Fatigue Assessment of Thermal Cyclic Loading Conditions based on a Short Crack Approach

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Abstract

Thermal loading conditions of nuclear power plant components cause local stress-strain hystereses. For the fatigue life prediction of nuclear power plant components under thermal cyclic and structural loading a new method based on the local strain approach is to be presented. This method involves finite-element simulations as well as the experience gathered from lifetime assessment methods based on short crack models.

The local stresses and strains are obtained from coupled-field FE-analyses. The calculation of the hysteresis-loops relies on appropriate material models and experimentally verified temperature-dependent material parameters in order to describe the elasto-plastic behavior of the material as realistically as necessary. Due to the temperature dependence of the material parameters the resulting hysteresis loops are of non-conventional shapes and similar to those observed under multiaxial non-proportional structural loading. Hence, fatigue methodologies developed for non-proportional loading conditions during the past years bear good prospects for successful application under non-isothermal loading conditions.

Keywords: Thermal fatigue; local strain approach; mechanistic damage parameter; mechanically short crack models; effective cyclic J-integral; crack closure; mean stress; load sequence; surface finish

1. Introduction

Thermal cyclic loading conditions can be found in various fields of application and may affect geometrically simple structures just as much as complex NPP component assemblies. Upon closer observation nearly all of these thermal load cases turn out to be thermal cyclic loading. Similar to purely structural loading these thermal load cycles result in damage relevant events.

For an assessment of the isothermal fatigue performance of components in general several approaches have been proposed which may be based on nominal or local stresses, on local strains or the assessment of fatigue crack growth [2, 3]. In the nuclear power plant technology the design codes (ASME, KTA, RCC-M) [4–6] guide the way for performing such proofs of fatigue strength. These codes are based on the local strain approach [7–9]. Within the local strain approach several modules have to be worked on and they are mirrored in the codes. Basic input

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quantities are the strain life curve (often described by the Manson-Coffin-equation [10–13] but usually plotted in terms of fictitiously elastic stress amplitudes or ranges in the design codes) as well as information on the cyclic plastification behavior of the material (plastification factors $K_e$ or cyclic stress-strain-curves for fully elasto-plastic analyses). Next, the transient relation between external loads and local strains at all fatigue critical locations is required (temperature is treated as an external load in this respect as well). In a linear elastic regime well known notch factors (for purely mechanical loading) or better finite element analyses will provide the necessary transfer information. However, as the non-linear relation is generally required either an elasto-plastic finite element analysis has to be performed or an approximation scheme like Neuber’s rule or code-based plastification factors $K_e$ are used. In the case of purely mechanical uniaxial or multiaxial-proportional loading together with specifying Masing behavior and the material memory rules complete local stress-strain paths are simulated [8].

It is widely accepted that the stage to initiate a technical crack is governed by short crack growth [14, 15]. The fatigue process of initiating a technical crack size can therefore advantageously be modeled based on short crack fracture mechanics. For isothermal fatigue numerous models have been proposed, see [14, 15]. However, rarely any such attempts are known to the authors with respect to non-isothermal fatigue. An extension of the model proposed in [15] is formulated in this paper for the first time paving the way for a short crack growth assessment under non-isothermal fatigue conditions of non-homogeneously stressed structures. The method is totally embedded in the framework of the local strain approach. This means that the conventional tools of this approach are used as usual; however, the damage assessment module is replaced by the procedure introduced below. It incorporates the temperature dependence of the material parameters into the concept of a damage parameter based on the cyclic $J$-Integral or the cyclic crack tip opening, respectively, and it is formulated in terms of effective local strain and stress ranges. Existing approaches for $J$-integral damage parameters use an approximation assuming a power law for the stress-strain relation. Due to the fact that thermal cyclic loading usually leads not only to multiaxial stress-strain states but might also produce unconventional hysteresis shapes the presented approach utilizes methods from the damage assessment of multiaxial non-proportional loading presented in [16].

2. Generating Data by FEM-simulations

The hysteresis loops for the damage assessment are solely determined by FEM analyses. The structural model used to generate the hysteresis loops for the presented method is a thick-walled tube which is assumed to be straight in axial direction. The structure is modeled with three-dimensional elements to provide a high versatility and to ensure that the model can be easily extended to suit more complicated geometries.

![Figure 1: Examples of hysteresis loops](image-url)
To minimize the requirements on the hardware and the time to do the calculations the model uses the maximum amount of symmetries provided by the component geometry. This means that only a 1° segment of the tube is modeled. Furthermore, the model is chosen to be only one element in axial direction. Together with the assumption of plane cross sections this provides the possibility of using only few elements to model a tube of arbitrary length. Due to the expected stress gradient the discretization scheme implies a denser element distribution towards the inner wall. The global x-direction corresponds to the axial direction whereas the y and z-directions correspond to the circumferential and reverse radial directions respectively.

The inner wall of the tube is subjected to a constant internal pressure \( p \) and a time dependent temperature transient with temperature \( T(t) \) ranging from 50° C to 350° C. The temperature dependent thermal and mechanical material parameters for the austenitic steel 1.4550 (X6CrNiNb18-10, AISI 347) are taken from the KTA rule [17], the research report [18] and the technical report [19]. These reports cover the above mentioned temperature range from 20° C to 350° C.

The cyclic elasto-plastic behavior of the material is described using the Chaboche plasticity model [20]. The according parameters for the given material were determined explicitly in [18]. The analyses for the temperature distribution and for the structural solution are run in two separate steps using the load transfer method and leading to a unidirectionally coupled-field solution. The first analysis determines the time-dependent temperature distribution for all discrete locations of the model. In the second step this temperature distribution is then applied as a load together with the pressure to the structural model in order to determine the deformations and the resulting stresses and strains. The hysteresis loops are calculated for several consecutive cycles of one transient to obtain quasi-stabilized hysteresis loops.

Figure 1 shows examples of the resulting hysteresis loops in circumferential and axial direction on the inside wall of the tube respectively. Since both hystereses result from the same temperature transient their shape is very similar. The radial components of the stresses do not result in damage relevant hysteresis loops.

3. Model for short fatigue cracks

The model presented in this section is based on Döring’s [16] proposal. It combines elasto-plastic short crack fracture mechanics with a critical plane approach [21]. The critical plane approach requires a consecutive rotation of the components of the stress and strain tensors into different planes. The procedure starts with the global circumferential and axial directions. By application of the criterion of maximum crack growth and minimum life respectively a critical plane is determined. As described in [22] a short crack is assumed to start at an initial crack length \( a_0 \) and ends at the size of a technical crack depth \( a_c \). Hence, the term short crack is used in the sense of physically short cracks and not microscopically short cracks [23] in this paper. The initial crack length has to be determined by backward integration of the crack growth equation (1). The resulting fatigue life is the amount of cycles \( N \) necessary to form a crack of the physical length \( a_c \). This means that all the fatigue life influencing factors due to the microstructure of the material are taken into account in an integrating way in the initial crack length \( a_0 \).

No explicit crack growth modeling through inhomogeneous microstructures is performed. Based on the concept introduced in [16] the calculations are performed based on a Paris-type equation for the crack growth rate as a function of the effective cyclic J-integral:

\[
\frac{da}{dN} = C(\Delta J)^m
\]  

(0)

with the crack growth rate \( \frac{da}{dN} \) and \( \Delta J \) being the effective cyclic J-integral. The material parameters \( C \) and \( m \) have to be determined using experimental crack growth data. The initial crack length \( a_0 \) is assumed to be a material constant and does not depend on the loading. In the framework of this model the driving force for the crack \( \Delta J \) is calculated by evaluating its components which in the present case are separated into the contributions from mode I and mode II as within the framework of the fatigue damage model a semicircular surface crack is supposed to grow. The crack face normal vector and the material surface normal vector are assumed to be perpendicular. The crack remains plane during its growth. Only the crack growth at the intersection of the crack front with the surface is modeled. The shape of the crack is assumed to be semicircular as stated above. At the shear stress-free surface, only mode I and mode II states can exist. For a given cycle, the simplest mixed mode hypothesis is applied:
It is worth noting that the mode II contribution in equation (2) plays an almost negligible role in this investigation due to the fact that the resulting stress-strain states are nearly equi-biaxial.

Clearly, the stress-strain hystereses shown in figure 1 cannot be expressed by means of functions. This means that an approximation as proposed in [24] or [22] is not applicable in the present case. Instead the decomposition of the $\Delta J$ in [16] is calculated by assuming proportionality between the $J$-integral components and the product of the strain energy density and the crack length $a$:

$$\Delta J = (\Delta J_{\text{eff}}^1 + \Delta J_{\text{eff}}^2)^{\frac{1}{2}}. \quad (2)$$

$$\Delta J_{\text{eff}}^1 = 2\varepsilon Y_1^2 \Delta W_{\text{eff}} \cdot a \quad \text{(3)}$$

$$\Delta J_{\text{eff}}^2 = \frac{\varepsilon}{1+\nu} Y_2^2 \Delta W_{\text{eff}} \cdot a. \quad \text{(4)}$$

The terms $Y_1$ and $Y_2$ are functions of the crack geometry for the corresponding mode.

A key element in any short crack model is the appropriate consideration of crack closure effects. Accordingly not the complete hysteresis causes crack growth. The contributing parts for mode I are considered to be the phase when the flanks of the crack are not touching and the crack is open. For mode II the tip shielding as a consequence of the roughness of the flanks is to be taken into account. To determine the crack opening stress Newman’s suggestion [25] is widely accepted. The multiaxial short crack model uses a modified version of these equations. The crack opening stress in $x$-direction $\sigma_{op}^{x}$ is estimated as:

$$\sigma_{op}^{x} = A_1 + A_2 R_v + A_3 R_v^2 + A_4 R_v^3 \quad \text{for } R_v > 0$$

$$\sigma_{op}^{x} = A_1 + A_2 R_v \quad \text{for } R_v \leq 0 \quad \text{(5)}$$

with

$$A_0 = 0.535 \cdot \cos \left( \frac{\pi}{2} \frac{\sigma_{\text{eqv, max}}}{\sigma_y} \right) + A_{\text{max}}. \quad \text{(6)}$$

$$A_1 = 0.344 \cdot \frac{\sigma_{\text{eqv, max}}}{\sigma_y} + A_{\text{max}}. \quad \text{(7)}$$

$$A_2 = 1 - A_0 - A_1 - A_2 \quad \text{(8)}$$

$$A_3 = 2A_0 + A_1 - 1 \quad \text{(9)}$$

and

$$R_v = \begin{cases} \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}} & \text{if } \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}} > 0 \\ \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}} & \text{otherwise} \end{cases} \quad \text{(10)}$$

This relation is evaluated for every investigated plane so that the $x$-direction varies with each iteration. The parameter $A_{\text{max}}$ is a fitting parameter to account for the material’s sensitivity to mean stress. The variable $\sigma_{\text{eqv}}$ is the mean value of the material’s ultimate tensile strength $R_m$ and the stress value $R_{p0.2}$ associated with 0.2% plastic strain on the cyclic stress-strain curve.

$$\sigma_{\text{eqv}} = \left( \frac{R_m + R_{p0.2}}{2} \right). \quad \text{(11)}$$

The variables denoted with the index ‘eqv’ correspond to an equivalent stress and $\sigma_{\text{eqv, max}}$ is defined as:

$$\sigma_{\text{eqv, max}} = \sqrt{\left( \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}} \right)^2 + 3 \left( \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}} \right)^2 \cdot \frac{\sigma_{\text{eqv, max}}}{\sigma_{\text{max}}}}. \quad \text{(12)}$$
The shear values in Eqn. (12) as well as the values of all other components for $\sigma_{e,\text{max}}$ have to be evaluated at the point of time when the maximum and minimum of $\sigma_e$ occur respectively. The index ‘max’ indicates the maximum value whereas ‘min’ marks those values to be taken at their respective minimum.

Vormwald and Seeger [26] showed that under uniaxial loading crack opening and closure can be assumed to occur at the same strain level:

$$\varepsilon_{\text{op}} = \varepsilon_{\text{cl}}.$$  \hspace{1cm} (13)

Since $\varepsilon_{\text{op}}$ can be easily determined on the ascending part of the hysteresis this allows to identify the crack closure stress on the descending part of the loop. Knowing the opening stress for the hysteresis loop of interest from the set of Eqns. (5)–(12) the opening and closure points can be identified as figure 2 shows.

Existing damage parameters as given by Vormwald [22] and revised by Savaidis [27] use an approximation for the geometric functions $Y_i$ in Eqns. (3) and (4) and assume the branches of the hysteresis loops to follow a power law. This allows to establish analytical solutions for the integrals given in the Eqns. (14) and (16) leading to a damage parameter based on the cyclic J-integral and independent of the crack length. However, in the present case and due to the unconventional shape of the resulting hysteresis loops the notion of a power law describing both hysteresis branches does not hold true. As a result the integrations for the $\Delta W_i$ have to be carried out numerically.

With the increments of the effective stresses and strains the effective strain energy density $\Delta W_{\text{eff}}$ for mode I for the effective cyclic J-integral in Eqn. (3) can be computed as:

$$\Delta W_{\text{eff}} = \frac{\Delta \sigma_{\text{eff}}^2}{2E} + \left\{ \int_{\varepsilon_{\text{op}}}^{\varepsilon_{\text{cl}}} \left[ \sigma_x(\varepsilon_{x,\text{max}}) - \sigma_x(\varepsilon_{x}) \right] \, d\varepsilon_x \right\}$$  \hspace{1cm} (14)

where the effective range of the stress is

$$\Delta \sigma_{\text{eff}} = \sigma_{x,\text{max}} - \sigma_{x,\text{cl}}$$  \hspace{1cm} (15)

and the superscript ‘pl’ stands for ‘plastic’.

Figure 2: Opening and closure points and resulting effective ranges
For the mode II contribution the whole loop is considered to be relevant and the effective part of the strain energy density is determined by a factor $U_{eff}$:

$$\Delta W_{str} = \left\{ \frac{\Delta \tau_{xy}^2}{2G} + \int_{x_{max}}^{x_{min}} \left[ \tau_{xy}(x_{max}) - \tau_{xy}(x_{min}) \right] d\tau_{xy} \right\} \cdot U_{eff}$$

(16)

The shielding function $U_{eff}$ may be computed as a relation between stresses or between strains. For the present model it is defined in terms of shear strains as:

$$U_{eff} = \frac{\max\{\gamma_{xy,ext}(t) - \gamma_{xy,mt}(t)\}}{\Delta \gamma_{eqv}}$$

(17)

with the reduced effective shear strain

$$\gamma_{xy,ext} = \gamma_{xy}(t) \left( \frac{2\Delta \tau_{xy}}{\Delta \tau_{xy} + \Delta \tau_{mt}} \left( 1 - \frac{\sigma_{is}(t)}{\sigma_{mt}} \right) \right)$$

(18)

In the above equation $\sigma_{is}$ is the value of normal stress necessary to completely separate the crack flanks and the variable $\Delta \tau_{mt}$ is the activation shear stress representing the lower bound of the shear stress necessary to “unlock” the crack. Both these values are considered to be material parameters. The range of the shear stress $\Delta \tau_{xy}$ is determined using the “longest chord approach” based on the description in [28].

In the above Eqns. (14), (16) and (18) the Macaulay Brackets $\langle ... \rangle$ ensure that only positive values of the enclosed terms are considered. In both Eqns. (14) and (16) the elastic contributions of the strain energy density are calculated by the fractions preceding the integrals which determine the contribution of the plastic components. In these relations it is assumed that for the strain energy density the relations given in Eqns. (3) and (4) hold true for both the elastic and the plastic parts individually.

The set of equations given in this section is sufficient to determine the fatigue life for a short crack growing from an initial length $a_0$ to its ending size $a_e$. As mentioned above this whole calculation has to be repeated for several planes to determine the critical plane by way of iteration.

4. Extension and modification of the short crack model for thermal cyclic loading

To adapt the short crack model given in the previous section to thermal cyclic loading conditions some assumptions have to be made. Returning to the basic crack growth equation (1) it is necessary to specify how the temperature dependence of the material parameter $C = C_r(T)$ is taken into account. In [29] the hypothesis has been presented and verified that the crack growth law is independent of the temperature if expressed in terms of the cyclic crack tip opening displacement $\Delta CTOD$. As the J-integral and $\Delta CTOD$ are correlated via

$$\Delta CTOD = \frac{\Delta J}{\Delta \sigma_r}$$

(19)

the temperature independent crack growth law

$$\frac{da}{dN} = C_r \left( \Delta CTOD \right)$$

(20)

can be expressed as

$$\frac{da}{dN} = \frac{C_r}{\left[ \Delta \sigma_r(T) \right]^n} \left( \Delta J_{int} \right)$$

(21)

$$= C_r(T) \cdot \left( \Delta J_{int} \right)$$

with the temperature dependent Paris-constant

$$C_r(T) = C_r(RT) \left[ \frac{\Delta \sigma_r(T = RT)}{\Delta \sigma_r(T = T_r)} \right]^m$$

(22)

In the above equations RT denotes room temperature which is assumed to be at 20° C. The material’s yield stress $\Delta \sigma_r$ is inserted as the cyclic 0.2% plastic offset stresses taken from the material’s stabilized cyclic stress curve $R_{0.2}$. 

It is considered at the temperature of the upper reversal point of the hysteresis loop $T_o$, since this will be the point of maximum crack tip opening displacement.

Since the initial crack length $a_i$ is derived by backward integration of the Paris-type crack growth equation it can also be assumed to be temperature independent. The temperature load does not change the shape of the crack geometry. This leads to the assumption that the factors $Y_i$ in Eqns. (3) and (4) are also not depending on the temperature.

As a consequence the temperature influence has to be considered when calculating the crack opening stress $\sigma_{op}$ and in the evaluation of the integrals to determine the strain energy density.

Most of the values used for insertion into the set of Newman type Eqns. (5)–(12) can be taken directly from the hysteresis to be analyzed. The mean stress sensitivity parameter $A_{mean}$ is again assumed to be independent of the temperature. As the local stress ratio $R_\sigma$ is close to 1.0 for the hysteresis curves used in the analyses this parameter is not causing a major contribution. However, $\sigma'_c$ is considered to be temperature dependent since $R_{\sigma_{I,2}}$ and the ultimate tensile strength $R_m$ are considered to be temperature dependent. To account for this the set of modified Newman equations is used to determine a crack opening stress. Analogous to the course of action described in the previous section the crack opening (closure) strain is obtained by scanning the ascending (descending) branch of the hysteresis. Knowing the temperature distribution for the whole hysteresis at each FEM computed data point the crack opening stress/strain can be related to a temperature. Using this temperature usually is completed after 2–3 iterations.

Although Newman’s equations have been successfully applied to purely mechanical load cases and verified in this context their application in the case of thermal loading is a hypothesis. Further investigations will be required to validate the applicability of these formulae in the context of thermal cyclic loading.

The equation for the effective mode I strain energy density has to be modified to allow for temperature dependent material parameters. In Eqn. (14) a temperature dependent Young’s modulus $E(T)$ has to be taken into account:

$$\Delta W_{int} = \frac{\Delta \sigma_{int} \Delta \varepsilon_{int}^p}{2} + \left( \int_{\sigma_{int}}^{\sigma_{int}} \left[ \varepsilon_{int} - \varepsilon_{int}^p \right] d\varepsilon_{int}^p \right)$$

with

$$\Delta \varepsilon_{int}^p = \varepsilon_{int}^p - \varepsilon_{int}^p \left( \frac{T_{\sigma_{int}}}{T_{\sigma_{int}}} \right)$$

Using Eqn. (24) an effective range of elastic strain can be computed. This equation is derived using Eqn. (15) and determining the elastic strain at the according points using Young’s modulus for the corresponding temperature. Although the changes in Eqn. (23) appear to be minor, for every step in the numerical integration the plastic increment $\Delta \varepsilon_{int}^p$ has to be evaluated using the relation

$$\varepsilon_{int}^p = \varepsilon_{int}^p - \frac{\sigma - \nu \sigma_x}{E(T_{\sigma_{int}})}$$

and determining the temperature dependent Young’s modulus for each integration step individually. To evaluate the bracketed expression in the integral in Eqn. (23) the same approach is to be taken.

For the mode II contributions the equations are modified in the same manner as already described above. The material parameter to be used for the mode II equations is the shear modulus $G$. It can be expressed in terms of the Young’s modulus and the Poisson ratio as:

$$G = \frac{E}{2(1+\nu)}$$

Since the Poisson ratio is generally assumed not to be temperature dependent for the temperature range considered in the present context the temperature dependence of $G$ corresponds directly to the behavior of Young’s modulus $E$. Equation (16) can be rewritten as:

$$\Delta W_{int} = \frac{\Delta \sigma_{int} \Delta \varepsilon_{int}^p}{2} + \left( \int_{\sigma_{int}}^{\sigma_{int}} \left[ \varepsilon_{int} - \varepsilon_{int}^p \right] d\varepsilon_{int}^p \right) \cdot U_{int}$$
with the relation

$$\Delta \gamma_{xy}^p = \frac{\tau_{xy,max}}{G(T(\tau_{xy} = \tau_{xy,max}))}$$  \hspace{1cm} (28)

analogous to Eqn. (24) and considering the whole loop for the mode II contribution as stated above in Eqn (16). Again the increments of the plastic shear strain $\Delta \gamma_{xy}^p$ as well as the bracketed expression in the integral of Eqn. (27) have to be calculated using a relation that evaluates the associated material parameter at the corresponding temperature:

$$\Delta \gamma_{xy}^p = \frac{\tau_{xy}}{G(T(\gamma_{xy}^p = \gamma_{xy}^p)))}$$  \hspace{1cm} (29)

The relations for the shielding function $U_{\text{eff}}$ and for the reduced effective shear strain remain unchanged as given in the Eqns. (17) and (18). This is equivalent to the assumption that the material parameters used in these equations are not depending on the temperature. Due to the minor impact of the mode II contribution a temperature dependence of these terms would have no significant effect on the solution. The set of equations given in this section allows to calculate the components $\Delta J_{\text{eff}}$ and $\Delta J_{\text{int}}$ of the effective cyclic $J$-integral. All effects caused by the thermal cyclic loading are incorporated into these values. The fatigue life calculation then follows from integration of the Paris type crack propagation Equation (1)

$$N = \frac{a_k^{\text{in}} - a_k^{\text{out}}}{(1 - m) C_e(T) \left( \frac{\Delta J_{\text{int}}}{a} \right)^m}.$$  \hspace{1cm} (30)

Solving the above equation for $a_k$ and inserting the cycle numbers $N$ allows to calculate the crack propagation life.

5. Results and discussion

As described in the previous sections the first step of the presented method is to generate the input data for the damage assessment routine via FEM analyses. The material parameters are obtained from [17] and [18]. These parameters are: Young’s Modulus $E$, the Poisson Ratio $\nu$, the secant coefficient of thermal expansion $\alpha$, the mass density $\rho$, the thermal conductivity $k$ and the specific heat capacity $c$.

![Figure 3: Predicted crack propagation over cycles](image-url)
In addition the heat transfer coefficient is specified to vary with the temperature for each temperature transient individually. The heat transfer coefficient is significant for the temperature of the inner wall of the tube. Since the calculated fatigue life is a result of not only stresses and strains but also of the temperature distribution an a priori estimation and comparison of transients cannot be transferred to a prediction of the fatigue life.

The proposed damage assessment method uses several additional material parameters that cannot be obtained from [17] or [18]. Wherever necessary these additional material parameters are taken from [16, 19]. These additional parameters are: Sensitivity to mean stress $A_{\text{mean}}$, the activation shear stress $\Delta \tau_{\text{act}}$, stress to completely separate the crack flanks $\sigma_{\text{m}}$, the constant for the crack growth equation $C$, the exponent for the crack growth equation $m$, the coefficient for the Ramberg-Osgood equation $K'$ and the exponent for the Ramberg-Osgood equation $n'$.

As mentioned before the initial crack length $a_0$ is determined by the backwards integration of the Paris crack growth equation. The values of $\sigma'_{t}$ and $R_{n2}$ can be determined using $K'$ and $n'$.

All analyses are run within a stand-alone FORTRAN program thus decoupling the damage assessment from the data generation. For the calculated fatigue lives no modifications for statistical safety were taken into account.

Figure 3 depicts the predicted crack lengths over load cycles for the given temperature transients. Clearly the crack growth rate increases rapidly as the crack grows longer.

6. Summary and Conclusions

A new method for the assessment of thermo-mechanical fatigue in NPP components has been proposed. This method is based on the widely approved local strain approach [7, 9, 22] and embedded into the integrated AREVA Fatigue Concept (AFC). The new method implies the interpretation of the abstract notion of a fatigue usage factor as the propagation of physically short cracks to an engineering crack size of about 0.5-3.0 millimeters. Alternatively, fatigue curves of unnotched polished standard specimens resulting from strain controlled tests usually describe this limiting damage criterion. The design fatigue curves of the applicable design codes [4–6] represent these types of tests with additional applicable margins. The phase of technical crack initiation to an engineering crack size is now alternatively described by means of short crack fracture mechanics. The main advantages of this approach are listed below:

- Realistic description of the fatigue damage process,
- Determination of more accurate usage factors,
- Additional consideration of the influence of surface roughness by a fracture mechanical interpretation as an initial damage (crack),
- Implicit consideration of mean stress effects by simulation of the crack closure behavior,
- Potential consideration of size and gradient effects,
- Realistic interpretation of load sequence effects by consideration of the transient crack closure behavior under variable amplitude loading and
- Evaluation of NDT findings on the basis of experimentally approved crack propagation laws.

The last three points have not been explicitly addressed here but clearly show the potential of the chosen approach. They are the topic of further scientific investigations.

The combination of concepts applied in practice and new cognitions gained from the research on non-proportional and multiaxial loading offers the opportunity to new and groundbreaking methods of damage assessment for thermo-mechanical fatigue phenomena.

However, further investigations on the behavior of the material under thermal cyclic loading and the applicability of the approximations are necessary to validate and further refine several parts of the proposed method.

References


