Creep-cyclic deformation in high temperature power plant structural materials: Informing assessment procedures in industry via multi-scale physical modelling

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ABSTRACT
A micromechanical model for dislocation-obstacle interactions at the microscale is implemented in a crystal plasticity multi-scale polycrystalline self-consistent model (SCM), developed by (Hu and Cocks, 2016) and (Petkov, Hu and Cocks, 2018), and employed to simulate deformation scenarios common to those in thermal power plants, such as the UK operated Advanced Gas Cooled Nuclear Reactor (AGR) power plants. The major aim of the article is to compare SCM predictions to typical industrial practice using the R5 assessment procedures and to examine potential aspects of the procedures which can be informed or improved via the more physically-rich SCM. Considering the damage assessment methods within R5, which are based on a ductility-exhaustion approach, plant-relevant cyclic histories with relaxation and constant-stress creep dwells are examined in this article with emphasis on estimation of accumulated creep strain to quantify the impact of a better understanding of creep-cyclic deformation interaction.

INTRODUCTION
For any high temperature power plant, such as the Advanced Gas-Cooled Nuclear Reactors (AGRs) operated by EDF Energy in the UK, high temperature Gen IV designs or conventional plant, creep-fatigue is likely to be a life-limiting degradation mechanism for some components. Extending the safe operational lifetime of such components provides a strong motivation to ensure the creep-cyclic deformation response of components is being accurately predicted as it is a fundamental input into any creep-fatigue damage assessment. Austenitic Type 316H stainless steel is a material often used for components operating at high temperature, including components in the AGR power plant. Such components typically operate in the creep regime and are often exposed to complex thermo-mechanical loading histories, including primary and secondary loads. This can, in some components give rise to local regions of cyclic plasticity, resulting in significant creep-fatigue interaction which must be accounted for in lifetime predictions. Therefore the mode of failure is local creep-fatigue damage, causing crack nucleation and subsequent growth. This differs from pure creep rupture failure which is a gross failure mechanism. In the case the AGRs, as they provide baseload generation capacity, it is typical that such creep-fatigue damage is creep dominated.

EDF Energy use the R5 lifetime assessment procedure (Assessment Procedure for the High Temperature Responses of Structures, R5, 2014) to assess such components. Unlike many other design codes or fitness-for-service codes, it deploys a ductility exhaustion creep damage model, as it has been demonstrated to provide better lifetime predictions for the materials and cycle types in AGRs (Spindler and Payten, 2011). The use of ductility exhaustion damage models requires accurate assessment of the accumulated inelastic strain during typical cyclic-creep deformation histories which poses the requirement for more accurate constitutive models for inelastic deformation. Due to limitations in the materials models available to industry, the industrial user of R5 will often make a series of conservative assumptions regarding materials behaviour to ensure the predicted lifetime remains conservative. These simplifying
assumptions potentially introduce excessive conservatism in the resultant lifetime predictions as they do not fully account for some of the following observations.

The relaxation of stress within creep dwells during cyclic loading results in a greater inelastic strain accumulated (Cronin and Spindler, 2016), but also in a decrease of the saturated stress in the hysteresis loop compared to cycling without a dwell. The effect of the dwell position within the cycle (e.g. dwell at 0.0% strain), contrary to a dwell at maximum tensile strain which is commonly considered in experiments, is also of interest to plant operation; it has been hypothesised that a greater decrease in saturated stress is expected from a dwell at peak strain as compared to a mid-strain dwell (Cronin and Spindler, 2016). Faster saturation of the hysteresis loop is also observed experimentally with the presence of creep dwells. Furthermore, current material models are based on short-term experimental dwell data (<168 hours). This will potentially have implications in predicting damage development during cyclic-creep deformation in plant (approx. 1000-hour dwell) which requires significant extrapolation of laboratory data.

The major objective of this study is to provide a better understanding of the effects of creep-cyclic interaction for typical power plant components, as well as guidelines for experimental studies to confirm the predicted behaviour, via the application of a physically-based multi-scale model. In addition, some of the predictions via the physically-richer multi-scale modelling framework can be used to inform the more simplistic models employed in industry and perhaps can be employed directly in structural assessments. A more detailed investigation of the effects of cyclic loading on the subsequent creep response, as well as the effects of dwells on cyclic response during a variety of loading histories, can be found elsewhere (Petkov, Hu and Cocks, 2018) and (Petkov, Chevalier, Dean and Cocks, to appear). The following section briefly reviews the micromechanical self-consistent modelling (SCM) framework employed in the present study. The subsequent two sections focus on i) comparison of the SCM with typical simplified assessment assumptions on the effects of displacement-controlled dwells on hysteresis loop evolution, and ii) the evaluation of load-controlled dwells on inelastic strain accumulation during a thermo-mechanical history similar to that experienced in a plant boiler section component. Conclusive remarks on the major findings from this study can be found in the last section.

MULTI-SCALE SELF-CONSISTENT MODEL

Overview of modelling framework

Hu and co-workers (Hu and Cocks, 2015, 2016; Hu et al., 2016) have developed physical models of elastic-plastic deformation and creep of 316H stainless steel, capturing the evolution of obstacles to dislocation motion (e.g. forest dislocation network, precipitate morphology and concentration of solute elements in solid solution) and the stress state evolution in a polycrystalline aggregate within a self-consistent crystal plasticity framework. It has been demonstrated that the SCM accurately predicts mesoscopic lattice strains and macroscopic deformation under short- and long-term plasticity, cyclic deformation and creep of ex-service (EX) and solution treated (ST) Type 316H samples, e.g. (Hu et al., 2016; Hu and Cocks, 2017). A brief introduction to the model is given here; a full description can be found in (Hu, 2015) and (Petkov, Hu and Cocks, 2018).

The deformation of the anisotropic grains in the SCM is modelled using crystal plasticity (CP) theory (Hu and Cocks, 2016); the rate-dependent mechanistic formulation for shear strain rate of thermally-activated dislocation glide follows the model proposed in (Kocks, Argon and Ashby, 1975), which relates \( \dot{\gamma}^{(\alpha)} \) to the required free activation energy \( \Delta F_0 = \alpha_0 G_b^r, G_0 \): shear modulus at 0 K) to allow the system to reach a new plastically-deformed state at a temperature \( T \) according to

\[
\dot{\gamma}^{(\alpha)} = \dot{\gamma}_0 \exp \left( - \frac{\Delta F_0}{kT} \left( 1 - \frac{\tau^{(\alpha)} + (\overline{\tau}_m)^{\tau^r}}{\tau_{cr}^{(\alpha)}} \right)^{2/3} \right) \left( \frac{4}{3} \right) sgn(\tau^{(\alpha)})
\]

where \( k \) is Boltzmann constant, \( \overline{\tau}_m^{\tau^r} \) is the Type III (micro-) internal residual stress (Hu et al., 2016), \( \dot{\gamma}_0 \) the reference shear strain rate. The polycrystalline aggregate is modelled via a self-consistent scheme (Hu and
Cocks, 2016), similar to that proposed in (Kroner, 1961) and (Budiansky and Wu, 1962) (KBW model) by treating grains as anisotropic inclusions in an elastic matrix. The model allows the increment in residual intragranular stress, $\Delta X^a$, in a given grain to be obtained according to

$$\Delta X^a = L^i (S^i - I) [C^a L^i (1 - S^i) + S^i]^{-1} \Delta \epsilon^a$$  \hspace{1cm} (2)

where $S^i$ is the Eshelby tensor, $C^a$ is the anisotropic grain compliance matrix and $L^i$ - the stiffness of the isotropic matrix surrounding the grains. The incompatible plastic mismatch strain between the grain and the polycrystalline body, $\Delta \epsilon^a$ is obtained, following KBW, from

$$\Delta \epsilon^a = \Delta \epsilon_p - T - T \Delta E^p$$  \hspace{1cm} (3)

with $\Delta E^p$ is the macroscopic plastic strain increment in the polycrystal in the global co-ordinate system, $\Delta \epsilon^a$ is the plastic strain in a grain and $T$ is the rotation matrix for the grain of interest.

The micromechanical part of the model captures the contributions to the internal resistance $\tau_{cr}$ of the three dominant obstacles to dislocation motion, i.e. dislocation junctions with mean spacing $L_d$, intragranular precipitates with mean spacing $L_p$, and discrete solute atoms with mean spacing $L_s$,

$$\tau_{cr} = \left( \frac{\alpha_d G b}{L_d} \right)^2 + \left( \frac{\alpha_p G b}{L_p} \right)^2 + \alpha_s G b (c b)^{1/2}$$  \hspace{1cm} (4)

where $G$ is shear modulus, $b$ is the magnitude of Burger’s vector and $\alpha_p$, $\alpha_d$ and $\alpha_s$ are dimensionless obstacle strength parameters, see (Argon, 2008). Evolution of precipitate and discrete solute atoms morphology due to long-term aging is considered in the model (Hu, 2015; Hu and Cocks, 2015), however, the present study is limited to ex-service material where the evolution of second-phases and solute content is negligible, i.e. $L_s$ and $L_p$ are constant. $L_d$ is a function of the dislocation junction obstacle density evolution, $\Delta N_d = 1/\Delta L_d^{2}$, due to self- and latent-hardening as a result of dislocation multiplication related to the increment in the resolved shear strain $\Delta \gamma$ on a slip plane:

$$\Delta N_{self} = j \Delta \gamma$$  \hspace{1cm} (5)

$$\Delta N_{latent} = j_s \Delta \gamma$$  \hspace{1cm} (6)

where $j_s$ and $j$ are fitting parameters (Hu and Cocks, 2016).

Dynamic recovery is captured within the multi-scale framework through a model, applied at the slip system level, introduced by Kocks (Kocks, 1976) by which the recovery process is a function of the probability that a dislocation segment length ($\Delta L_r$) is annihilated as a function of accumulated obstacle density given an increment of the resolved inelastic shear strain $\Delta \gamma$

$$\Delta N_d = - \frac{\Delta L_r}{b} N_d \Delta \gamma$$  \hspace{1cm} (7)

Note that in the temperature range of interest in the present study cell formation is not explicitly modelled since the precipitates present in 316H retard cell formation (Petkov, Hu and Cocks, 2018). Hu and Cocks (Hu and Cocks, 2017) describe the thermal recovery of the dislocation forest assuming that at elevated temperatures long dislocation links grow at the expense of shorter ones. As a result, the increase in dislocation junction spacing due to static recovery can be expressed as

$$\dot{L}_{di} = \frac{W_c D_c G b^5}{kT} \frac{1}{L_d^{3}}$$  \hspace{1cm} (8)

where $D_c$ is the core diffusivity and $L_{di}$ is the dislocation junction obstacle mean spacing on the $i$th slip plane (Hu, 2015), and $W_c$ is the free parameter controlling static recovery of the dislocation structure.
Calibration of model

To calibrate the model, we employ the procedure described by (Petkov, Hu and Cocks, 2018) via which calibration is carried out using experimental data from 4 independent tests: i) uniaxial elastic-plastic loading; ii) a displacement-controlled cyclic loading test at room temperature; iii) a standard creep test at constant stress; and iv) a relaxation test, following monotonic loading up to the initial stress state. The model is calibrated to the response of ex-service (EX) austenitic Type 316H stainless steel (Cast 69431) at 550°C. Once calibrated to simplistic loading histories, the modelling framework is then employed to simulate more complex histories with the predictions being independent of any free parameters. A summary of fitting and physical parameters employed in the model is given in Table 1.

Table 1: Material parameters for multi-scale model, Type 316H (T_m = 1810 K).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Parameter</th>
<th>Value</th>
<th>Parameter</th>
<th>Value</th>
</tr>
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<tbody>
<tr>
<td>C_11</td>
<td>198 GPa*</td>
<td>N_0</td>
<td>1.5 x 10^14</td>
<td>b</td>
<td>2.5 x 10^-10 m</td>
</tr>
<tr>
<td>C_12</td>
<td>125 GPa*</td>
<td>j</td>
<td>2.0 x 10^15</td>
<td>γ_0</td>
<td>1.0 1/s</td>
</tr>
<tr>
<td>C_44</td>
<td>122 GPa*</td>
<td>j_s</td>
<td>1.0 x 10^-6</td>
<td>α_0</td>
<td>0.18</td>
</tr>
<tr>
<td>E</td>
<td>101.3 GPa</td>
<td>W_c</td>
<td>0.015</td>
<td>τ_p</td>
<td>21 MPa</td>
</tr>
<tr>
<td>υ</td>
<td>0.39</td>
<td>D_c</td>
<td>6.47 x 10^-15 m^2/s **</td>
<td>τ_s</td>
<td>37 MPa</td>
</tr>
<tr>
<td>G_0</td>
<td>139 GPa</td>
<td>Δ</td>
<td>8</td>
<td>α_d</td>
<td>0.35</td>
</tr>
</tbody>
</table>

* Data from (Hu, 2015); ** Parameters from (Ashby and Frost, 1982)

The solute concentration and mean spacing of precipitates were determined from a knowledge of the alloy composition and energy-dispersive X-ray spectroscopy (EDX) studies. The degree of latent hardening, represented by the ratio j/S, was set to 1.4, as discussed in (Petkov, Hu and Cocks, 2018). Once the hardening parameter j and initial value of L_d, which is related to the initial density of pinning points N_0, are determined from the fit to elastic-plastic monotonic response, the dynamic recovery controlling parameter is found from fitting the cyclic loading response to saturation for a given number of cycles. The creep and relaxation responses are determined by the parameters α_0 for thermally-activated glide and W_c for static recovery; N.B. α_0 is treated as a free parameter based on physical considerations, see (Petkov, Hu and Cocks, 2018). The fitted model response to creep, relaxation and cyclic deformation tests is shown in Figure 1.

Figure 1. SCM predictions and comparison to experimental data on a) creep, b) relaxation and c) cyclic.

INTRODUCTION TO SCM COMPARISON WITH INDUSTRIAL PRACTICE

Austenitic Type 316H stainless steel hardens during cyclic loading. In displacement-controlled cycles, the peak-hardened response is referred to as the saturated cyclic stress amplitude. R5 makes reference to the AGR Materials Data Handbook R66, which gives the Ramberg-Osgood parameters for the stress range-strain response in the stabilised cyclic state. The stabilised curve is typically used in the damage assessment procedures within R5. Creep dwells are common to thermo-mechanical deformation histories during operation of plant components and the R66 data handbook recognises some softening of the cyclic response due to the presence of such dwells and provides guidance on modifying the stress-strain response - the saturated cyclic stress amplitude is decreased in cases where the material is subject to creep dwells.
There is currently limited advice on the impact of cyclic plasticity on the creep response, with the conservative option to be assuming full re-priming of creep behaviour at the start of each creep dwell.

With the above assumptions, the hysteresis loop construction generally provides a conservative estimation of stress, creep strain accumulation and hence creep damage, and potentially a non-conservative estimate of the total strain range and therefore fatigue damage. This is a conservative combination in creep-dominated creep-fatigue cases (such as the AGRs), but may be non-conservative if fatigue were the dominant mechanism. It is also worth noting that current practice is informed by experimental studies, where the applied strain range (typically >0.6%) and length of the dwell (typically <24h), that does not fully represent dwells during plant operation, which typically have a lower strain range (<0.4%) and much longer dwell (>1000h). Hence, application of the procedures to plant scenarios at lower strain ranges and longer dwells introduces uncertainty into their accuracy and conservatism.

The self-consistent model (SCM), introduced in the preceding section, could be useful in providing a better understanding of the effects of creep-cyclic deformation interactions during deformation histories representative of plant operation on the accumulation of inelastic strain. Of particular importance is to understand better the evolution of the hysteresis loop when creep dwells are substantially longer than those usually introduced in laboratory experiments, i.e. similar to those occurring in service, which are of the order of 1000 hours, and shorter strain ranges of \( \Delta \varepsilon < 0.4\% \), with the aim of confirming the conservatism associated with the modification of the cyclic curve due to softening, as proposed in the R66 data handbook. In addition, it is important to understand how the creep strain accumulation during a load-controlled dwell and displacement-controlled dwell evolves with subsequent cycles, compared to the assumption that creep is unaffected by the prior cyclic deformation and full re-priming of creep behaviour each cycle.

**MODELLING CYCLIC HISTORIES WITH DISPLACEMENT CONTROLLED DWELLS**

In this section we employ the SCM to replicate the deformation response of a particular ex-service austenitic Type 316H stainless steel, as fitted in the previous section, under a particular cyclic loading history where displacement-controlled dwells (pure relaxation) are introduced at the maximum tensile strain position. Experimental data on the softening response during the same cyclic-dwell history of this particular cast of Type 316H is available for dwell durations of 1, 24 and 100 hours at strain ranges between 0.6\% and 1.2\% (Cronin and Spindler, 2016). Considering the available experimental data and the type of strain ranges more relevant to plant operation, e.g. \(<0.4\%\), the SCM is employed here to examine strain ranges between 0.2\% and 0.8\% and dwell times varying from 1 to 1000 hours. Therefore, direct comparison can be established between the predictions via the physically-informed multi-scale SCM with both experimental data and R66 advice on hysteresis loop modification.

In Figure 2 several examples of the predicted cyclic response of the material at \( \Delta \varepsilon = 0.8\% \) for relaxation dwells of 24 and 1000 hours are shown, together with the saturated hysteresis loop without the presence of dwells (pure fatigue).

![Figure 2. Predicted hysteresis loops at \( \Delta \varepsilon = 0.8\% \) for a) pure fatigue and dwells of b) 24h and c) 1000h.](image-url)
The model predictions capture the trend in progressive softening and decreasing stress amplitude of the cyclic response with increasing dwell time; faster saturation of the hysteresis loop (saturation assumed when the change in cyclic stress amplitude reaches 1 MPa) with cycles is found for longer dwells which qualitatively agrees with (Cronin and Spindler, 2016). The observed decrease in saturated stress amplitude for longer dwells is due to the coarsening of the dislocation junction structure as a result of the thermal recovery process during the creep dwell, as described in (Petkov, Hu and Cocks, 2018); it should be noted that the evolution of solute content and precipitates is minimal in ex-service material and will not have significant effects on the observed softening behaviour of the material examined in the present study. Comparison of the predicted variation in saturated stress amplitude with inelastic strain via the SCM, where the total inelastic strain in the cyclic history is the sum of creep strain during the dwell and the plastic strain accumulated during cyclic deformation, with experimental data at 0.6% strain range from (Cronin and Spindler, 2016) is made in Figure 3 (left). The SCM correctly captures the correlation between inelastic strain, dwell time and saturated stress ($\sigma_{sat}$ is measure of the amplitude) corresponding to greater creep strain accumulation at longer dwells, in addition to the increase in saturated stress for greater strain ranges, thus providing confidence in the accuracy of this multi-scale physical modelling approach. In Figure 3 (right), a comparison is also made between the predicted variation of $\sigma_{sat}$ with inelastic strain via the SCM and R66 advice (noting R66 provides a fit for multi-cast data). Although there is an agreement between the trends in softening predicted for different dwell durations via the two approaches, the R66 advice underestimates the decrease in saturated stress amplitude significantly, e.g. at 0.3% strain range the predicted decrease in $\sigma_{sat}$ from that for pure fatigue to a history with a 1000-hour dwell via the R66 advice is ~25 MPa compared to ~75 MPa predicted via the SCM. It is important to note that a difference of 50 MPa in the predicted start of dwell stress can lead to significant inaccuracies in the estimation of creep damage, e.g. the creep rupture strength of ex-service Type 316H at 550°C can vary by approx. an order of magnitude for a stress difference of 50 MPa according to experimental data from EDF Energy.

![Graph](image-url)

Figure 3. Experimental results for Cast 69431 on $\sigma_{sat}$ with $\varepsilon_{ine}$ at $\Delta \varepsilon = 0.6\%$ and SCM predictions (left). Comparison between R66 and SCM predictions on variation of $\sigma_{sat}$ with $\varepsilon_{ine}$ and dwell time (right).
previously mentioned, if an assessment was fatigue-dominated the SCM model suggests that R66 may be non-conservative regarding the estimation of strain enhancement for histories with creep dwells. Although the results are not shown here, the SCM also confirms the excessive conservatism in start of dwell stress prediction via the R5 assessment procedure at this temperature, noted in (Cronin and Spindler, 2016).

Another aspect of the assessment procedures is identified where direct improvements could be made through insights via the SCM. Models in plant assessment procedures are incapable of capturing how the evolution of creep strain accumulation during a dwell changes as the number of cycles increases, i.e. creep strain accumulation during a dwell at a given stress and dwell time is assumed to be the same as that during a dwell at the same stress and time without any prior cyclic loading history. Here, we employ the SCM to systematically predict the accumulation of creep strain during relaxation dwells of different durations and strain ranges, and then predict equivalent relaxation histories with the same duration and start of dwell stress, but without prior deformation histories. Based on this, we introduce a factor which is computed as the ratio between i) the creep strain accumulation during a dwell in a cyclic history at a given stress and duration and ii) creep strain during an equivalent monotonic creep test (N.B. at the same stress and duration without any prior history), i.e. 

\[ f_d = \frac{\varepsilon_{c,dwell}(\sigma_0,t)}{\varepsilon_{c,mon}(\sigma_0,t)} \]

Predicted values of \( f_d \) for different dwell times and strain ranges, as a function of saturation cycles, are summarised in Figure 4.

Figure 4. Evolution of \( f_d \) with total strain range, normalised cycles to saturation and dwell times for relaxation dwells at maximum tensile strain.

As shown in Figure 4, at lower strain ranges and shorter dwell times the SCM predicts a decrease of \( f_d \) with cycles which reveals significant differences between creep strain during dwells to saturation and the equivalent creep loading without prior history at the same stress; i.e. the practical assessment approach will overestimate the creep strain during the dwell. This difference appears to diminish at longer dwells and larger strain ranges due to the fact that the primary creep stage in the material tends to re-initiate at larger strain ranges (Petkov, Hu and Cocks, 2018); the minimum creep rate at higher stresses is expected to be less influenced by the prior plasticity during the cycles (Fookes, Li and Smith, 1999). Based on this, the ratio \( f_d \) can potentially be used directly in assessment procedures as a multiplication factor on the creep strain predicted for a known start of dwell stress for shorter strain ranges relevant to plant operating conditions. For example, for a strain range 0.3% and 1000h creep dwell, beyond an N/N value of 0.5 the predicted creep strain is reduced by \( \sim10\% \). This would in effect, reduce the predicted creep damage by \( \sim10\% \).

The effect of dwell position with respect to strain on the softening response of the hysteresis loop, which is highly-relevant to plant operating conditions where dwells may not always be at the maximum tensile strain position, is examined, for which experimental data on the same ex-service cast of 316H is available at \( \Delta \varepsilon = 0.6\% \) with a dwell at the 0.0% strain position. The SCM is employed, as demonstrated in Figure 1 but with the dwell being positioned at 0.0% strain, for strain ranges between 0.2% and 0.8% and dwell times of 1-1000 hours. Faster saturation of the hysteresis loop with longer dwell times is again
predicted, however, for dwells at mid-strain positions the saturation is reached for a greater number of cycles for a given dwell time as compared to dwells at maximum strain level (see Figure 5). Comparison of the predicted variation in saturated stress with inelastic strain via the SCM with experimental data at 0.6% strain range for 24-hour dwells at \( \varepsilon = 0.0\% \) and \( \varepsilon_{\text{max},\text{tens}} \) (Cronin and Spindler, 2016) is made in Figure 5. The agreement between SCM predictions and the experimental data demonstrates that the softening effect on stress is lower for a mid-strain dwell which is expected due to the lower start of dwell stress. Since the current assessment procedures do not provide advice on the softening response due to dwells at mid-strain positions, the SCM could be used as described above to systematically examine the effects of different dwell positions on softening and provide an extrapolation framework which can incorporate this behaviour in assessment methods; this is not further examined in this article.

![Figure 5](image.png)

**Figure 5.** Experimental data on \( \sigma_{\text{sat}} \) at \( \Delta \varepsilon = 0.6\% \) and SCM predictions for different dwell positions (left). SCM predictions of cycles to saturation with different cyclic-dwell histories and strain ranges (right).

**MODELLING CYCLIC HISTORIES WITH LOAD CONTROLLED DWELLS**

In the present section the cyclic-creep interactions are examined during a deformation history similar to that experienced in thin section pressure-retaining components where primary loads dominate the creep dwell, but the components are still susceptible to some cyclic plasticity due to a combination of primary and secondary loading (Dean and Johns, 2015). The deformation history examined here comprises of loading to maximum tensile strain followed by a constant-stress dwell of a given duration with constant strain range load reversal after the end of the dwell (N.B. start of dwell stress is the same at each cycle). The two extremes of possible creep dwell behaviour which would be considered by an assessor using R5 would be i) that the cycling has no influence on the creep strain evolution and continues unperturbed by the cycling (i.e. a 9000h dwell is identical to 9x1000h dwells); or ii) that cycling re-sets creep deformation behaviour, i.e. full re-priming (therefore the creep strain in 9x1000h dwells would be much greater than one 9000h dwell).

The SCM is employed to assess which of the practical assessment approaches i) and ii) is most reasonable and whether that is consistent dependent of strain range and dwell length. Strain ranges between 0.3% and 0.8% with dwells of length 24-1000 hours are examined. Example of the hysteresis loop (\( \Delta \varepsilon = 0.3\% \)) and creep strain evolution during 24-hour dwells is shown in Figure 6; again, hardening induced by the cyclic deformation causes a reduction in the extent of primary creep strain (Figure 6.b).
The modelling results in Table 2 demonstrate that the difference between the two extremes of behaviour, i.e. assumption i) and ii), is a function of dwell length and strain range. In all cases the actual dwell history response, as predicted via the SCM, lies between the two approaches. As a result, approach i) always remains non-conservative and approach ii) remains conservative. Table 2 shows the extent of this is strongly dependent on dwell length and strain range. With 9x1000h dwells the response is much closer to approach i), with the predicted creep strain within 10% for both Δε. The agreement between the actual predicted history and approach i) for 1000h dwells is due to the diminishing extent of primary creep with cyclic loading, but as a result of load-reversal small amounts of primary creep will be present (Petkov, Hu and Cocks, 2018). Assumption ii) with full re-priming overestimates the accumulated creep strain by a factor of 3 for 9x1000h dwells. For both 24h and 200h dwells approach i) becomes increasingly non-conservative since reduced dwell duration results in some re-priming creep strain, more pronounced at larger Δε. Assumption ii) greatly overestimates the accumulated creep strain – by factors of 10 and 7 for the 45x200h history and 14 and 11 for the 375x24h history at strain ranges 0.3% and 0.8%, respectively. These observations highlight the importance of assessment assumptions regarding cyclic-creep interactions and emphasise the role which strain range and dwell length play in the expected outcome. This is important since experimental data tends to be high Δε and short t_dwell, whereas plant operation tends to be low Δε and long t_dwell. The SCM provides a way to make predictions across the parameter space on a physical basis as well as allowing an intelligent design of experimental studies to verify the predicted behaviour, which is needed in this area. Further discussion can be found in (Petkov, Chevalier, Dean, Cocks, to appear).

Table 2: Creep strain accumulation for two strain ranges after 9x1000-hour, 45x200-hour and 375x24-hour stress dwells and comparison with approaches i) and ii) for the three different 9000h histories.

<table>
<thead>
<tr>
<th>Δε/σ_dwell</th>
<th>9000-hour dwell (i)</th>
<th>9x1000-hour</th>
<th>45x200-hour</th>
<th>375x24-hour</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.3%/140 MPa</td>
<td>0.087</td>
<td>0.31</td>
<td>0.092</td>
<td>0.97</td>
</tr>
<tr>
<td>0.8%/167 MPa</td>
<td>0.157</td>
<td>0.45</td>
<td>0.172</td>
<td>1.61</td>
</tr>
</tbody>
</table>

CONCLUSIVE REMARKS

- The SCM employed in the present study is a versatile modelling tool, which informs the simpler approaches for structural assessment employed in industry, particularly for deformation histories with longer dwell times (>200h) for which experimental studies will be impractical to conduct to the extent to inform empirical models. The SCM can be used systematically to inform industrial user advice by including the effect of dwell position on the expected softening response of the material beyond current empirical fits.
• In displacement-controlled dwells, the SCM model predicts significantly more softening in long term dwells (beyond the current experimental data) compared to current empirical based advice which is designed to be conservative. Increased softening due to creep would dramatically reduce predicted creep dwell stresses and therefore significantly reduce predicted creep damage. However, in assessments total strain range would increase which would be deleterious for the predicted fatigue damage predictions.

• Modelling results reveal that the practical approach i) to estimate the accumulated creep strain for load-controlled dwells in a cyclic history, employed in assessment procedures, underestimates the accumulated strain with a difference of less than 10% (1000h dwells); the approach with full re-priming appears to overestimate creep strain by a factor of 3-14 depending on dwell duration.

• The parameter $f_d$ captures the ratio between i) the creep strain accumulation during a dwell in a cyclic history and ii) creep strain during an equivalent monotonic creep test. The parameter can be employed directly in assessments, allowing for more accurate estimation of creep strain generated during dwells within creep-fatigue loading.

REFERENCES