CREEP-FATIGUE PROPERTIES OF GRADE 91 STEEL

Rami Pohja, Asta Nurmela, Pekka Moilanen
VTT Technical Research Centre of Finland, Espoo, Finland.

Stefan Holmström
Joint Research Centre – Institute for Energy and Transport (IET), Petten, The Netherlands.

ABSTRACT

The creep-fatigue properties of modified 9Cr-1Mo (grade 91) steel have been investigated for the purpose of design in cyclic service. In this paper test results from creep-fatigue (CF) and low cycle fatigue (LCF) on grade 91 steel are reported. The tests performed on the high precision pneumatic loading system (HIPS) are in the temperature range of 550-600ºC, total strain range of 0.7-0.9% and with hold periods in both tension and compression. Curves of cyclic softening and stress relaxation are presented. The CF test results and results obtained from literature are also analysed using methods described in the assessment and design codes of RCC-MRx, R5 and ASME NH as well as by the recently developed Φ-model. It is shown that the number of cycles to failure for CF data can be accurately predicted by the simple Φ-model. The practicality in using the life fraction rule for presenting the combined damage is discussed and recommendations for alternative approaches are made.

KEYWORDS

Grade 91 steel, creep-fatigue, life fraction rule, modelling.

INTRODUCTION

The modified 9Cr–1Mo steel (grade 91) is widely used as structural material at elevated temperatures in applications of energy industry, such as fossil fired power plants. The grade 91 steel has a good combination of mechanical properties, high thermal conductivity, low thermal expansion coefficient and good resistance to stress corrosion cracking in water–steam systems compared to austenitic stainless steels. The 9-12% Cr steels, such as grade 91, are also considered to be creep resistant and they have found increasing application in thick-section components like piping, large forgings and castings of steam power plants.

However, the power plant components are often subjected to cyclic thermal and mechanical stresses as a result of temperature gradients and changes in pressures that occur during start-ups and shut-downs. During the steady state operation of a power plant creep deformation may occur under high temperature and pressure. Failure mechanisms under such loading conditions are complex interactions of creep and fatigue damages. Thus, the evaluation and assessment of the creep-fatigue interaction is an important issue for the design, maintenance and life assessment purposes for power plant components.

Although the creep, fatigue and creep-fatigue properties of grade 91 steel has been widely studied, the published data seldom includes all the important details, such as complete relaxation data, needed for the extensive creep-fatigue assessment. One main objective of the experimental part of this study was to provide such information for the assessment and modelling.

EXPERIMENTAL DETAILS

Tested material

The test specimens were manufactured from a plate of grade 91 steel with a thickness of 30 mm. The chemical composition of the material employed in this study is presented in Table 1. The material was austenized at 1050°C for 30 minutes followed by a quenching and tempering treatment at 780ºC for 1 hour.

Table 1. The chemical composition of grade 91 steel employed in the study.

Element	C	Mn	P	S	Si	Cu	Ni	Cr	Mo	Al
mass %	0.086	0.363	0.017	0.001	0.324	0.068	0.149	8.910	0.917	0.018

Testing equipment

The creep-fatigue testing was conducted using high precision pneumatic loading system (HIPS). Although the tests were conducted at air atmosphere, the pneumatic servo-controlled loading system is capable of operating in a range of extreme conditions such as at high temperature, pressurized water or steam, supercritical water (SCW) and irradiation environments. The main benefit of the pneumatic loading system is that there is no moving parts (loading lead-throughs) required for loading a specimen inside a pressure vessel or otherwise demanding containment (water, gas, radioactive), only pressure lines and electrical feedback connections together with the pneumatic loading unit, bellows, are required. [1, 2] More detailed description of high precision pneumatic loading system and its capabilities is given in reference 1 and 2. A schematic illustration of HIPS creep-fatigue testing equipment is presented in Fig. 1.

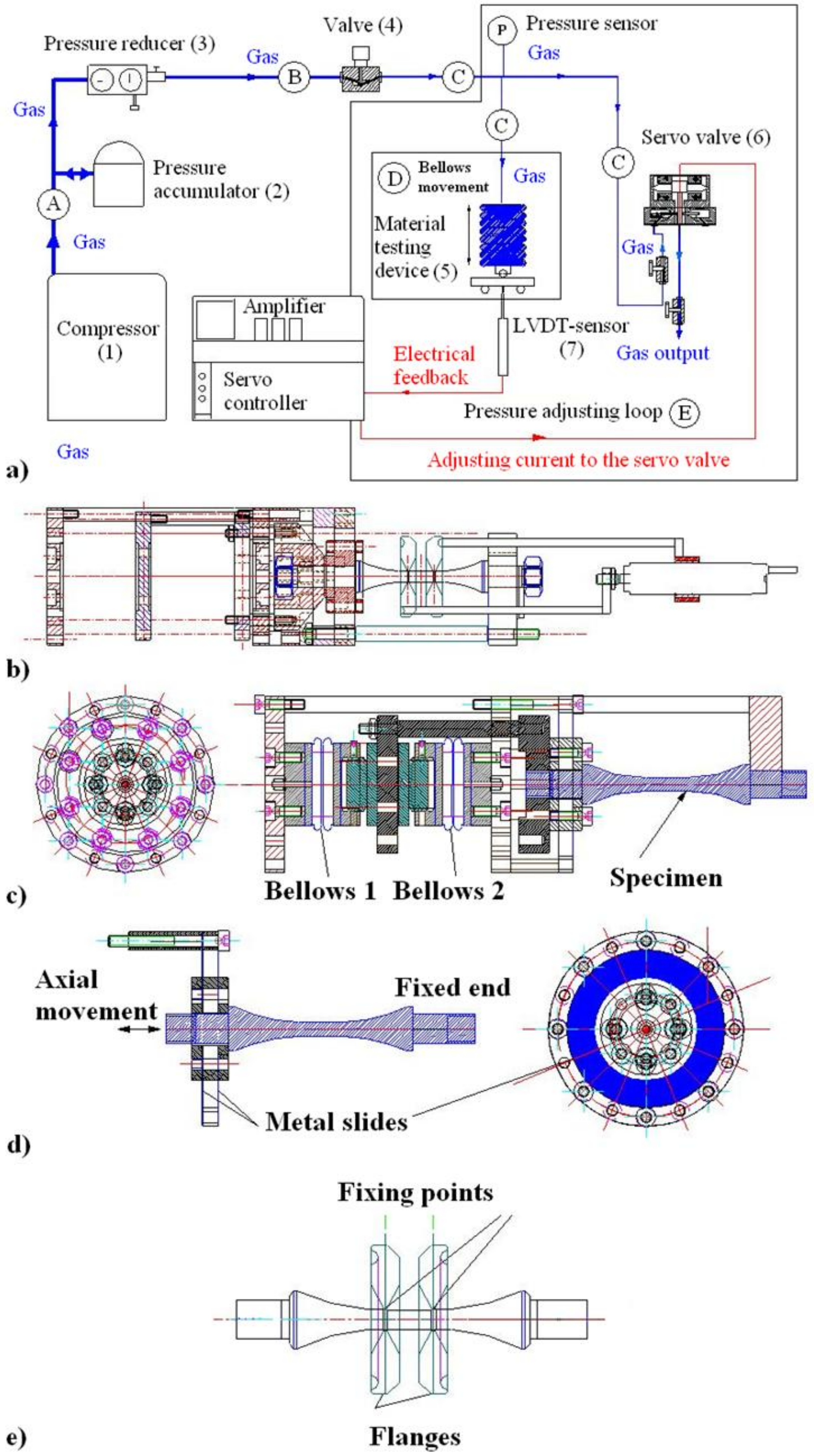

Figure 1. a) The main components of the HIPS system, b) the main parts of the displacement measurement system, c) the loading frame, d) the specimen fixing parts and e) the LVDT fixing flanges.

Test parameters

Creep-fatigue tests with fully-reversed (R=-1) total axial strain control with and without hold periods were conducted in air atmosphere. The temperature range for testing was between 550 and 600°C. The strain rate was 0.1% / sec and the total strain range was 0.7% of the gauge length. The length of hold periods was 10 minutes. The strain waveform for creep-fatigue testing without hold periods and with hold periods are shown in Fig. 2.

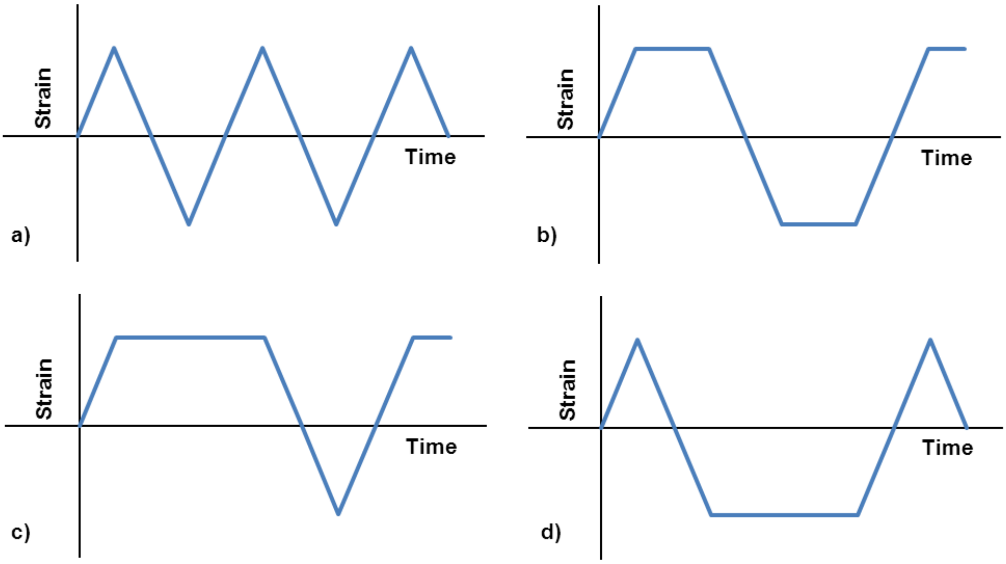

Figure 2. a) The strain waveforms for creep-fatigue testing without hold periods, b) with hold periods in tension and compression, c) with hold period in tension and d) with hold period in compression.

RESULTS

Creep-fatigue test results

In this study the number of cycles to failure used in the assessment is defined as the number of cycles to 10% load drop. Peak stresses as a function of cycles for specimens tested at 550-600ºC are shown in Fig. 3. As expected, the peak stresses and number of cycles to failure increased when the temperature was decreased from 600°C to 550°C. All specimens exhibited continuous cyclic softening both at 550°C and at 600ºC during the creep-fatigue testing. The cyclic softening was larger for the specimens tested at 600ºC and a major part of the cyclic softening occurred during the first 200 cycles for all tested specimens. Introducing a hold period of 10 minutes in tension or in compression or both slightly increased the cyclic softening. Furthermore, the hold period in compression was found to be more detrimental than hold period in tension which is in accordance with the results found in literature [3]. Thus, the number of cycles to failure was larger for the specimens tested with hold period in tension than for the specimens tested with hold period in compression.

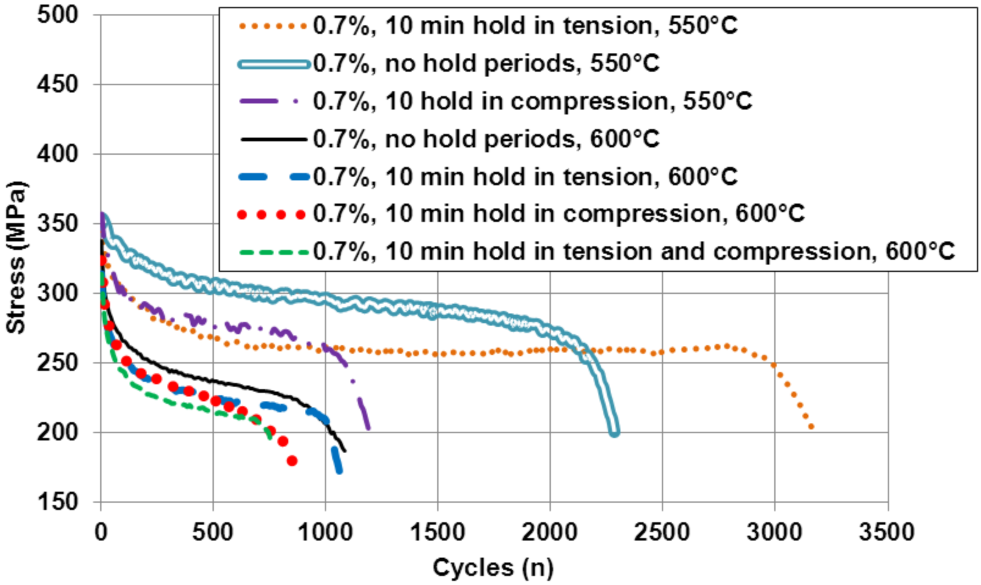

Figure 3. Peak stresses as a function of cycles for tested grade 91 steel specimens.

Life fraction rule assessment

The linear damage rule or life fraction rule has been used extensively in the evaluation of creep-fatigue interaction. It is based on the simple assumption that fatigue damage can be expressed as summed cycle fractions and that creep damage can be expressed as summed time fractions. It is also assumed that these quantities can be added linearly to represent damage accumulation. Failure should occur when this summation reaches a certain value, so that:

$$ {}_j\left(\frac{t}{t_r}\right)_j + {}_k\left(\frac{n}{N_f}\right)_k \leq D \qquad (1)$$

Where n is the number of cycles of exposure at a given strain range, N_f is the cycles to failure at the same strain range, t is the time of exposure at the same stress-temperature combination and t_r is the time to rupture at the same stress-temperature combination. In some cases, D is assumed to be unity, because cycle ratio summation should be unity when no creep damage is present. [4] In nuclear codes such as RCC-MRx, the interaction envelope whose intersection point is (0.3, 0.3) is used for grade 91 steel for design purposes. This means that from design point of view the combined fatigue and creep damage that a component experiences should not at any circumstances exceed the (0.3, 0.3) line, otherwise a failure may occur. Furthermore, in nuclear codes the fatigue and creep damage are calculated with safety margins for design purposes. [5]

The creep-fatigue test results with hold periods were evaluated with the life fraction rule method as shown in Fig. 4. The creep component (*dc*) for one cycle was obtained by using the creep strain rate rules and minimum creep rupture stresses without safety margins given in the RCC-MRx code. The total creep damage *(Dc)* was obtained by multiplying the *dc* obtained from the $N_f/2$ cycle with the total number of cycles to failure. This approach (calculating the total creep damage from $N_f/2$ cycle) is similar to the method described in the RCC-MRx. In ASME NH, the creep component for one cycle is calculated from the 1st cycle of the test. The fatigue component was obtained by comparing the total number of cycles to failure of specimens tested to the fatigue limits given in RCC-MRx code. No creep damage is assumed to be present in tests performed without hold periods. Thus, the tests without hold periods were not assessed with life fraction rule method.

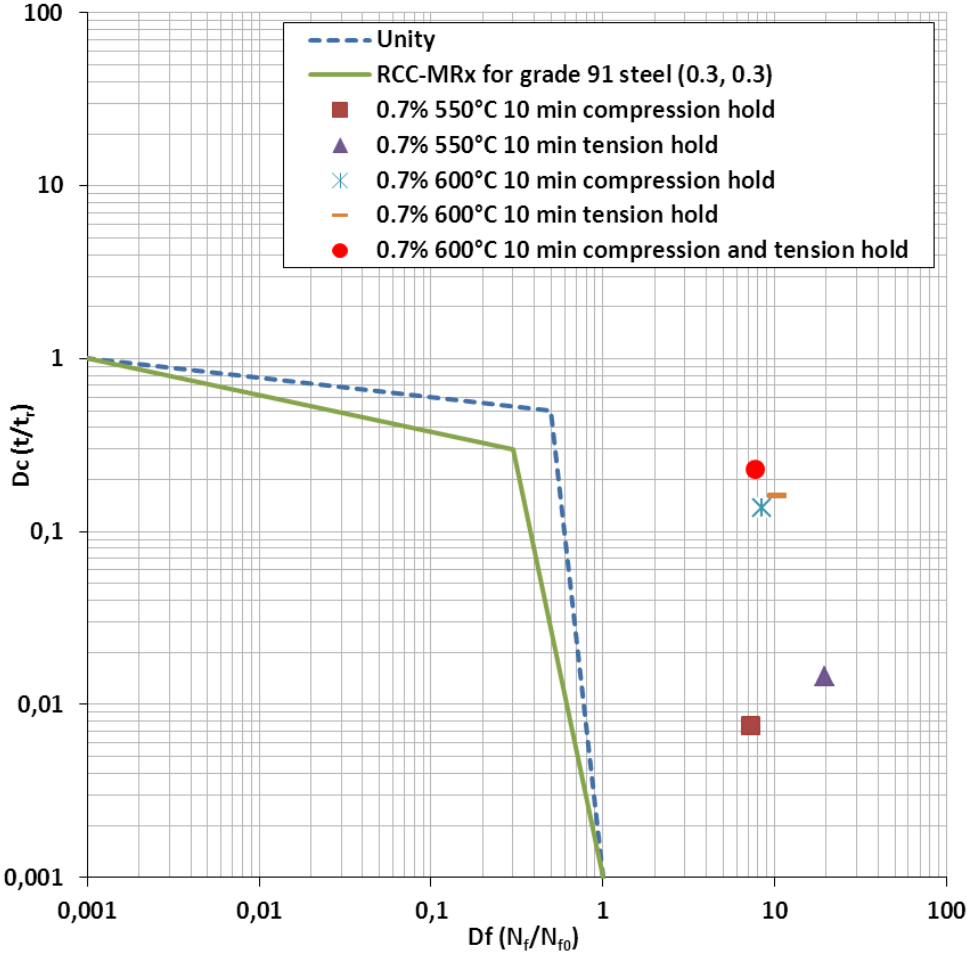

Figure 4. Life fraction rule plot with test results.

Relaxation during the hold period of a creep-fatigue test has a strong impact on the creep component in the life fraction rule assessment. Overestimation of relaxation decreases the creep component in the life fraction rule plot. Furthermore, the ASME code procedure, where the creep component for one cycle is calculated from the 1st cycle of the test, tends to overestimate the stress level used for creep component calculation. Especially with materials, such as grade 91 steel, which exhibit continuous cyclic softening the impact to *dc* can be considerable. In the RCC-MRx procedure the creep component is calculated from the half life cycle of the test. The measured and predicted relaxation curves using the Feltham equations [6] and RCC-MRx procedure of the half life cycle of the test with 0.7% strain and 10 minutes hold period in tension at 600ºC are presented in Fig. 5. The Feltham equations predicted only slightly larger relaxation than the measured values, whereas the RCC-MRx procedure predicted significantly larger relaxation as shown in Fig. 5.

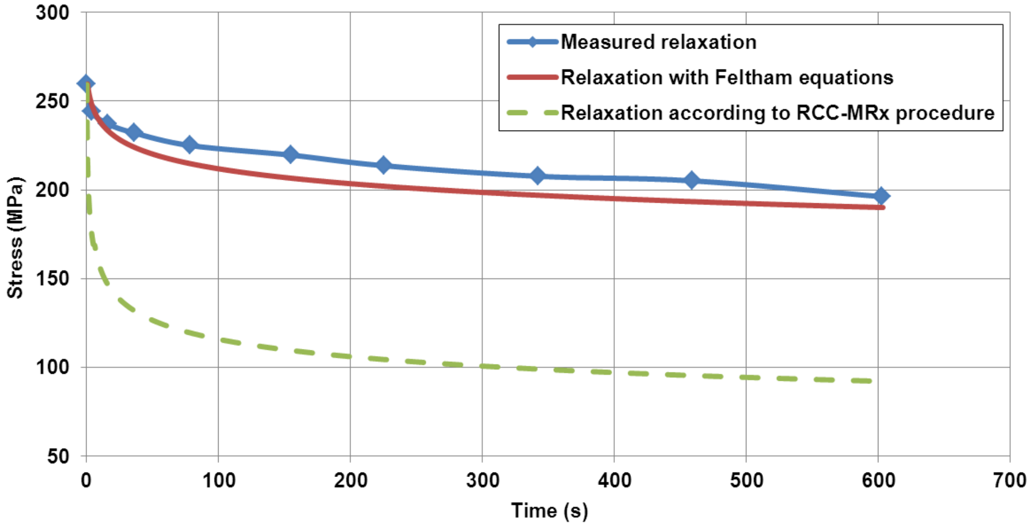

Figure 5. The measured and predicted relaxation curves using the Feltham equations and RCC-MRx procedure of the half life cycle of the test with 0.7% strain and 10 minutes hold period in tension at 600ºC

The Φ-model assessment

The creep-fatigue test results and results obtained from literature were assessed with recently developed Φ-model. The model predicts the expected life under tensile-compressive loading cycles with or without hold periods. The effective creep-fatigue lifetime (t_{CF}) and corresponding number of cycles to failure (N_{CF}) are predicted utilizing the creep rupture properties and the ultimate tensile strength of the material [7]. When using a creep model for assessing creep-fatigue data, the selected model should have good predictive capability both in short and long term range, which is not usually the case for models optimized for best performance for long term predicted life. If the creep rupture model of choice is that of Wilshire, then the measured value of the normalized reference stress Φ_m for each CF data point can be calculated from:

$$\Phi_m = \exp\{-k[t_{CF} \cdot \exp(-\frac{Q}{R \cdot T})]^u\} \tag{2}$$

Where k and u are material constants from Wilshire model, Q is the apparent activation energy, T is temperature and R is the gas constant and t_{CF} is the sum of hold times, i.e. $t_h \cdot N_f$ [8]. Now it is possible to describe the normalized reference stress as a function of strain range, hold time and temperature by multi-linear regression to the equation:

$$\Phi_{CF} = c_1 + \frac{c_2}{\Delta\varepsilon} + c_3 \cdot \log\ t_h\ + c_4 \cdot T \tag{3}$$

By combining Eq. (2 - 3) the predicted t_{CF} can be calculated as:

$$t_{CF} = -(\frac{\ln(\Phi_{CF})}{k})^{\frac{1}{u}} \cdot e^{(\frac{Q}{R \cdot T})} \tag{4}$$

And the predicted number of cycles to end criterion is:

$$N_{CF} = \frac{t_{CF}}{t_h} \tag{5}$$

A comparison of the predicted versus measured creep-fatigue life of the test specimens and public domain data up to 200 000 cycles in terms of total number of cycles to end criterion in logarithmic scale is shown in Fig. 6. For the data assessed the Φ-model was able to predict the creep-fatigue life within a scatter factor of 2.88 with 99% confidence.

The scatter factor Z [9] is defined as:

$$Z = 10^{2.5*\sqrt{\frac{\Sigma(\log N_{pred} - \log(N_{meas}))^2}{n-1}}} \tag{6}$$

Where N_{pred} is the model prediction, N_{meas} the measured number of cycles and n the number of test data points.

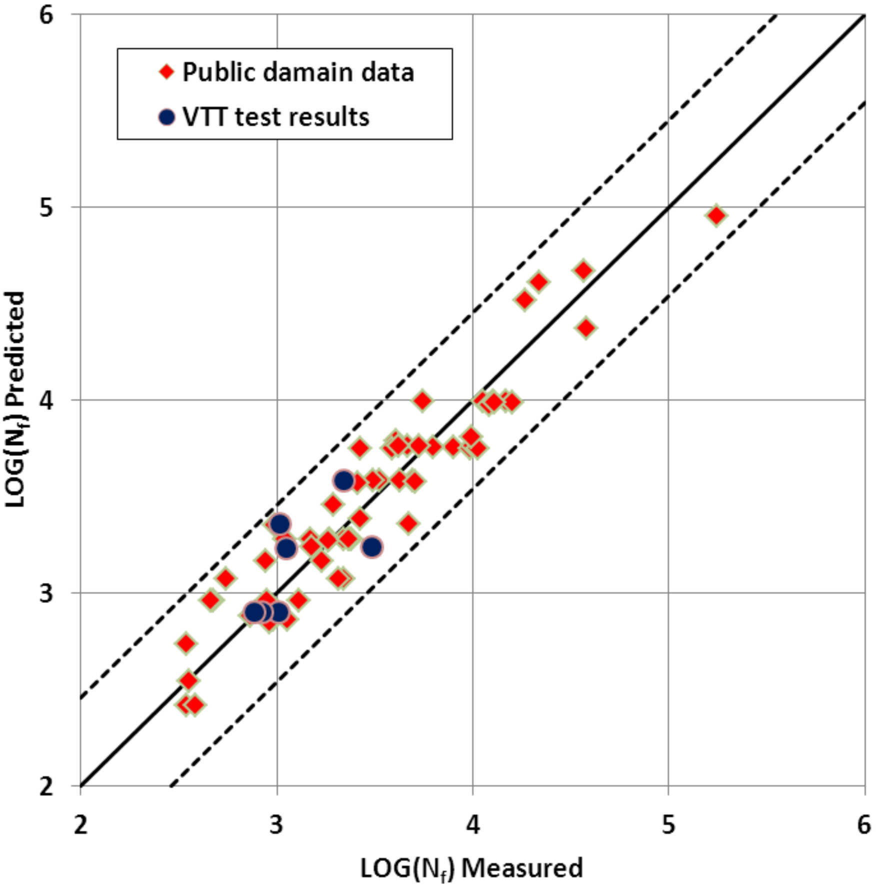

Figure 6. Predicted versus measured creep-fatigue life in terms of cycles to failure in logarithmic scale modelled with Φ-model [3, 10, 11, 12, 13].

DISCUSSION

The test results demonstrated that decreasing the testing temperature increases the peak stress levels and the number of cycles to failure. Furthermore, the test results demonstrated that cyclic softening is accelerated during the first 200 cycles if the testing temperature is increased from 550°C to 600°C. Introducing a hold period of 10 minutes in tension or in compression or both slightly increased the cyclic softening. In the near future, tests with 0.9% total strain range in air atmosphere will be carried out for grade 91 specimens.

Although the life fraction rule is widely used in creep-fatigue damage assessment, there are issues to be considered concerning the practicality of the method. Safety margins included in the nuclear codes such as ASME III NH and RCC-MRx may lead to very conservative results where single test plots are a thousand times above the unity line in the diagram when assessing creep-fatigue test results. In some cases the evaluation is not even possible, if the determined stress levels exceed the limits given in the codes. When hold periods are included the determination of the relaxed stress level, which is not straightforward in some cases, has a strong impact on the creep component. The ASME procedure, where the creep component for one cycle is calculated from the 1st cycle of the test, tends to overestimate the stress level used for creep component calculation. Especially with materials such as P91 which exhibit continuous cyclic softening the impact to *dc* can be considerable. When used for design purposes, the conservatism of the ASME and RCC-MR life fraction procedure may lead to solutions, which are not economically reasonable.

A significant advantage of the Φ-model to predict creep-fatigue life is that at least for a given isothermal test type, it is not necessary to consider further details of individual creep-fatigue cycles, such as features of relaxation, peak stress, softening or hardening behavior. The extrapolation in hold time may be possible in a similar way as for creep (longest time x 3), but this has still to be validated with culled data sets or by data with longer hold times. Assuming that the Wilshire creep rupture model is used, and the temperature dependence of ultimate tensile strength (UTS) is described by a 3rd degree polynomial, the total number of fitting constants is four for UTS + three for the Wilshire model + four for Φ-model equaling 11 constants. This is a very small number considering that the constants for UTS and Wilshire creep model can be determined separately from relatively simple standard tests. The number of constants may be further reduced if UTS has been tested for the same material batches and isotherms that have been used for creep testing.

The Φ-method has been shown in earlier work to predict well the observed creep-fatigue life of austenitic stainless steel 316FR and nickel alloy A230 subjected to isothermal strain controlled cycles with tensile hold periods [7, 14]. For grade 91 steel (VTT and public domain data) the maximum expected error in prediction is within a scatter factor of 2.88 with the 99% confidence limit. It is a future objective to find adjustment factors for the Φ-model to also predict cross-weld specimen tests in LCF and CF.

The Φ-method also allows a simple definition of creep-fatigue damage $D_{CF} = N/N_{CF} = \Sigma t_h/t_{CF}$ where N is the consumed amount of cycles and Σt_h is the corresponding time in hold. For this damage parameter there is no need to separate creep and fatigue damage or life fractions. The Φ-method hence allows for more straightforward damage assessment for both design and later life assessment than the common methods using summed life (or strain) fractions. It is expected that the approach is applicable for many creep-fatigue cases in power generation, where strain rates and cycling frequencies are low and even lower (more creep dominated) than in creep-fatigue testing in the laboratory.

CONCLUSIONS

Creep-fatigue properties of grade 91 steel were investigated and creep-fatigue interaction was evaluated and compared with the linear life fraction rule and the Φ-model. The following conclusions were drawn from the test results and evaluation:

(1) The specimens tested at 600ºC showed larger cyclic softening during the first 200 cycles than the specimens tested at 550ºC.
(2) Introducing a hold period of 10 minutes in tension or in compression or both slightly increased the cyclic softening.
(3) The hold period in compression was found to be more detrimental than hold period in tension.
(4) All experimental combinations of creep and fatigue damage resided above the interaction envelope for grade 91 steel provided in RCC-MRx and even above the unity line in life fraction rule plot due to the large total fatigue component. The total creep component remained low because the RCC-MRx evaluation method overestimates the relaxation and the total fatigue component was high because of the strict fatigue limits in the RCC-MRx.
(5) The Φ-model predicted the creep-fatigue life within a scatter factor of 2.88 at 99% confidence for the grade 91 steel specimens.

ACKNOWLEDGEMENTS

The authors wish to acknowledge the support from the MATTER project (Project no: 269706 – FP7-Fission-2010).

REFERENCES

[1] Moilanen, P., Pneumatic servo-controlled material testing device capable of operating at high temperature water and irradiation conditions, a doctoral dissertation, VTT Publications: 532 (Espoo 2004), pp. 70-130.
[2] Pohja, R. *et al*, “Multifunctional High Precision Pneumatic Loading System (HIPS) for Creep Fatigue Testing”, Procedia Engineering, Vol. 55, No. 1 (2013), pp. 573-577.
[3] B. Fournier. *et al*, “Creep-fatigue-oxidation interactions in a 9Cr–1Mo martensitic steel. Part II: Effect of compressive holding period on fatigue lifetime”, International Journal of Fatigue, Vol. 30, No. 4 (2008), pp. 663–676.
[4] Conway, J., ASM Handbook, Vol. 8, Mechanical Testing, ASM International, (USA 1992), pp. 346-359.
[5] Riou, B., Improvement of ASME NH for grade 91 negligible creep and creep fatigue, ASME Standards Technology, (New York 2008), pp. 4-98.
[6] Feltham, P., “Stress Relaxation in Copper and Alpha-Brasses at Low Temperatures”, Journal of the Japan Institute of Metals, Vol. 89, No. 1 (1961), pp. 210–214.
[7] Holmström, S. and Auerkari, P. “A robust model for creep-fatigue life assessment”, Materials Science & Engineering A, Vol. 559, No. 1, (2013) pp. 333-335.
[8] Wilshire, B. *et al*, “A new approach to creep data assessment”, Materials Science and Engineering A, Vol. 511, No. 1, (2009) pp. 3-6.
[9] Holdsworth, S. *et al*, ECCC Recommendations, Vol. 5, Creep data validation and assessment procedures, ECCC publications, (2005), pp. 5-10.
[10] Takahashi, Y., “Creep-fatigue behavior of various alloys used in power industry and their predictability”, Transactions of The Indian Institute of Metals, Vol. 63, No. 2, pp. 251-257.

[11] Nagesha, A. *et al*, “Influence of temperature on the low cycle fatigue behaviour of a modified 9Cr–1Mo ferritic steel”, International Journal of Fatigue, Vol. 24, No. 11 (2002), pp. 1285–1293.

[12] Shankar, V. *et al,* “Low cycle fatigue behavior and microstructural evolution of modified 9Cr–1Mo ferritic steel”, Materials Science and Engineering A, Vol. 437, No. 1 (2006), pp. 413–422.

[13] Fournier, B. *et al,* “Comparison of various 9–12%Cr steels under fatigue and creep-fatigue loadings at high temperature”, Materials Science and Engineering A, Vol. 528, No. 22 (2011), pp. 6934–6945.

[14] Holmström, S. *et al,* “Creep and Creep-fatigue Behaviour of 316 Stainless Steel”, Procedia Engineering, Vol. 55, No. 1 (2013), pp. 160-164.