

# TMF Life Prediction of High Temperature Components Made of Cast Iron HiSiMo: Part II: Multiaxial Implementation and Component Assessment

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## ABSTRACT

HiSiMo cast irons are frequently used as material for high temperature components in engines as e.g. exhaust manifolds and turbo chargers. These components must withstand severe cyclic mechanical and thermal loads throughout their life cycle. The combination of thermal transients with mechanical load cycles results in a complex evolution of damage, leading to thermomechanical fatigue (TMF) of the material and, after a certain number of loading cycles, to failure of the component. In Part I of the paper, a fracture mechanics model for TMF life prediction was developed based on results of uniaxial tests. In this paper (Part II), the model is formulated for three-dimensional stress states, so that it can be applied in a post-processing step of a finite-element analysis. To obtain reliable stresses and (time dependent plastic) strains in the finite-element calculation, a time and temperature dependent plasticity model is applied which takes non-linear kinematic hardening into account. The material properties of the model are identified from the results of the uniaxial test. The plasticity model and the TMF life model are applied to assess the lifetime of an exhaust manifold.

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## INTRODUCTION

High temperature components in combustion engines and exhaust systems must withstand severe mechanical and thermal cyclic loads throughout their life cycle. Inhomogeneous temperature distributions during thermal cycling and the interaction with neighboring components constrain the thermal expansion and contraction of the used material, resulting in high local stresses and inelastic strains. The combination of thermal transients with mechanical load cycles results in a complex evolution of damage, leading to thermomechanical fatigue (TMF) of the material and, after a certain number of loading cycles, to failure of the component.

Numerous expensive and time-consuming dynamometer tests are necessary to find the appropriate design that ensures the reliability of the component for the intended product life.

Hence, there is a need for material and damage models that allow the calculation of the lifetime of the component at the design stage. With these models, the components could be optimized via computer simulations and the number of bench tests could be minimized.

Computational methods for TMF life prediction comprise three main steps: In the first step, the transient temperature fields in the components are computed based on computational fluid dynamics calculations (CFD) and measurements. Secondly, the transient temperature fields are prescribed in finite element calculations to compute the transient stress and strain fields. In the third step, the computed local stresses and strains enter a lifetime model to predict the fatigue life of the component.

To obtain reliable stress and strain fields in the simulations, constitutive equations are needed for the description of time and temperature dependent cyclic plasticity. Commercial finite element programs as ABAQUS and ANSYS offer basic viscoplastic models, as e.g. the two-layer viscoplasticity model.

and the Perzyna model. Further improved models, as e.g. the models due to Chaboche [1, 2], can be implemented in the commercial programs as user subroutines.

Reliable lifetime models for TMF problems are rare. Purely phenomenological lifetime models, as e.g. the Manson-Coffin relation, the Smith-Watson-Topper parameter or the Ostergren parameter, cannot reproduce the dependency of damage on the stress-temperature history appropriately. The well-known Sehitoglu model [3] incorporates damage accumulation due to fatigue, oxidation and creep. However it uses a purely phenomenological model for fatigue. Hence, there is a need for a reliable lifetime model that captures the actual damage mechanism, namely the growth of TMF cracks, and that are able to reproduce the dependency of crack growth rates on cycle times, strain amplitudes, mean stress and temperature [4, 5, 6].

A starting point for a mechanism-based model is the description of crack growth under cyclic loading with fracture mechanics load parameters [7, 8]. The advantage of these models is that they contain a relatively small number of adjustable parameters and that the dependency on multi-axial stress states can be derived.

In a foregoing paper (Part I), a mechanism-based model for TMF life prediction is developed based on experimental results of low-cycle fatigue (LCF) tests at various temperatures and TMF tests with the ferritic cast iron HiSiMo. In the model, fracture mechanics concepts are applied in order to describe fatigue crack growth and to take into account the brittle fracture behavior of the material at 400 °C as observed in the experiments. The newly developed model can predict the LCF as well as TMF lives very well. However, the model is formulated for uniaxial loading conditions which are present in the LCF and TMF tested specimens. Thus, in this paper (Part II), the model is extended to three-dimensional stress states and applied together with a time and temperature dependent plasticity model to predict fatigue lives of an exhaust manifold.

## MULTIAXIAL MECHANISM-BASED MODEL FOR TMF LIFE PREDICTION

In the mechanism-based model for TMF life prediction, the number of cycles to failure  $N_f$  is estimated from

$$N_f = C \ln \frac{a_f}{a_0} D_{TMF}^{-B}. \quad (1)$$

$a_0$  and  $a_f$  are the initial crack length and the crack length at fracture, respectively.  $C$  and  $B$  are material parameters determined on the basis of experimental results. The damage parameter  $D_{TMF}$  is a measure of the damaging effect of a given arbitrary non-isothermal loading cycle, already presented in Part I for uniaxial stress states.

For the general case of multiaxial non-proportional loading conditions, most likely to occur in complex loaded components as exhaust manifolds, fracture mechanics results do not exist. However, for the multiaxial and proportional loading case, fracture mechanics results for small cracks and Ramberg-Osgood hardening materials [1] do exist. Using these results, the multiaxial version of the damage parameter can be written as,

$$D_{TMF} = \left( 1.45 \frac{\Delta \sigma_{I,eff}^2}{\sigma_{CY} E} + \frac{2.4}{\sqrt{1+3n'}} \frac{\Delta \sigma_I^2 \Delta \varepsilon_{eq}^p}{\sigma_{CY} \Delta \sigma_{eq}} \right) F(\sigma, T). \quad (2)$$

The material properties are Young's modulus  $E$ , cyclic yield stress  $\sigma_{CY}$  (defined as 0.2 %-offset stress) and Ramberg-Osgood hardening exponent  $n'$  of the stress-stress hysteresis loop.  $\Delta \varepsilon_{eq}$  is the von Mises equivalent value of the range of the (visco-)plastic strain tensor.  $\Delta \sigma_I$  is the maximum principal value of the stress range tensor and  $\Delta \sigma_{eq}$  its von Mises equivalent value. The subscript 'eff' indicates that crack closure is taken into account via the effective stress range [10]

$$\Delta \sigma_{I,eff} = \frac{3.72}{(3-R)^{1.74}} \Delta \sigma_I. \quad (3)$$

$R$  is the ratio of minimum stress to maximum stress in the loading cycle with respect to the principal direction of the stress range  $\Delta \sigma_I$ .

Creep effects are described by the function  $F$ , so that in the multiaxial formulation the von Mises value of the stress range tensor is used:

$$F = \left( 1 + \alpha e^{\frac{Q_{cr}}{RT_{ref}}} \int_{t_0}^{t_1} \sigma_{CY}^{m-1} \delta \sigma_{eq} e^{\frac{Q_{cr}}{RT}} dt \right)^{\frac{1}{m}}. \quad (4)$$

The symbol  $\delta$  refers to the corresponding range of stress with respect to the point of load reversal.  $m$  is the stress exponent,  $Q_{cr}$  is the activation energy for creep and  $\alpha$  is related to the coefficient of Norton's creep law.

To account for non-proportional loading conditions, the fracture mechanics-based multiaxial version of  $D_{TMF}$  in equation (2) is reformulated using the critical plane approach. To this end, the principal value of the stress range tensor is replaced by the stress range with respect to the critical plane (CP):

$$\Delta \sigma_{CP} = n_i \Delta \sigma_{ij} n_j. \quad (5)$$

$n_i$  is the normal to the critical plane and  $\Delta\sigma_{ij}$  is the stress range tensor. One obtains the multiaxial version of the damage parameter

$$D_{TMF} = \left( 1.45 \frac{\Delta\sigma_{CP,eff}^2}{\sigma_{CY} E} + \frac{2.4}{\sqrt{1+3n}} \frac{\Delta\sigma_{CP}^2 \Delta\epsilon_{eq}^p}{\sigma_{CY} \Delta\sigma_{eq}} \right) F(\sigma, T). \quad (6)$$

To account for brittle fracture observed for HiSiMo at 400 °C, the crack length at fracture for the fatigue life prediction with [equation \(1\)](#) is computed from

$$a_f = f_T a_{f,brittle} + (1 - f_T) a_{f,technical} \quad \text{with} \quad f_T = e^{\frac{1}{2} \left( \frac{T - 400^\circ C}{250^\circ C} \right)}. \quad (7)$$

$a_{f,technical}$  is the length of a technical crack that defines failure in cases where  $a_{f,brittle} > a_{f,technical}$ . For fatigue life prediction, the smallest value of  $a_f$  in the loading cycle is used. The crack length  $a_{f,brittle}$  at which slip induced brittle fracture occurs is estimated using elastic-plastic fracture mechanics and derived in Part I. Thus, only the result is given here:

$$a_{f,brittle} = \frac{0.002 (\sigma_Y)^{\frac{1}{n}} I_n x_c}{J_D} \left( \frac{\sigma_f^*}{\tilde{\sigma}_{\phi\phi}} \right)^{\frac{n+1}{n}}. \quad (8)$$

$\sigma_Y$  is the 0.2 %-offset stress and  $n$  is the Ramberg-Osgood hardening exponent of the monotonic stress-strain curve.  $x_c$  is the distance ahead of the crack tip where the cleavage fracture stress  $\sigma_f^*$  is reached.  $I_n$  and  $\tilde{\sigma}_{\phi\phi}$  with the superposed tilde are quantities depending on  $n$  and are available in tabular form. To express  $J_D$  for multiaxial loading conditions, again existing fracture mechanics solutions for proportional loadings are used [1] and reformulated, analogously to [equation \(6\)](#), in terms of the critical plane approach:

$$J_D = 1.45 \frac{\sigma_{CP}^2}{E} + \frac{2.4}{\sqrt{1+3n}} \frac{\sigma_{CP}^2 \epsilon_{eq}^p}{\sigma_{eq}}. \quad (9)$$

Same meanings as in [equation \(6\)](#) hold for the subscripts.

Thus, the fatigue life according to [equation \(1\)](#) is not evaluated in a single maximization procedure as in the case of the uniaxial model. Rather for multiaxial stress states, the fatigue life is evaluated in a double maximization procedure in which the two time points  $t_0$  and  $t_1$  within the loading cycle and therein the critical plane are found which result in the maximal value of the product  $\ln(a/a_0) \cdot D_{TMF}$  (i.e. in the minimal value of the fatigue life). The determination of the material properties of the  $D_{TMF}$  model with brittle fracture criterion for HiSiMo is presented in Part I.

## MULTIAXIAL TIME AND TEMPERATURE DEPENDENT PLASTICITY MODEL

In order to apply the multiaxial  $D_{TMF}$  model for the assessment of the fatigue life of components, a constitutive model is required to reliably compute stresses and plastic strains. The constitutive model should consider two major aspects. First, cyclic loadings require the application of kinematic hardening laws. They allow a proper description of the Bauschinger effect observed in stress-strain hysteresis loops. Second, materials behave time dependent at higher temperatures so that viscoplastic models are necessary to describe phenomena as strain-rate dependency, creep, relaxation and recovery of hardening.

Both aspects are considered by models due to Chaboche [1, 2]. However, a Chaboche model with static recovery is not available in the commercial finite-element programs ABAQUS and ANSYS. Thus, besides a Chaboche-type model the two-layer viscoplasticity model is used in this work which is available in ABAQUS. Both models are described in the following sections. Then, the determination of the material properties is addressed.

### Chaboche Model

In the Chaboche model, the stresses are computed from

$$\sigma_{ij} = C_{ijkl} (\epsilon_{kl} - \epsilon_{kl}^{th} - \epsilon_{kl}^{vp}), \quad (10)$$

where  $C_{ijkl}$  is the isotropic elasticity tensor depending on the temperature dependent elastic properties Young's modulus and Poisson's ratio. The thermal strain is obtained from the thermal expansion coefficient  $\alpha^{th}$  and the temperature change with respect to a reference temperature

$$\epsilon_{ij}^{th} = \alpha^{th} (T - T_{ref}) \delta_{ij}. \quad (11)$$

$\delta_{ij}$  is the second order identity tensor. The viscoplastic strains are calculated by integration of the flow rule

$$\dot{\epsilon}_{ij}^{vp} = \frac{3}{2} \dot{p} \frac{\beta_{ij}}{\beta_{eq}}. \quad (12)$$

$\beta_{ij} = \sigma'_{ij} - \alpha_{ij}$  is the relative stress where  $\sigma'_{ij}$  denotes the deviator of the stress tensor and  $\alpha_{ij}$  is the backstress tensor.  $\beta_{eq}$  is the von Mises equivalent value. The equivalent viscoplastic strain rate is represented by the power law

$$\dot{p} = \left\langle \frac{\phi}{K} \right\rangle^n \quad \text{with} \quad \phi = \beta_{eq} - \sigma_{Y0}. \quad (13)$$

$\phi$  is the yield criterion and  $\langle x \rangle = 0.5 \cdot (x + |x|)$ . The viscous material properties are described by the temperature dependent material properties  $K$  and  $n$ .  $\sigma_{y0}$  is the initial yield stress.

The backstress tensor describing kinematic hardening is the sum of two parts each following the evolution equation

$$\dot{\alpha}_{ij}^{(k)} = \frac{2}{3} C^{(k)} \dot{\varepsilon}_{ij}^{vp} - \gamma^{(k)} \dot{p} \alpha_{ij}^{(k)} - R^{(k)} \alpha_{ij}^{(k)} + \frac{\partial C^{(k)}}{\partial T} \frac{1}{C^{(k)}} \dot{T} \alpha_{ij}^{(k)}. \quad (14)$$

The first two terms are the non-linear Frederick & Armstrong kinematic hardening law [11]. Chaboche [1, 2] added the static recovery term (third term) to account for thermally activated climb of dislocations at higher temperatures. The temperature rate term (fourth term) stems from thermodynamic considerations. Cyclic hardening/softening is not considered since it is not observed in the complex LCF (CLCF) and TMF tests described in Part I.  $C^{(k)}$ ,  $\gamma^{(k)}$  and  $R^{(k)}$  are temperature dependent material properties quantifying hardening.

### Two-Layer Viscoplasticity Model

In the two-layer viscoplasticity (2LVP) model, the stresses are additively computed from the stresses in the viscous and the plastic layer:

$$\sigma_{ij} = \sigma_{ij}^p + \sigma_{ij}^v \text{ with } \sigma_{ij}^p = C_{ijkl}^p (\varepsilon_{kl} - \varepsilon_{kl}^{th} - \varepsilon_{kl}^p) \text{ and } \sigma_{ij}^v = C_{ijkl}^v (\varepsilon_{kl} - \varepsilon_{kl}^{th} - \varepsilon_{kl}^v). \quad (15)$$

The isotropic elasticity tensors  $C_{ijkl}^p$  and  $C_{ijkl}^v$  describe the elastic properties of each layer, respectively. The corresponding Young's moduli are related via

$$f = \frac{E^v}{E^v + E^p}. \quad (16)$$

The parameter  $f$  is determined from experimental data.

The thermal strains are described by [equation \(11\)](#). The plastic strains are computed from the flow rule in analogy to [equation \(12\)](#), where in this case the equivalent plastic strain rate is determined from the consistency condition  $\phi = 0$ . In the plastic layer, two non-linear Frederick & Armstrong kinematic hardening laws are considered, i.e. [equation \(14\)](#) applies with  $R^{(k)} = 0$ . Static recovery is not implemented in the 2LVP model in ABAQUS.

The viscous strains in [equation \(15\)](#) are determined by Norton's law:

$$\dot{\varepsilon}_{ij}^v = \frac{3}{2} A (\sigma_{eq})^n \frac{\sigma'_{ij}}{\sigma_{eq}}. \quad (17)$$

$\sigma_{eq}$  is the von Mises equivalent stress.  $A$  and  $n$  are the temperature dependent material properties quantifying the viscous material behavior.

The viscoplastic strains of the 2LVP model are obtained from

$$\varepsilon_{ij}^{vp} = f \varepsilon_{ij}^v + (1 - f) \varepsilon_{ij}^p \quad (18)$$

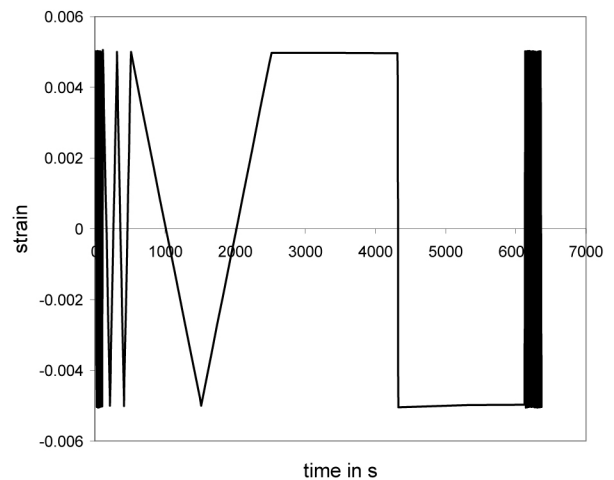
With this relationship, the accumulated viscoplastic strains (also known as equivalent viscoplastic strains) can be computed by integration of the equivalent strain rate

$$\dot{p} = \sqrt{\frac{2}{3} \dot{\varepsilon}_{ij}^{vp} \dot{\varepsilon}_{ij}^{vp}}. \quad (19)$$

### Determination of the Material Properties

The viscosity and plasticity properties describing HiSiMo ferritic cast iron are determined on the basis of the non-periodic parts of the CLCF tests at 20, 200, 400, 500, 600, 700 and 800 °C. To this end, analytical and numerical methods for parameter identification are used [12]. The material properties of both material models, namely the Chaboche model that considers static recovery and the 2LVP model, are determined. While the 2LVP model is available in ABAQUS, the Chaboche model is implemented as user-defined material subroutine UMAT in ABAQUS [11].

[Figure 1](#), [Figure 2](#), and [Figure 3](#) show examples of the calibration of both the Chaboche and the 2LVP models for selected CLCF tests at 20, 500 and 800 °C. In these figures, the same normalized stress scale is used.



a)

Figure 1. Non-periodic part of CLCF test at 20 °C: a) prescribed strain-history; b) measured (symbols) and calculated (lines) stresses; c) corresponding stress-strain hysteresis loops