Thermomechanical Fatigue of Metallic High-Temperature Materials

Summary: The specific aspects of the fatigue behaviour which may result from a combined variation of both mechanical and thermal loading (thermomechanical fatigue, TMF) are illustrated by means of selected examples taken from recent studies. In order to determine TMF-specific changes as compared to isothermal behaviour, TMF tests and corresponding isothermal tests have to be carried out in such a way that a comparative evaluation is possible. Furthermore, the knowledge of the so-called pseudo-isothermal TMF behaviour that defines a reference condition for a characterization of the distinctive features of the behaviour under TMF conditions is very useful. The examples reported in this contribution refer to various alloys which differ in a characteristic way regarding their behaviour under TMF conditions. It will be shown that TMF may result in a particular microstructure giving rise to a specific stress-strain response. Consequently, a lifetime assessment is very likely to be non-conservative if carried out on the basis of isothermal data.

1. INTRODUCTION

Metallic engineering materials used for high-temperature applications are often subjected to cyclic mechanical loading that takes place in combination with a periodic temperature variation (e.g. as a consequence of start-up and shut-down processes). It is well-known from numerous studies that TMF can be the life-limiting degradation mode for severely-loaded high-temperature components, such as turbine blades in aero engines, and that cyclic stress-strain response, crack initiation mechanism and crack propagation behaviour may differ from isothermal conditions. The significance of TMF manifests itself in international conferences which were held during the last 12 years [1-7] and which were mainly devoted to TMF. Despite the knowledge of the danger arising from thermomechanical fatigue (TMF), still only in rare cases particular TMF tests are carried out in the materials testing practice, in order to obtain data relevant
to materials selection and qualification for corresponding service conditions. This is due to the high experimental effort and the large amount of additional testing parameters (e.g. width of the temperature range and phase shift between thermal and mechanical load) which are typical of TMF experiments. Instead, it is often considered a conservative procedure that isothermal tests performed at the maximum temperature of the expected TMF loading are used as basis for a lifetime prediction. However, as a consequence of frequent observations it must be stated that this approach does not hold true, and the necessity of TMF tests needs to become generally accepted. Initiatives have been triggered in the US as well as in Europe for a standardization of corresponding testing procedures. The objective of this paper is to provide an introduction to the field of TMF, its problematic nature and multi-layered character. The emphasis is put on a comparison of the TMF behaviour with the behaviour under isothermal conditions in the corresponding temperature range. Therefore, in the first two sections the question of how to reach comparability is addressed concerning experimental methods and calculation techniques. Then examples from recent studies are reported referring to alloys which differ in a characteristic way regarding their behaviour under TMF conditions. A relatively neutral behaviour is typical of an aluminium alloy that is dispersion strengthened (X8019). The cyclic stress-strain behaviour as well as the cyclic lifetime under TMF conditions can both be predicted on the basis of the data from isothermal fatigue tests. The high-temperature titanium alloy IMI 834 exhibits shortest fatigue life under TMF conditions where a tensile mean stress arises. This observation can be attributed to those specific environmental effects that take place at low and intermediate temperatures and which seem to have a more important influence as compared to creep damage. However, in the case of an austenitic stainless steel, the TMF behaviour is determined by an interaction of fatigue with creep if the maximum temperature exceeds a certain limit and coincides with tensile loading.

2. TMF TESTING

Ideally, fatigue tests should be carried out under conditions which resemble as closely as possible those experienced by the actual component. Since many high-temperature components are subjected to very complex mechanical strain cycles in combination with temperature changes and varying gradients, service conditions are hard to define and even more difficult to reproduce in laboratory testing. Hence, the demand for reliability and safety requires testing under real service conditions that is usually done simply by using the component in its future technical surrounding and often under intensified and more severe conditions. It is clear that this type of testing (e.g. a turbine blade tested within a
jet engine running in a test rig) is expensive and does not provide fundamental findings. Hence, cheaper and more scientifically meaningful test techniques need to be applied, in order to reduce the extent of near-service tests to a minimum.

A first step in this direction is depicted in Fig.1 which schematically shows the experimental arrangement which can be used to perform thermal (stress) fatigue tests. The geometry of the wedge-shaped sample follows that of a turbine blade and the wedge is alternately heated by means of a high-frequency induction coil and cooled by pressurized air. Cyclic temperature gradients result which give rise to cyclic stress and strain. Thermal fatigue cracks initiate at the tip of the wedge and propagate into the sample.

![Experimental arrangement for thermal (stress) fatigue testing](image)

The disadvantage of the testing technique described is that the only information resulting from corresponding tests is the number of cycles until the cracks have reached a pre-defined length that acts as the failure criterion. As a consequence of the locally heterogeneous stress, strain and temperature state, the cyclic stress-strain response cannot be measured, rather only assessed for example by means of finite-element calculations. Therefore, information on the physical mechanisms of damage evolution is widely lacking.

The more fundamental studies on thermomechanical fatigue are carried out under uniaxial loading in universal testing systems. As an example, Fig.2 shows a servohydraulic testing machine which is equipped with an induction heating system. Since this system was designed for studies on the different damage contributions which are relevant to TMF, specimen, hydraulic grips, induction coil and strain gauge are located within a vacuum chamber so that tests can be carried out in high vacuum and well-defined gas atmospheres, respectively. Load and temperature are applied in such a way that the entire gauge length is subjected to the same time-dependent condition. Hence, microstructural examinations (e.g. optical microscopy, scanning electron and transmission
electron microscopy (SEM and TEM)) are relatively easy to be performed.

![Servohydraulic testing system used for TMF tests in defined atmospheres](image)

**Fig.2.** Servohydraulic testing system used for TMF tests in defined atmospheres

**Fig.3.** Strain-temperature phasing in TMF testing phase

Usually TMF tests are carried out under total-strain control using a phase difference of either 0° or 180° between the strain and the temperature command signal. These testing modes which are called in-phase and out-of-phase testing (IP and OP, respectively) are represented as straight lines in Fig.3. In order to
reflect more complex technical situations, sometimes phase shifts of 90° or 270° are applied. The corresponding terms of clockwise or counter-clockwise diamond TMF cycle stem from the diamond-like shape of the strain-temperature path and its circuit direction, as seen in Fig.3. The commonly used IP and OP loading is shown in more detail in Fig. 4. Instead of total strain, plastic strain is applied as feedback signal in the closed loop of the testing system for the cases depicted. This so-called true plastic strain control in combination with a triangular command signal has the striking advantage that the plastic strain amplitude as well as the (absolute value of the) plastic strain rate is kept constant throughout the test.

As it is known from the literature, the effect of the plastic strain rate $\dot{\varepsilon}_{pl}$ on the cyclic stress-strain behaviour and on cyclic life is of significance particularly at intermediate and high temperature. Therefore, by means of plastic strain control the continuous change of $\dot{\varepsilon}_{pl}$ which is typical of stress and total-strain control and may lead to “deformed” hysteresis loops can be avoided. Hence, isothermal tests performed at different temperatures and also tests at different plastic strain amplitudes (with suitable adjustment of the test frequency) can be compared directly on the basis of identical plastic strain rate. It is more important in the context of this paper that the varying temperature which is applied in TMF
testing usually gives rise to a marked alteration in the strain rate sensitivity of the material under consideration. As a consequence, a change in the plastic strain rate within the cycle leads to TMF hysteresis loops which are hard to interpret regarding the dominating microstructural deformation processes. Moreover, a comparison of isothermal with anisothermal cyclic stress-strain hysteresis loops seems to be reasonable only, if the parameter $\dot{\varepsilon}_{pl}$ is eliminated by keeping it constant using true plastic strain control.

A detailed description of how this control mode can be realized technically is given in ref. [9]. The basic idea is that by means of a simple analogue electronic circuit or, in the case of a computer-controlled system, by a mathematical operation, the plastic strain $\varepsilon_{pl}$ is continuously calculated from the stress signal $\sigma$ and the measured total strain $\varepsilon$ according to Hooke’s law.

\[ \varepsilon_{pl} = \varepsilon - \varepsilon_{el} = \varepsilon - \frac{\sigma}{E} \]  

(1)

In the case of a material with low elastic modulus $E$ and a high strength, the deviations from a linear elastic behaviour can become significant, and therefore have to be taken into account in the calculation of the elastic strain $\varepsilon_{el}$. This can be done in a relative simple but accurate way by expanding Hooke’s law with a quadratic term [10]

\[ \sigma = E_0 \varepsilon_{el} + k \varepsilon_{el}^2 \]  

(2)

where $E_0$ is the elastic modulus at $\sigma=0$ and $k$ is a constant. If the temperature varies during the test as it does in a TMF experiment within each cycle, the simultaneous calculation of $\varepsilon_{pl}$ is more difficult, since the thermal expansion $\varepsilon_{th}$, which is a function of temperature, need to be subtracted additionally in eq.(1). Furthermore, the temperature dependence of the Young’s modulus must be taken into account.

As seen in Fig.4, IP TMF testing in true plastic strain control means that the maximum of temperature coincides with the maximum (tensile) plastic strain and vice versa. The high temperature in the tensile half cycle can lead to superimposed creep deformation. Since the flow stress of a material normally decreases with increasing temperature, IP hysteresis loops usually exhibit a negative mean stress and often the maximum stress is reached already before the load reversal point in tension. Under OP conditions the opposite holds true. High temperature acts in combination with compressive stress leading to a positive mean stress.
3. MODELLING OF CYCLIC STRESS-STRAIN BEHAVIOUR UNDER TMF CONDITIONS

The cyclic stress-strain behaviour under TMF conditions is strongly affected by the continuously changing temperature and therefore produces a mechanical hysteresis loop that is complicated in its shape and very difficult to interpret with respect to the peculiarities as compared to isothermal fatigue. An important requirement to identify the specific aspects of the TMF cyclic stress-strain response is the knowledge of the so-called pseudo-isothermal TMF behaviour. This behaviour results from the isothermal cyclic deformation behaviour at each temperature of the TMF cycle assuming that at each point of this cycle the material behaves exactly as it does isothermally under corresponding conditions. Hence, the calculation of the pseudo-isothermal TMF behaviour primarily does not aim at a close reproduction of the real TMF behaviour. Rather, it defines a reference state in which there is no additional effect by the combination of cyclic loading with changing temperature.

Calculation techniques used for TMF are mostly based on viscoplastic constitutive equations. Equations with internal variables were developed among others in the group of Chaboche [11], to describe non-linear kinematic hardening in order to take the Bauschinger effect into account (see [12] for more details). An alternative method that is more appropriate to reflect the physical nature of cyclic deformation is the application of composite or multi-component models. The basic concept of the simple model of Masing [13] is shown in Fig. 5.

![Fig.5. The composite model of Masing [13]](image)

A parallel arrangement of ideally elastic-plastic elements is assumed. As a consequence of the parallel arrangement, all elements undergo the same total
strain. However they can contribute with different stresses to the total stress, since the microscopic yield stresses $\sigma_{if}$ differ. The physical foundation of this model assumptions is basically the observation of a heterogeneous dislocation distribution with strong local variations in the dislocation density in fatigued metals and alloys. Further variations in local yield stress may arise from different grain orientations or when precipitates are present.

The most striking advantage of this type of model is that it does not contain any fit parameter. The calculation of the cyclic stress-strain response under isothermal conditions is solely carried out on the basis of the distribution function of the microscopic yield stress $f_p(\sigma_{if})$. This function, which may be considered a mathematical representation of the microstructure, can be obtained directly from a single branch of a stress-strain hysteresis loop in relative co-ordinates $\sigma_r, \varepsilon_r$ (i.e. related to the last load reversal point) [14] according to:

$$f_p(\sigma_{if}) = -\frac{2}{E} \frac{d^2 \sigma_r}{d\varepsilon_r^2}$$

(3)

For the calculation of the pseudo-isothermal TMF behaviour the following procedure was developed [15]:

- The distribution function of the microscopic yield stress $f_p(\sigma_{if})$ is determined from isothermal fatigue tests at different temperatures.
- These distribution functions are interpolated regarding the temperature so that for each temperature of the TMF cycle the corresponding distribution function can be calculated.
- The Young’s modulus must be known or determined as a function of temperature.
- In an improved composite model the temperature and strain rate dependency of the microscopic yield stresses is taken into account by splitting these stresses in athermal and thermal components. The thermal stress component can be determined by means of an analysis of the hysteresis loops produced in isothermal fatigue tests with stepwise changes of the plastic strain rate.
- At high temperatures, the effect of creep deformation must not be neglected. In a first approach this can be considered by incorporating steady-state creep according to the simple Norton’s equation into the composite model. The parameters of the creep law are derived from creep tests.
- The temperature and strain (load or plastic strain, if pertinent) courses with time are defined according to the TMF cycle considered.
- The distribution function is transformed into a discrete distribution of the yield stresses of a reasonable number of elementary volumes (e.g. 30).
- The stress-strain relation is finally calculated from the reaction of the elementary volumes to small finite changes in total strain and temperature
reproducing the TMF cycle. The total stress that is carried by the composite is the sum of the stress contributions of the elements.

A comparison between an experimentally observed TMF hysteresis loop and the corresponding calculated pseudo-isothermal TMF behaviour is given in Fig.6. The stress-strain loop were measured in an counter-clockwise diamond TMF cycle [16] on the cast Ni-base superalloy IN 100. An interpretation on the effect of TMF loading on the loop shape without knowing the pseudo-isothermal behaviour is almost impossible due to the complex testing condition and the resulting unusual shape of the mechanical hysteresis loop. The calculated stress-strain path shows that the material exhibits stress and plastic strain ranges which are very similar to those expected from the isothermal observations. The strongest deviations can be identified at high temperatures (upper branch at around zero strain).

![Experimental vs. Calculated TMF Loop](image)

**Fig.6.** Comparison of experimentally observed [16] and calculated TMF loop [15]

### 4. CHARACTERISTIC BEHAVIOUR UNDER TMF CONDITIONS

This section is subdivided into three parts according to typical behavioural patterns found in recent studies in which emphasis was put onto comparable experimental conditions for isothermal and thermomechanical fatigue testing. In the first example, the material, a dispersion-strengthened Al alloy, shows a cyclic stress-strain response and a cyclic life in TMF that can be deduced satisfactorily from the isothermal behaviour. This holds true for the second example, that deals with a near-α titanium alloy, only under certain limiting
conditions. Particularly, the damage evolution differs in TMF and isothermal testing giving rise to a non-conservative lifetime prediction if based solely on isothermal data. In the third example, an austenitic stainless steel is considered which does not only form a microstructure which depends strongly on testing mode, but also engages TMF-specific damage mechanisms.

4.1. The dispersion-strengthened Al alloy X8019
Dispersion-strengthened Al alloys, which are produced via a powder-metallurgical route, show excellent microstructural stability at high temperatures [17]. Hence, as opposed to precipitation-hardened alloys which undergo coarsening at elevated temperatures, strengthening by dispersoids remains effective during high-temperature applications.

Figure 7 documents that in the case of the alloy X8019, which has a nominal composition of Al-8Fe-4Ce, a strong interaction of dislocations with the small dispersoids takes place leading to a relatively high strength. Microstructural changes are not observable in the temperature range of interest (up to about 350°C) and there is also no appreciable influence of temperature and plastic strain amplitude applied in fatigue testing. More important, the type of testing (isothermal or TMF) does not seem to play a significant role for the microstructure.

Taking these microstructural findings into account, it is not surprising that the cyclic stress-strain behaviour of this material under TMF conditions can be modelled as pseudo-isothermal behaviour quite accurately [18,19]. In Fig. 8 the hysteresis loop registered in an IP TMF test with a temperature variation between 100°C and 300°C is compared with a loop calculated as described above. There is just a slight deviation at high temperatures (close to the maximum of plastic strain) which can mainly be attributed to the uncertainty in
extrapolating isothermal data observed at $\dot{\varepsilon}_{pl} \geq 10^{-3} \text{ s}^{-1}$ to a plastic strain rate of $10^{-5} \text{ s}^{-1}$ that was used in TMF testing.

A lifetime assessment for TMF is very complex and usually requires a detailed knowledge of the damage mechanisms which are specifically relevant under
TMF conditions. In the case of the very stable alloy X8019, a prediction can be done using solely isothermally determined data. Since fatigue crack propagation was found to be life determining, a corresponding method, which is based on the $\Delta J$ concept and additionally takes oxidation damage into account [20], was applied. Some reasonable assumptions and minor adaptations of this model, which are discussed in detail in ref.[21], provide very satisfactory prediction accuracy. Figure 9 summarises the results for both isothermal and TMF tests. The dotted lines mark the range within which the deviation of prediction and actual value is better than a factor of 2.

It is interesting to note that cyclic life under TMF conditions is similar (IP) or even longer (OP) as compared to isothermal tests at the maximum temperature of the TMF cycle. Hence, the material considered behaves very conservative, and a reasonable life assessment could be carried out, if the highest temperature is considered to be the most severe condition. A completely reverse behaviour was observed in a comparative study of a particle-reinforced material with X8019 as matrix [18,21]. The addition of SiC particles leads to a strong life reduction under TMF conditions and a coupling of damage mechanisms occurs so that lifetime prediction from isothermal data is hardly possible.

4.2. The high-temperature titanium alloy IMI 834
In order to reduce the weight of jet engines in modern aircrafts, there is a strong tendency to substitute heavy nickel-base alloys by suitable less dense titanium alloys. IMI 834 is the most recent commercial high-temperature Ti alloy which was designed for the use as disc and blade material in the hot part of the compressor of jet engines at intended service temperatures of up to values above 600°C [22]. These high maximum temperatures that are far beyond the range of application of earlier Ti alloys give rise to the interest in TMF of IMI 834, since thermal strains are no longer tolerated by the material as elastic deformation. Contrary to the behaviour of the dispersion-strengthened Al alloy X8019 the microstructure of IMI 834 depends both on temperature and testing mode. In the case of isothermal fatigue tests, a transition from a more planar dislocation arrangement existing at low and moderate temperatures to a more homogeneous dislocation distribution at high temperature is observable [23,24]. This transition that takes place at test temperatures of around 600°C manifests itself in the TMF tests as well. The decisive factor seems to be the value of the maximum temperature $T_{\text{max}}$ engaged in the TMF cycle considered. If this value exceeds the microstructural transition temperature a more wavy-type dislocation arrangement results, while at $T_{\text{max}} \leq 600^\circ\text{C}$ planar glide behaviour prevails so that slip is mainly localized to slip bands. A comparison is given in Fig.10 showing two TEM micrographs taken after TMF experiments with temperature ranges of 400°C-600°C and 400°C-650°C, respectively. Except for the maximum temperature, all other testing parameters were identical.
Fig. 10. Dislocation arrangement in IMI 834 (TEM micrographs from primary $\alpha$-grains) after TMF at $T = 400^\circ$C-600$^\circ$C (left) and 400$^\circ$C-650$^\circ$C (right), plastic strain amplitude 0.2%, $g=(10\overline{1}1)$.

Fig. 11. Cyclic stress response curves of a isothermal fatigue test at 400$^\circ$C and an IP TMF test performed with changes between anisothermal to isothermal (400$^\circ$C) conditions [25].

Taking this observation into account, it is not surprising that a modelling of the cyclic stress-strain response by means of the pseudo-isothermal approach
provides reasonable results only, if $T_{\text{max}}$ is below the transition temperature, since this condition leads to a microstructure that is widely independent of temperature and testing mode [15]. The predictive capability becomes poor at higher values of $T_{\text{max}}$ as the change in dislocation slip mode in the high-temperature part of the TMF cycle results in a microstructure in the low-temperature part which strongly deviates from the corresponding isothermal situation. In Fig.11, the cyclic deformation curve of an isothermal test performed at 400°C is compared with the stress response under IP-TMF conditions at $T=400°C-650°C$. In both cases a plastic strain amplitude of 0.2% and a plastic strain rate of $4 \cdot 10^{-5} \text{ s}^{-1}$ was applied. In the specific TMF experiment shown in Fig.11, the temperature cycling was interrupted after about 400 cycles at the minimum strain, i.e., at 400°C, and cycling was continued isothermally at this temperature. The stress amplitude resulting from this change from anisothermal to isothermal conditions is clearly higher than expected from the corresponding plain-isothermal test. Hence, it can be concluded that the microstructure formed in the high-temperature part of the TMF loop is much harder if tested at 400°C than the structure that forms normally at 400°C. The slow decrease of the stress amplitude towards the isothermal reference value indicates that the TMF-specific microstructure is relative stable and that large cumulative plastic strain is necessary for a complete transfer into the isothermal state.

Table 1. Number of cycles to failure for TMF tests at a plastic strain amplitude of 0.2% in air

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>350-600</th>
<th>350-650</th>
<th>400-600</th>
<th>400-650</th>
<th>450-650</th>
</tr>
</thead>
<tbody>
<tr>
<td>$N_f$ (in-phase)</td>
<td>1100</td>
<td>620</td>
<td>1162</td>
<td>800</td>
<td>950</td>
</tr>
<tr>
<td>$N_f$ (out-of-phase)</td>
<td>570</td>
<td>285</td>
<td>482</td>
<td>317</td>
<td>320</td>
</tr>
</tbody>
</table>

Table 2. Number of cycles to failure for isothermal tests at a plastic strain amplitude of 0.2% in air

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>350</th>
<th>400</th>
<th>450</th>
<th>600</th>
<th>650</th>
</tr>
</thead>
<tbody>
<tr>
<td>$N_f$</td>
<td>1566</td>
<td>1420</td>
<td>1472</td>
<td>850</td>
<td>685</td>
</tr>
</tbody>
</table>

Selected life data is shown in tables 1 and 2 for TMF and isothermal conditions, respectively. Irrespective of the position and width of the temperature range in
which the TMF tests were carried out, out-of-phase testing proved to be more
detrimental as compared to in-phase conditions. A detailed consideration of the
various damage contributions being involved indicates that the main difference
in cyclic lifetime between IP and OP results from a strongly different sig-
nificance of degradation related to the effect of water vapour [26]. This damage
mechanism provides a strong contribution (in addition to pure fatigue and
oxygen-induced damage), if low and moderate temperature act in combination
with tensile stress (i.e., when cracks are open). Hence, OP conditions are very
unfavourable as tensile stresses prevail in the low temperature part of the loop.
By contrast, in IP TMF compressive stress is predominant at low temperature
avoiding the exposure of freshly formed crack surface to water vapour.

Fig.12. Surface cracks formed isothermally at 400°C (left) and during TMF at 400°C-
600°C (right)

A comparison of the data shown in table 1 and table 2 clearly documents the
danger that a non-conservative lifetime assessment results, if the maximum
temperature of TMF loading is used in an isothermal test to represent the most
severe condition. While in this way for IMI 834 the TMF life for in-phase
conditions may be estimated with reasonable accuracy, this method fails for out-
of-phase loading.

Distinct differences between TMF and isothermal conditions regarding damage
evaluation can be found both for crack initiation and crack propagation. A very
clear effect which refers to the crack initiation site is illustrated in Fig.12 as an
example. During isothermal fatigue at 400°C, cracks are preferentially formed
in primary-α grains of the bimodal microstructure studied. By contrast, these
primary-α grains seem to act as obstacles to crack growth in TMF, where cracks
initiate in the lamellar microstructure of transformed-β grains. Here indeed, the
maximum temperature of the TMF cycle seems to determine the relevant crack
formation mechanism, since a change in the surface crack appearance towards
the TMF situation takes place with increasing isothermally applied temperature.
Nevertheless, this example underlines that the reaction of a material to TMF
conditions is almost unpredictable from the isothermal behaviour.
4.3. The austenitic stainless steel AISI 304L
The economical and ecological demand of higher effectiveness in energy conversion gives rise to steadily increasing operating temperatures in power plants which can hardly be sustained by ferritic steels. Consequently, austenitic stainless steels have become promising candidate materials for respective future applications, since their temperature capability fills the gap between ferrite steels and the very expensive nickel-base alloys. TMF is of technical significance for austenitic steels particularly due to their low thermal conductivity in combination with high thermal expansion that may cause large thermal stresses. In the context of this paper the results obtained on the steel AISI 304L represent an extreme case where for TMF conditions neither the cyclic stress-strain response nor the damage evolution (i.e. the cyclic life) can directly be deduced from isothermal experiments [27].

Fig. 13. Dislocation arrangement in AISI 304L after isothermal fatigue at 250°C (a), 400°C (b) and 650°C (c) (plastic-strain control at a plastic strain amplitude of 0.5%)

First information on TMF-specific features is given from microstructural observations. TEM micrographs showing the typical dislocation arrangements formed in isothermal tests at different temperatures are compared in Fig. 13. Obviously the effect of temperature is very marked leading to a cell structure both at low and high temperature (Fig. 13a and 13c) and to a more homogeneous distribution of the dislocations at intermediate temperatures (Fig. 13b). The reason for this pronounced transition with increasing temperature from wavy dislocation slip to a more planar glide behaviour and back to wavy slip is an impeded dislocation mobility due to the occurrence of dynamic strain ageing processes which affect cyclic plasticity strongest in a broad temperature range around 450°C. If a TMF cycle is run in such a way that the temperature regime of dynamic
strain ageing is included within the temperature range applied, the material cannot change its microstructure quickly enough in each cycle to follow the corresponding isothermal dislocation arrangements. Hence, after a transient period a steady-state condition is established that is characterized by a distinctive TMF microstructure not formed in constant-temperature tests [28]. Figure 14 represents this microstructure for IP TMF conditions with T=300°C-550°C. Dislocations are arrange in such a way that dislocation-poor channels parallel to the trace of the slip plane form (Fig.14a) or a labyrinth structure appears (Fig.14b).

![Fig.14. Dislocation arrangement in AISI 304L after IP TMF at T=300°C-550°C; (a) channel structure and (b) labyrinth](image)

The consequences arising from this TMF-specific microstructure are quite obvious. The TMF hysteresis loops strongly deviate from the pseudo-isothermal ones [15], since the respective microstructures are different. The deviations are highest in the high-temperature part of the TMF loop indicating that the “averaged” TMF dislocation arrangement is harder than the corresponding isothermal cell structure. In order to understand the implications of these observations to cyclic lifetime it is important to know that for AISI 304L at temperatures above about 600°C creep damage strongly contributes to damage evolution under cyclic loading. Wedge cracks form at grain boundary triple points and pores nucleate and grow preferentially at grain boundaries [29]. Since in OP testing compressive stresses act at high temperatures, the creep damage contribution is much less significant as compared to IP conditions. Consequently, IP is much more detrimental than OP for AISI 304L despite the opposing influence of the mean stress that is positive (tensile) for OP and negative for IP loading. The higher stresses resulting from the TMF-specific
microstructure at high temperatures lead to an accelerated creep damage contribution under IP conditions as compared to the isothermal behaviour at constantly applied maximum temperature. This negative effect even outweighs the positive effect that in TMF creep is restricted to the high-temperature part of the cycle.

5. CONCLUDING REMARKS

The examples from recent studies given above document clearly that the deformation behaviour as well as the damage evolution under thermomechanical fatigue conditions show distinctive features which can be reproduced in isothermal tests only in very rare cases. Hence, the TMF behavioural patterns are hardly to be deduced from isothermal test results. Consequently, the data that is necessary for a safe and reliable application of high-temperature materials as components in structures, where these components are subjected to cyclic mechanical loading in combination with cyclic thermal loading, must be obtained in suitable TMF experiments. If the resulting behaviour shall be compared with the behaviour under isothermal conditions, those testing parameters which may strongly affect the cyclic stress-strain response, such as plastic strain amplitude and plastic strain rate should be held constant. Furthermore, an appropriate modelling method needs to be used, in order to transform the isothermal data to non-isothermal conditions. If these prerequisites are not fulfilled, the determination of TMF-specific peculiarities is very difficult.

The methods used for cyclic lifetime prediction under TMF conditions were not considered in detail here, since this is a very complex topic on its own and is far beyond the scope of this paper. However, it should be stated that amongst the various methods that are reported in the literature, those seem to be most promising for complex loading situations that are mechanism-based and incorporate a physically measurable quantity of damage. Furthermore, the prediction method should be able to take microstructural aspects into account. From this point of view, microcrack propagation models, e.g. based on the $\Delta J$-concept with expansions for environmental degradation and creep damage, are very attractive. If specific and well-directed TMF tests complemented by thorough microstructural studies are carried out and the findings regarding the relevant damage mechanisms are incorporated in such a model, reasonable predictive accuracy can be reached. On the basis of this treatment of TMF distinctive features, all three materials dealt with in this paper show a satisfactory TMF life predictability.
REFERENCES


[27] Zauter R, Petry F, Christ H-J, and Mughrabi H, in [1], pp 70-90
