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# Oxidation assisted fatigue crack growth under complex non-isothermal loading conditions in a nickel base superalloy

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ABSTRACT

This paper deals with the prediction of fatigue crack growth at high temperatures in the N18 nickel base superalloy, which is employed by Snecma for turbine disc applications. This material and other nickel base superalloys were widely studied in the past under isothermal conditions and constant amplitude fatigue. Dwell time effects are observed which are attributed, in this material, to grain boundary oxidation. The main objective of this research is to use this knowledge to model the fatigue crack growth rate in the N18 nickel base superalloy when complex "missions" are encountered. This implies variable amplitude and non-isothermal loading conditions (450–650 °C). For this purpose, an incremental fatigue crack growth model which was originally developed for isothermal variable amplitude loading conditions was extended so as to be applicable to non-isothermal loading conditions. In addition, the incremental form of the fatigue crack growth law in this model is very useful to account for the coupling effect between fatigue and time-dependent phenomena such as creep or oxidation. In the present case, the effect of the environment was modelled as a competition between two phenomena: a detrimental effect of grain boundary oxidation ahead of the crack tip and a beneficial effect of the growth of a passivation layer of oxides on the freshly created crack surfaces. The model was used to simulate fatigue crack growth under complex cycles at high temperature and the comparisons with experimental results are satisfactory.



## 1. Introduction

The aircraft industry faces the problem of predicting fatigue fracture of their aircraft engines parts under variable loads, variable thermal conditions and detrimental environmental conditions. An accurate prediction of the fatigue crack growth rate under complex loading conditions is useful to improve the inspection intervals of industrial components. However, predicting fatigue crack growth in metals under realistic loading schemes remains difficult because the fatigue crack growth rate is very sensitive to load history. As a matter of fact, constrained plasticity at crack tip has been known for decades to induce history effects in fatigue crack growth [1–7] and these effects are closely related to the cyclic elastic–plastic behaviour of the material [8,9]. Therefore, a strategy was proposed to identify a global history-dependent fatigue crack growth model, using finite element computations that include a cyclic elastic–plastic constitutive model identified for each material studied [10–12]. Once identified, this model consists finally of a set of about ten scalar derivative equations, which therefore allows the computation of typically a million variable amplitude fatigue cycles within a minute. In this model, the crack growth rate is a time derivative equation $da/dt$. This formulation explicitly avoids the need of a cycle counting method [10,11]. In addition, it facilitates the writing of coupled multi-physic problems, such as oxidation assisted fatigue crack growth and non-isothermal fatigue. This model was identified and validated at room temperature for a low carbon steel using constant and variable amplitude loading fatigue crack growth experiments [12].

The aim of the present research is to examine how that method can be extended to consider oxidation assisted fatigue crack growth in a nickel base superalloy when variable amplitude and non-isothermal loading conditions are encountered. In the following, the method employed to build the model is briefly recalled and then the application to fatigue crack growth under variable amplitude loading and non-isothermal conditions in the N18 nickel base superalloy is discussed.

## 2. Multiscale strategy

Let us briefly summarize what has already been done for mode I fatigue crack growth under variable amplitude loading conditions. The fatigue crack growth model is based on the assumption that the fatigue crack growth rate is a function of crack tip plasticity. First of all, a global measure $d\rho_I/dt$ of the rate of crack tip plasticity is introduced. Then the model is divided into two parts. The first

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**Nomenclature**

| | |
|---|---|
| CTG | crack tip region |
| $da/dt$ | rate of production of cracked area per unit length of the crack front. |
| $K_I^\infty$ | nominal applied stress intensity factor |
| $v(x,t)$ | velocity of the point $x$ in the CTG |
| $\underline{u}_I^e(\underline{x})$, $\underline{u}_I^c(\underline{x})$ | elastic and plastic reference spatial fields |
| $dK_I^*/dt$, $d\rho_I/dt$ | intensity factor rates of $\underline{u}_I^e(\underline{x})$ and $\underline{u}_I^c(\underline{x})$ |
| $b_c$, $a_c$ | size and displacement rate of the elastic domain of the CTG |
| $a_{xm}$ | displacement rate of the closure point versus $d\rho_I/dt$ |
| $\alpha$ | rate of production of cracked area per unit length of the crack front and per unit of $\rho_I$ at fixed time |
| $\beta$ | rate of production of cracked area per second at fixed $\rho_I$ in the absence of a passivation layer of oxides |
| $m$ | efficiency of the passivation layer |

part provides the fatigue crack growth rate $da/dt$ as a function of $d\rho_I/dt$ and is established using the results of fatigue crack growth experiments. The second part allows predicting $d\rho_I/dt$ according to the loading conditions and the values of a set of internal variables introduced to account for history effects. This second part is established using elastic–plastic finite element computations. Details about this model can be found in previous publications [10–12].

The second part of the model can be considered as a global cyclic elastic–plastic constitutive model for the crack tip region. A multiscale strategy was proposed in order to benefit on the one hand from the ability of the local FE model to account for detailed and complex material behaviours and on the other hand from the computational efficiency of global crack growth criteria. It assumes that the velocity field in the crack tip region is partitioned into elastic and plastic parts which are also assumed to be the product of an intensity factor and of a reference spatial field (Eq. (1)).

$$\underline{v}(\underline{x},t) \approx \frac{dK_I^*(t)}{dt}\underline{u}_I^e(\underline{x}) + \frac{d\rho_I(t)}{dt}\underline{u}_I^c(\underline{x}) \tag{1}$$

When the material behaviour is elastic, the intensity factor rate $dK_I^*/dt$ of the elastic field $\underline{u}_I^e(\underline{x})$ is equal to the nominal applied stress intensity factor rate $dK_I^\infty/dt$. When crack tip plasticity occurs, $dK_I^*/dt$ diverges slightly from $dK_I^\infty/dt$ Furthermore, the intensity factor rate of the plastic field $\underline{u}_I^c(\underline{x})$, denoted by $d\rho_I/dt$ becomes non negligible and is used as a global measure of plastic deformation within the crack tip region. Numerically, it is always possible to approximate a velocity field as it is done in Eq. (1), using a proper orthogonal decomposition such as the Karhunen–Loeve transform. In practice, $\underline{u}_I^e(\underline{x})$ is defined as the finite element solution of a 2D plane strain elastic problem with boundary conditions such that $K_I^\infty = 1\ MPa\sqrt{m}$. Then a 2D plane strain elastic–plastic finite element computation is performed, with $K_I^\infty$ increasing from zero to a maximum value chosen to be consistent with a fatigue application (i.e. a loading ramp from $K_I^\infty = 0$ up to $K_I^\infty = 40\ MPa\sqrt{m}$, for instance). At the end of the computation, $dK_I^*/dt$ is obtained by projecting the elastic–plastic numerical solution onto the reference elastic field $\underline{u}_I^e(\underline{x})$. The rest is partitioned into an intensity factor and a reference field using the Karhunen–Loeve transform. This allows the building of a numerical reference solution for $\underline{u}_I^c(\underline{x})$, which is non-dimensioned so that $/dt$ could also be read as the mean rate of the plastic part of the displacement between the crack faces in micrometers. Alternatively, an analytical expression for $\underline{u}_I^c(\underline{x})$ can also be used which is based on the theory of distributed dislocations [13]. In such a case, $\underline{u}_I^c(\underline{x})$ can be defined as the displacement field around an edge dislocation aligned with the crack plane and with a Bürgers vector equal to one. In both cases, once the reference fields $\underline{u}_I^e(\underline{x})$ and $\underline{u}_I^c(\underline{x})$ are known for a material, they can be used to extract $d\rho_I/dt$ as a function of $dK_I^\infty/dt$ from any complex elastic–plastic FE computation of a cracked body.

Once the reference fields $\underline{u}_I^e(\underline{x})$ and $\underline{u}_I^c(\underline{x})$ are defined, the approximation in Eq. (1) is performed at each step of a FE computation using an automated post-processing routine based on the least square method. The mean square error between the computed velocity field during the step $\underline{v}(x,t)$ and the approximated one $(dK_I^*/dt)\underline{u}_I^e(\underline{x}) + (d\rho_I/dt)\underline{u}_I^c(\underline{x})$ is also computed at each step of the FE computation. This error is always found to remain well below 10%.

The finite element method and the post-treatment routine are then employed to generate evolutions of $d\rho_I/dt$ under various loading cases, including cyclic loadings with either a stationary crack (Fig. 1) or a growing crack.

Then, a global empirical model, developed within the framework of dissipative processes, is associated to these evolutions and allows predicting $d\rho_I/dt$ as a function of $dK_I^\infty/dt$ and of a set of internal variables.

For instance, at each load's reversal (Fig. 1), it is observed that there is a domain within which no variation of $\rho_I$ is observed. Since $\rho_I$ is a measure of crack tip plasticity, this domain (E in Fig. 1) can be considered as an elastic domain for the crack tip region. If the material does not display any isotropic hardening, it was shown, using FE computations, that the size of the elastic domain (E) is constant in a $\rho_I, K_I^\infty$ diagram (Fig. 1). That size, $b_c$, can be identified for a given material behaviour using an automated post-treatment routine on the curves generated using FE computations (Fig. 1). If the stress intensity factor range remains below, then $d\rho_I/dt$ remains negligible.

If the stress intensity factor range exceeds $b_c$, the elastic domain (E in Fig. 1) moves. An internal variable $(\phi_{xc} = K_{Ixc}^2(1-\nu^2)/E)$ is therefore introduced to define the position of the centre of the global elastic domain of the crack tip region. Since the size of the elastic domain is constant, the relation between $\Delta K_I^\infty$ and $\Delta\rho_I$ allows the characterization of the displacement of the centre of the elastic

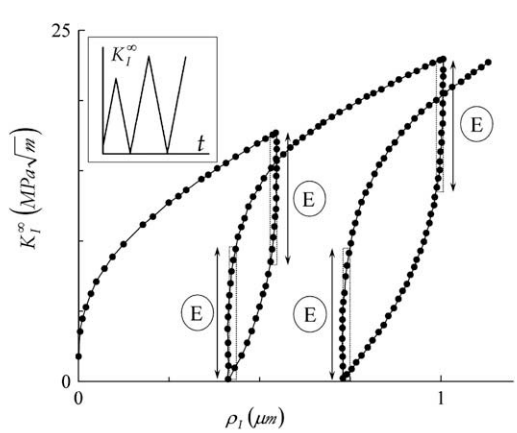


**Fig. 1.** The nominal stress intensity factor $K_I^\infty$ is plotted against the plastic strain intensity factor $\rho_I$. Within the domain (E), the variation of $\rho_I$ being negligible and the cracked structure is assumed to behave elastically.