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CRACK GROWTH DETERMINATION ON LABORATORY COMPONENTS

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INTRODUCTION

In order to aid design and support remanent life assessment of plant components operating at elevated temperatures, the reliability of the analytical methods, which translate materials data procured from the laboratory to the behaviour of actual components, requires validation. Such a validation can of course be interpreted from operating plant, however the potential risks involved encourage the development of out of plant techniques for the validation of representative components. For meaningful validation, these techniques need careful control and high accuracy which can best be achieved in a laboratory environment. As the laboratory component test should be designed to simulate actual plant conditions as closely as possible, the direct extension of the results to the plant component case requires scaling up. Consequently the successful development of such a test may even lead to the advantageous situation where it could form an alternative to the conventional route where, for example, it may not be possible to obtain the plant component's metallurgical structure in a conventional specimen or, alternatively, when too many assumptions are required in the analysis when translating to different geometries and stress systems. Under these conditions, in spite of the more sophisticated test requirements, it may prove more reasonable to opt for the more representative laboratory component data for use in design or lifetime prediction.

The present work describes the application of the component validation test philosophy to the problem of crack growth under two rather different loading conditions. In both cases, crack growth is measured using the direct current PD technique on tubular metallic components containing artificial defects, however the plant conditions to be simulated lead to either creep or thermal fatigue. The creep studies on Alloy 800H support heat exchanger design for nuclear plant, solar towers and chemical plant, whereas the work on the ferritic 2¼Cr1Mo is principally related to steam raising power plant and petro-refinery applications. Under thermal fatigue conditions the stainless steel 316L of the first wall of the fusion reactor is specifically addressed. It is important to stress that once these techniques have been successfully demonstrated for simulating a particular plant component with one material, adaptation for some other simulation or material should be relatively straightforward.

TEST METHODOLOGY

Publications at earlier SMIRT conferences(1,2) and elsewhere(3,4,5) have given greater details of the actual techniques used to measure both creep and thermal fatigue crack growth in tubular components. In consequence, only the basic principles will be reiterated here along with information concerning improvements and more recent developments. All experiments were carried out on tubular testpieces, the Alloy 800H being machined from wrought tube stock, the 2¼Cr1Mo from thick wall steam pipe, and the 316L from rolled sheet. Each testpiece was furnished with either a spark eroded longitudinal defect or a mechanically machined fully circumferential groove. For Alloy 800H tubes only external defects were considered, while both external and internal defects were examined for the two steels. The full range of testpiece and defect dimensions for each material are given in Table 1.

Table 1. Notched component geometries and test conditions

Tubular Test Piece			Defect				Loading
Material	O.D.	Wall	Position	Orientation	Length	Depth	
Alloy 800H	24.5	2.5	External	Axial	15, 50, 200	0.4-1.7	Creep I.P.
Alloy 800H	24.5	2.5	External	Circumf.	------------	1.0	Creep I.P., Tension, Comb.
2¼Cr1Mo	33, 35	2.0, 3.0	External	Axial	5, 15, 50	0.9-1.1	Creep I.P.
2¼Cr1Mo	33	2.0	External	Circumf.	------------	1.0-1.6	Creep I.P., Tension, Comb.
2¼Cr1Mo	34, 35	2.5, 3.0	Internal	Axial	15	1.4, 1.8	Creep I.P.
2¼Cr1Mo	34, 34	2.5, 3.0	Internal	Circumf.	------------	0.9-1.4	Creep I.P., Tension
316 L	44	9.5	External	Axial	2, 4, 15	1,2,4	TF 80-350° C
316 L	44	9.5	External	Circumf.	------------	1.0-2.0	TF 80-350° C
316 L	44	9.5	Internal	Axial	15	1.0,2.0	TF 25-65° C
316 L	44	9.5	Internal	Circumf.	------------	1.0	TF 25-65° C

All testpieces were instrumented with DCPD for in situ, continuous measurement of crack growth. The detection of potential change was achieved by means of pairs of thin wire probes attached across the defect position (Figure 1) but always located on the external surface of the tube, even for internal defects.

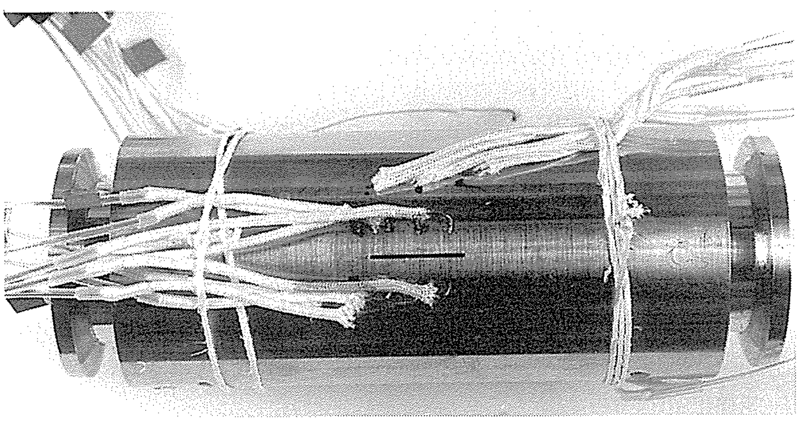

Figure 1. Instrumented longitudinally notched component.

The tubular creep crack growth testpieces were loaded by either the application of a tensile axial load, internal pressure or, in some cases, a combination of both and were conducted in the concrete cells of the Tube Testing Facility of JRC Petten. Test temperatures were selected to be consistent with both potential plant applications and the availability of conventional creep and crack growth data required for the validation analysis, Alloy 800H being tested at 800°C and the 2¼Cr1Mo steel at 550°C and 600°C. The PD output of these experiments was converted into crack growth evolution with the aid of calibration curves derived either from specially made tubes in which the defect was successively deepened during PD measurement or from PD measurements on a thin foil representative of the tube and defect geometries. In all cases these calibration curves were recently improved using data from interrupted tests.

A special rig was developed for the thermal fatigue tests which had the remit to provide the thermal gradients and cyclic conditions foreseen for a fusion reactor first wall face cycling between 80°C and 350°C. The tubular testpiece was heated using a high frequency induction coil and cooled internally with water. The thermal cycle was controlled by a thermocouple on the external surface which switched the induction heating on for around 5 seconds to reach 350°C and off for 21 seconds to 80°C. During the imposed cycle, the internal surface temperature cycled from 25°C to 65°C with the consequence that high tensile stresses were present at the inside wall during heating and at the outside during cooling. A finite element calculation showed similar axial and hoop stresses, with comparable maximum tensile stresses at the inside and outside, exceeding the yield stress of the material at the respective temperatures. Defects of various orientations and dimensions were machined into the internal and external surfaces as for the creep test-pieces, (Table 1). The evolution of crack growth was again determined from the DCPD changes recorded, using calibration curves originally derived from models, but later improved through the numerous measurements on fracture surfaces of failed and interrupted testpieces, as shown for a circumferential notch in Figure 2.

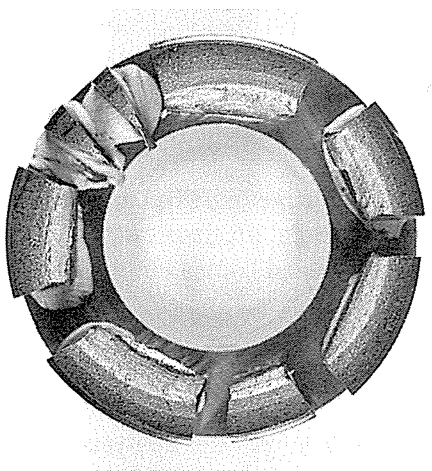

Figure 2. External circumferentially notched tube, sectioned and cracked open to reveal depth of thermal fatigue crack growth.

RESULTS AND DISCUSSION

The objectives of all types of experiment were twofold: firstly, to demonstrate that significant crack growth could be achieved on a laboratory component exposed to conditions close to those experienced in plant, and secondly, the benchmark validation of the predicted crack growth behaviour derived from an analytical method utilising conventional experimental data. With regard to the attainment of the first of these, Figure 3 is a composite of crack growth measurements from a wide spectrum of experiments. The thermal fatigue crack growth results are also given a time dependence for this purpose as the cycle length can be considered as constant. The thermal fatigue crack growth duly decreases as it grows into a lower stressed region whereas the creep crack growth accelerates as the remaining ligament length shortens.

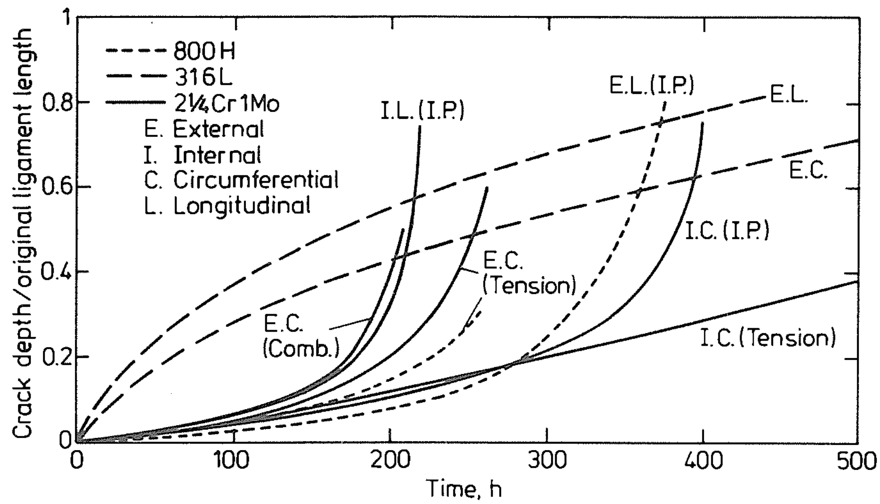

Figure 3.
Crack growth measurements on notched components.

It should be emphasised that such a presentation of the results is highly complimentary to the techniques employed. This is because all the changes in PD which are observed are construed as crack growth, whereas in reality, other factors can also play a role. In this respect, global and local ligament deformation are particularly important for relatively thin walled tubes and should receive attention in the subsequent analysis. As an example, it has been shown (5) that ligament deformation preceding creep crack growth in experiments on the ferritic steel has little influence on the predictive capabilities of the method, however global deformation caused by,for example, high internal pressures leads to global failure and not crack growth. In this case, no comparison can be drawn with conventional crack growth data but, nevertheless, the component test does accurately predict the way in which a defective structure would fail if it were ever subjected to such high transient system stresses.

Examination of the methods ability to meet the second objective must be carried out separately for the cases of creep and thermal fatigue in view of the different mechanisms and stress systems operating but, more importantly, due to the unrelated analytical routes employed.

a) Creep crack growth

In the analysis of the CCG behaviour for both Alloy 800H and 2¼Cr1Mo, the operative stress controlling the crack growth can be deduced either from finite element analysis or by using the reference stress concept. The former was not available and hence the simplified method had first to be assessed to establish that the approach indeed provided a relevant description of the creep behaviour of the defective components. The reference stress is derived from limit load solutions which are given in the literature (6-9) for the particular notched tube geometries. A number of different solutions have been used in the present work to cover the external/internal notch, cicumferential/longitudinal notch orientation, and whether axial tension or internal pressure loading is applied . For the special case of combined pressure and axial loading a summation of the separate contributions to the operative reference stress must be made. The suitability of these various reference stress calculations can be gleaned from Figure 4 where the rupture life of a sample of notched components tested is compared with baseline material creep data. Although not shown in Figure 4, conventional CCG data obtained through the COST 501 programme from compact tension (CT) specimens fabricated from the same material batch also fit the baseline well when expressed in terms of the appropriate reference stress.

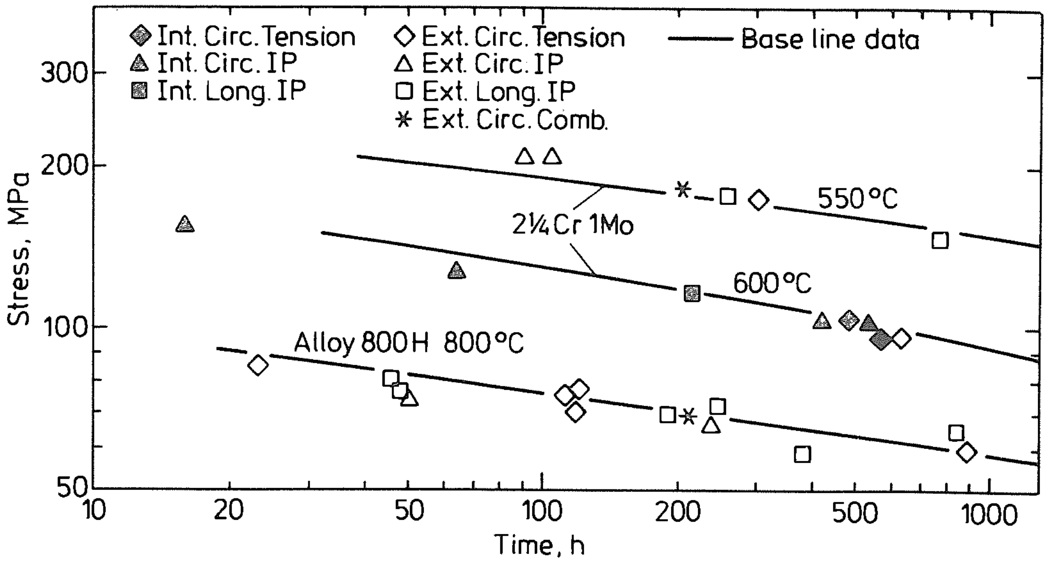

Figure 4. Reference stress rupture data for notched components.

The next step in the validation utilises these reference stresses to obtain a comparison of the actual CCG rates in components with those observed in compact tension specimens. It has been established (1) that the C^* integral is more appropriate than the stress intensity factor, K_I, for correlating CCG and hence a procedure developed by Ainsworth et al (10) has been adopted for the notched components. C^* is calculated during CCG from the relation

$$C^* = \dot{\epsilon}_{ref} K_I^2 / \sigma_{ref}$$

where the reference stress is derived from the limit load solutions, K_I for different geometries from the literature, and $\dot{\epsilon}_{ref}$, the minimum creep rate at the σ_{ref}, from the Norton creep law for the material in question. Examples of the agreements obtained are shown schematically in Figure 5 where the only poor correlations are either due to inaccuracy in the Norton law constants used

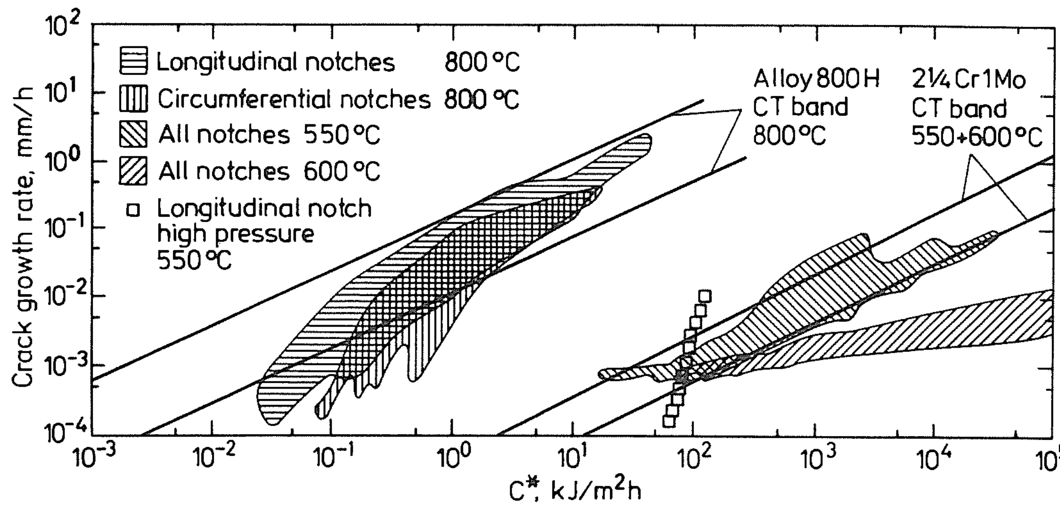

Figure 5. Comparison of CCG rate of notched components and CT specimens.

for the 2¼Cr1Mo tests at 600°C or failure by global rather than local means as shown, for example, for a high pressure test at 550°C. Although this methodology is rather sensitive to the basic creep data employed, and would consequently benefit from experimental advances which would facilitate a more direct derivation of C*, the method could be applied directly to plant components operating with defects.

b) Thermal fatigue crack growth

As the ultimate objective is to demonstrate that the component test can be used for validation purposes, the analysis must lead to a comparison between the thermal fatigue crack growth behaviour observed with the component and the mechanical crack growth behaviour observed in conventional specimens which would normally be used for design. Mechanical fatigue crack growth is usually represented by the Paris Law and the results from compact tension tests can be easily displayed in this format. Determining the appropriate stress intensity factors controlling crack growth under thermal cycling is much more of a challenge. For this crack growth analysis, finite element methods were used but limited to defining the stress field across the un-notched tube wall. The elastic-plastic analysis leads to hoop and axial stress distributions which can be subsequently adjusted to take cyclic plastic strains into account. Inclusion of the notches within the structure undoubtedly influences the stress distributions. For circumferential notches, the axial stresses were increased using the cyclic stress concentration equation given by Skelton(11). In the case of the longitudinal notch, a single FE calculation was made for a notched tube and the stress enhancement observed added to the stresses already determined for the plain tube. The resultant stress profiles across the wall for axial and hoop stresses at both maximum and minimum temperatures of the cycle were fitted with a polynomial function and the equivalent stress intensity factors calculated for these using the superposition method described by Buchalet and Bamford (12). The circumferential cracks are adjudged to grow from a free surface and to be infinitely long for the corresponding superposition coefficients. The longitudinal cracks also growing from a free surface are neither infinitely long nor do they maintain their shape throughout growth. They evolve from a rectangular starter notch into a semi-elliptical profile and this must also be taken into account in selecting the superposition coefficients. Values of ΔK, which are simply the differences between the stress intensities at maximum and minimum stress can thus be derived for all experiments. Examples showing the results from five tests with different starter notches are compared with data from compact tension specimens in Figure 6.

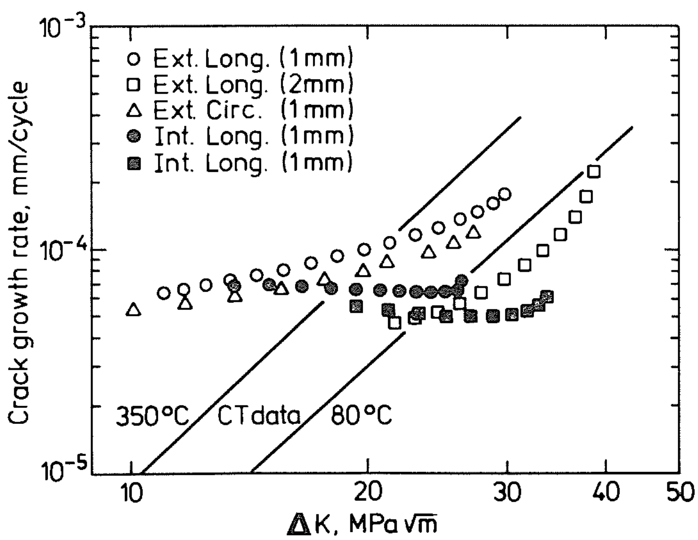

Figure 6. Comparison of thermal fatigue crack growth rate between notched components and compact tension specimens.

The full crack growth results have been reduced by removing the initial stages of crack growth, which take place in a highly plastically strained region where the use of ΔK is not justified and also the final stages, where $a/t > 0.6$ and the superposition method is not recommended. Interpretation of the component test results is rather complex, in particular as the stress intensity factor actually decreases as the crack grows. Nevertheless the results fall very close to the band delineating the properties of the material under mechanical fatigue. The exact simulation of the fusion reactor first wall temperature profiles has led to very similar stress distributions and corresponding stress intensity factors, even for quite different notch geometries. This leads to limited variation in crack growth rate (around 10^{-4} per cycle) and hence some difficulty in elucidating the influence of temperature, notch position or geometry. For the designer,however, the component test does show that cracks will grow from all types of defect under these particular cyclic conditions and at high rates which would be expected of this material in this temperature and stress range.

CONCLUSIONS

A demonstration has been given of the success of techniques developed for the measurement of crack growth on tubular components in the laboratory under both creep and thermal fatigue conditions. Analytical approaches have been employed to confirm that these techniques can be used either as benchmarks to validate conventional design and remanent life assessment methods or as independent tests which can be directly related to plant component performance.

REFERENCES

1. Hunter C.P. and Hurst R.C., 1989, SMIRT 10, pp137-145
2. Kerr D.C.,Andritsos F. and Hurst R.C., 1991, SMIRT 11, pp 265-270
3. Hunter C.P.,Hurst R.C. and Taplin D.M.R. 1992 Materials at High Temp.,V.10,No.2, pp144-149
4. Hunter C.P.,Church J.M.,Hurst R.C. and Taplin D.M.R., 1990, Creep and Fracture of Eng. Mat. and Structures, Eds. B.Wilshire and R.Evans, Inst. of Metals, London, pp 739-752
5. Church J.M. and Hurst R.C.,1992, Behaviour of defects at high temperatures, ESIS, Sheffield, In press.
6. Ainsworth R.A. and Coleman M.C., 1987, Fat. Fract. Eng. Mat. Struct. V.10, No.2, p129
7. Jones M.R. and Eshelby J.M.,1990, Limit solutions for circumferentially cracked cylinders under internal pressure and combined tension and bending, Nucl. Elect. TD/SID/REP/0032.
8. Kumar V.,German M.D. and Shih C.1981 An engineering approach for elastic plastic fracture analysis, EPRI NP 1931 Palo Alto, USA.
9. Miller A.G. Review of the limit load solutions of structures containing defects, CEGB Report TPRD/B/0093/N82
10. Ainsworth R.A. et al, 1987,Fatigue Fract. Eng. Mat. Struct. V. 10. No.2, pp 115-127
11. Skelton R.P., 1983, Fatigue at High Temperatures, App. Sci. Publ. London, pp 1-62
12. Buchalet C.B. and Bamford W.H. 1976, Mech. of Crack Growth,ASTM STP590,pp384-402

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