

Qu.: What is the Goodman diagram in algebraic form?

Algebraically this means that the effective stress range, or alternating stress, should be factored up according to the ratio of the mean stress to the UTS:-

$$\text{Stress range to use in fatigue endurance equation} = \frac{\Delta\sigma}{1 - \sigma_m / UTS} \quad (5)$$

This assumes that the fatigue equation being used was based on tests with zero mean stress (R = -1), i.e., fully reversed fatigue tests.

Note that this means that plant cycles from zero to some maximum stress would require a mean stress fatigue correction – which is not normally done in my experience (since R5 does not require it). Should it?

The Goodman correction will be minor for modest mean stresses. It really matters, though, if there is a moderate stress cycle superimposed on top of a large static stress.

The catastrophic failure of two gas circulator impellers at Torness in 2002 can be attributed to a large centrifugal stress on top of which a relatively modest vibration stress was superimposed.

Qu.: What is the Gerber parabola?

The Goodman straight-line interaction diagram may be rather conservative. A parabola between the points $(0,S_a)$ and (UTS,0) is often a better representation of the data and is known as the Gerber parabola. Algebraically this would be,

$$\text{Stress range to use in fatigue endurance equation} = \frac{\Delta\sigma}{1-(\sigma_m / UTS)^2} \qquad (7)$$

Data generally lies between the Goodman line and the Gerber parabola – so I'd advise using the more conservative Goodman line unless you have evidence to the contrary.

Qu.: What causes fatigue damage in an uncracked item?

At one level the answer is "repeatedly depositing energy into the material". This happens if there is a hysteresis cycle. And we have seen that this will occur much more readily at the tip of flaws or notches.

In fact, even microscopic flaws will have severe stress raisers at their tips. So you may think that you have applied a stress in the elastic range but there can still be very small regions of plastic energy absorption which are not apparent from the gross structural load-displacement behaviour.

Qu.: What is the effect of pre-existing microscopic material flaws?

It is well known that even very small pre-existing flaws will markedly reduce the fatigue endurance of a structure. Moreover, the presence of weldments, even high quality weldments, increases the probability of such defects very markedly. One school of thought is that the fatigue performance of a material depends relatively little on the matrix microstructure, but rather is controlled by the non-metallic inclusions and/or intrinsic defects. From this point of view, the scatter in fatigue endurance between specimens of a material is due mostly to the random variations in the sampled inclusions and defects.

The typical design code approach to the fatigue assessment of welds (e.g. PD5500, BS7608) is based on S-N curves derived empirically, i.e., by fatigue testing actual weldments. The implicit assumption is that a given class of welds has a well defined probability distribution of defects which is reflected in the scatter found in the endurance data. Thus, by choosing the appropriate weld class, and a suitable lower bound S-N curve for that class, a design assessment can be performed.

The drawback to this approach is that a weldment which has been made to exacting standards will have far better endurance than implied by the lower bound design S-N curve.

The strength of the approach is that the design curve implicitly includes allowance for typical levels of weldment defectiveness (including the effects of weld toes, weld roots, etc). Indeed, this is the reason for the weld endurance recommendations in codes being so markedly lower than those for parent material.

Within R5 the same issue was previously dealt with via the FSRF and, in Revision 2 to R5V2/3 Appendix A4, the same role is played by the two factors: WSEF and WER.

Qu.: Can the effect of pre-existing microscopic flaws be quantified?

The literature abounds with models. In the case of very high cycle fatigue, one such is due to Murakami.

As discussed above, the scatter in fatigue endurance between specimens of a material is due mostly to the random variations in the sampled inclusions, intrinsic defects and welding flaws (assuming the surface condition is good). The matrix material contributes to the fatigue strength only via its gross resistance to plastic straining, as may be correlated, for example, with hardness. Thus, Murakami* has suggested the following empirical relation between fatigue endurance and Vicker's hardness (H_V), defect area and stress ratio ($R = \sigma_{min} / \sigma_{max}$), for sub-surface defects,

$$\text{endurance stress range limit} = 1.56 \frac{(H_V + 120)}{\left(\sqrt{area}\right)^{1/6}} \left[(1 - R)/2\right]^{\alpha} \qquad (8a)$$

where, $\alpha = 0.226 + H_v / 10{,}000$ (8b)

Equs.(8) give the endurance stress range limit in MPa for √area measured in microns.

For example, for a Vicker's hardness of 200 Hv and a flaw size of 0.1mm x 0.1mm, the endurance stress range limit (twice the amplitude) evaluates to 232 MPa, 196 MPa or 111 MPa for R = -1, 0 or 0.9 respectively. This illustrates the effect of high mean stress.

This model effectively claims that the material fatigue properties are determined by the Vicker's hardness and the size of the largest microscopic flaw in the relevant region. I'm not suggesting you actually use this model (it's only for very high cycle fatigue), but it is interesting that such things exist.

**Y.Murakami, "High and Ultrahigh Cycle Fatigue", in "Comprehensive Structural Integrity", Volume 4, pp.41-76, Elsevier, 2003 (ISBN 0-08-044155-6).*

Qu.: Is there a clear demarcation between high strain and high cycle fatigue?

As far as I am aware, not really – it's a bit vague.

But 10^4 is still low cycle, whereas I guess $>10^6$ is high cycle.

But high cycle tests can go up to $\sim 10^9$ cycles. This is not as difficult as you might imagine. After all, at 50Hz, 10^9 cycles takes only ~8 months.

Qu.: Is there a specific Company procedure for high cycle fatigue?

Yes. It is R2.

You can find it here: G:\Engineering\SISB\Ltasks\SAG\Standards\R2.

It's worth a look – but it is very voluminous. Our local contact on R2 is John Hart. Well, it was. Who is it now? It should logically belong to Rotating Plant Dynamics.

The trouble with high cycle fatigue assessments usually is that you do not know what the stress ranges are – or, in general, the effective number of cycles either. This makes performing an assessment tricky. So an R2 assessment usually needs to be done in conjunction with a practical exercise of strain gauging or accelerometer measurements on live plant.

My rule of thumb is that, if your plant has been running for a number of years and hasn't broken – then it has not got a high cycle fatigue problem *during normal* *operating conditions*. The reason is that you knock up cycles so fast at cycling rates measured in Hz that the structure won't survive long if the amplitudes are above the fatigue limit/threshold. Not a very scientific approach, admittedly.

On the other hand, vibration conditions which occur only intermittently (e.g. during on load refuelling) can lead to gross failures many years down the line. An example is Torness gas circulator impellers, which failed 14 years after commissioning (though the same impellers had not been in service for the whole time).

Qu.: What is the effect of environment on fatigue endurance?

This is too large a topic to be handled thoroughly here. Salient points are,

- Water-phase aqueous environments, including wet steam, can be deleterious;
- If a water phase environment also includes aggressive anions (chlorides, sulphates, etc) then this is very bad – fatigue can enhance the corrosion issues which will occur (corrosion-fatigue, IGA, SCC, EAC generally);
- The reactor CO_2 environment was believed to be benign – until very recently. There is now a growing appreciation that the reactor CO_2 environment is probably more onerous in fatigue than both vacuum or air conditions. This is due to the high temperatures and the fact that CO_2 can cause oxidation and carburisation. The oxidation can then be synergistic with a creep or fatigue mechanism, whilst surface carburisation can degrade both the creep ductility and the fatigue endurance. Unfortunately there is little clear guidance at present, though the issue is now recognised within R66 in the form of warning notes. On the positive side, any significant deleterious effect is likely to be confined to items with high stress (e.g., > yield). Unfortunately, due to thermal transient effects, there are more of these than one would like (e.g., PCPV internal insulation cover plate retention hoops). So there could be widespread implications for CLA. This is being addressed under HiTBASS.

Qu.: What is the effect of high temperature operation on fatigue endurance?

- Most obviously, high temperature operation is usually accompanied by thermal stress – either in steady operation or in transient conditions, or both. This can considerably increase stress ranges associated with major plant cycles (reactor starts/shut-downs). However, this can be quantified.
- Exposure to high temperatures causes thermal ageing of materials. For ferritic materials (e.g., CMV), conditions in coal/oil fired plant main steam pipework (~570^{o}C) has caused both a degradation of fatigue endurance and softening of the material. The latter results in a greater strain range for a given load range, and hence further enhances fatigue damage. Combined with two-shifting and severe thermal transients, this resulted in TTIBC being common in coal/oil power plant main steam systems in the UK over the last 15 years. It is probably not a significant issue for BE/EdF nuclear plant due to much lower CMV operating temperatures and far fewer cycles.

Qu.: How are creep and fatigue damage combined in R5V2/3?

In R5V2/3, creep and fatigue damage are combined linearly, so that the total damage is just,

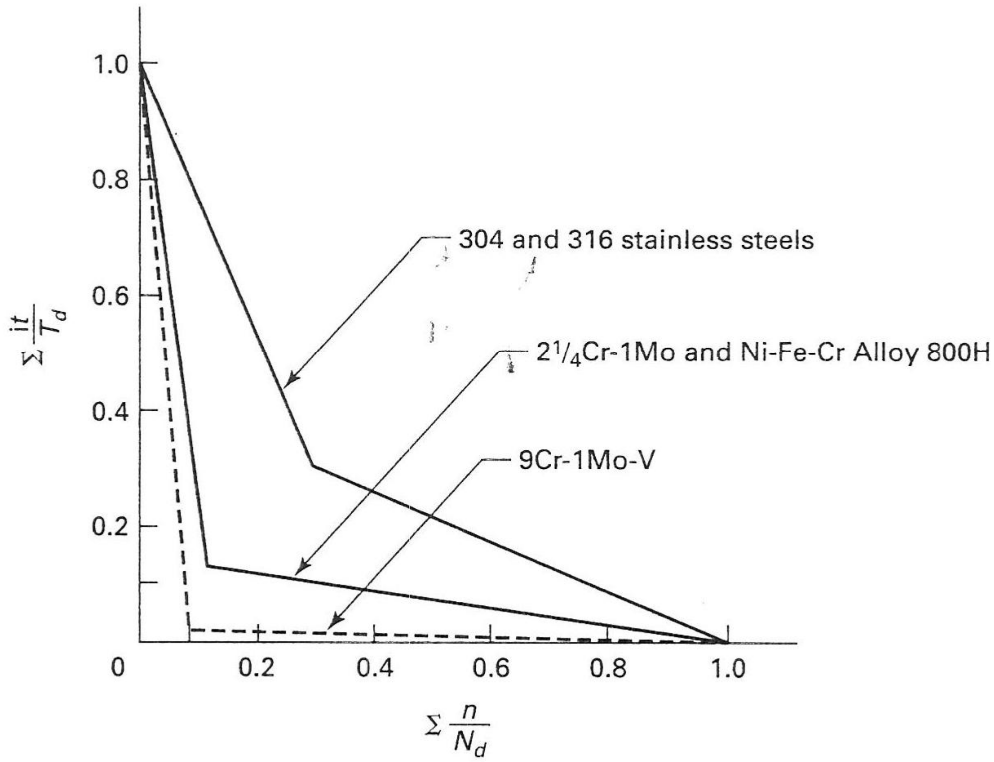
$$D = D_c + D_f \tag{9}$$

Qu.: Why do other procedures use non-linear interaction diagrams?

Pass!

In ASME the creep-fatigue procedure is given in the non-mandatory Appendix T of ASME III NH: “Strain Deformation & Fatigue Limits at Elevated Temperature”. The creep-fatigue interaction diagram is given in Fig.T-1420-2:-

FIG. T-1420-2 CREEP-FATIGUE DAMAGE ENVELOPE



The 9Cr curve here is for modified 9Cr (i.e., P91), not the forms of 9Cr we have in our AGRs (at least, I think so). The extreme severity of this P91 interaction diagram is presumably a reaction to the widespread plant problems with this material (a few years ago Fig.T-1420-2 did not include 9Cr).

But even for 304ss, 316ss, alloy 800 and 2.25Cr1Mo, the interaction diagrams are considerably more onerous than the linear summation used in R5.

Qu.: Why?

The methodology for calculating the fatigue and creep damage terms in ASME NH Appendix T, i.e., the axes of Fig.T-1420-2, is completely different from R5. The methodology is too complicated to summarise meaningfully here.

I used to believe that the reason for the more onerous interaction diagram was that the D_c and/or D_f terms would be smaller in ASME NH. But having just skimmed Appendix T, I’m not convinced that this is so, quite the contrary. It looks to me that the ASME-based (D_f, D_c) assessment point is likely to be *further* from the origin than the R5 point. So the interaction diagram makes ASME NH potentially far more onerous than R5. However, without working through an actual case in detail with both procedures I cannot pretend to be sure.