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A simple numerical method of cycle jumps for cyclically loaded structures

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1. Introduction

Failures of structures subjected to cyclic loading are often influenced by a slow evolution of material properties. For example, over the lifetime of the structure, yield-strength or elastic modulus may change and eigen-strains may develop due to phase transformation. Factors such as thermal exposure (e.g. thermal barrier coatings [1–3]), humidity (e.g. fuel cells [4], silica glass [5]), and environmental attacks (e.g. corrosion [6–8], irradiation [9]) can cause a change in material properties. Design against failure for this class of structures is associated with many challenges, including determining the material properties as they evolve with time and how the stress state of the structure change within each load cycle and with long term exposure. Finite element analysis (FEA) is a convenient tool that readily gives a stress and strain distribution as a function of time when individual load cycles are simulated, and where material properties from experimental results can easily be incorporated, e.g., [10–12]. However, due to the intrinsic non-linearities associated with complex systems – such as non-linear, temperature dependent material properties – a single load cycle in FEA normally consist of many numerical increments and iterations. Thus, simulating each cycle a structure goes through before fatigue failure occurs is many times not feasible.

In this paper, we will focus on modeling aspects of structures with evolving properties subjected to cyclic loading. In particular, we will investigate a method of accelerated FEA, where not all individual cycles need to be modeled. We aim to develop a numerical technique that shortens the simulations, by utilizing the general concept of “cycle-jump”. This will eliminate the need of simulating each individual cycle and significantly reduce the need for extensive experimental investigations. The method assumes knowledge of the evolving properties. However, if these properties are not known, the model can instead be used for reverse analysis, i.e., determining properties from experimental observations.

Here, a class of structures with slowly evolving structural properties will be considered. An example of structure with such features relates to thermal barrier coatings discussed below. However, the model is not limited to thermal...
barrier coatings but can be used for any structure with evolving material properties or slowly changing boundary conditions.

The concept of cycle jumps has been investigated by several authors. The “Large Time Increments Method” (LATIN Method) was introduced in the early 1990’s by Ladevèze and co-workers [13,14]. This method separates the equations of the initial boundary value problem into two groups: (i) linear equations, global in space; and (ii) non-linear equations, which are local in space. Even though the theory behind the “LATIN method” is interesting, the implementation into commercial FEA software tends to be too cumbersome in its current form to be of practical interest. Fish and coworkers [15,16] have developed an alternative method for cycle jumps where the time is decomposed into two time scales: one micro-chronological (fast time scale) and one macro-chronological (slow time scale). Thus, the micro-chronological time corresponds to the cyclic behavior, and the macro-chronological to the overall trend of the structure. Even though the concept is sound, there are some challenges with implementing the method into commercial finite element programs. Kiewel et al. [17] developed a method to extrapolate the complete set of internal variables over a certain range of cycles.

The extrapolation is based on piecewise polynomial or spline functions that are formed for each integration point in the finite element model. As for the previous models mentioned, the method is customized for viscoplastic material. A simple cycle-jump scheme was proposed in [18] by Van Paepegem et al. based on extrapolation of the damage parameter by using the explicit Euler integration formula. This method performs automatic computations of the jump length. A local computation (at each integration point) is accomplished by imposing a user supplied maximum allowed increment/jump for the damage variable. The global jump length is then obtained as the abscissa value for which the cumulative statistical distribution of locally determined jump length reached a certain percent (10%) which is empirically provided by the user.

All of the above methods are based on some variant of damage theory, whereas our method is based on evolving properties. This could include damage theory but in this feasibility study, we study the effect of transformation strains, discussed below. The proposed method allows for both time and/or temperature dependent structural changes such as increasing elastic modulus due to high-temperature sintering; geometry changes due to oxidation, or permanent non-linear deformation; changing yield strength due to high temperature diffusion or eigen-strain due to phase transformations. Needless to say, the cyclic nature of this set of problems results in a completely different state than if the structure was subjected to a static load [10,19].

The typical evolution with time of either a primary or a secondary dependent variable (such as a displacement, strain or stress component) is shown in Fig. 1. As the structure is cyclically loaded (where “load” can be force, displacement, temperature or any other external parameter that will affect the state variables), the state variables will change slightly after each full cycle, Fig. 1. Thus, a local and a global change in the variable occur, where the local is the high frequency variation and the global corresponding to the general, long term trend. If the general trend can be expressed as a mathematical function, this function can be used to extrapolate the state variable. We will refer to this function as the “global evolution function.” Thus, utilizing the global evolution function, the long-term response of the structure can be determined, or at least approximated, without having to model the details of all cycles.

The overall goal of this work is to develop a method that can reduce the computational effort by performing cycle jumps along the global evolution of structural properties. To achieve this, our general approach is:

1. Conduct a set of cycles in FEA to establish the trend line, i.e., the global evolution function for each structural variable;
2. Extrapolate the state using the global evolution functions;
3. Impose the extrapolated state as the initial state for a new finite element analysis after the cycle jump.

A major challenge is to judge the success of the cycle jump. We will see later that convergence of the FEA is not guaranteed for a correct solution. Therefore, an extrapolation scheme including a “control function” is needed to enhance the accuracy of the calculation, controlling the length of the cycle jumps. This will be explored in the following.
2. Concept and modeling of cycle jumps

The system that has inspired us in this investigation, thermal barrier coatings, will be used to illustrate the concept of cycle jumps. However, the method developed can be applied to any system with evolving structural properties.

2.1. Example of a system with evolving material properties

In this section, we will describe some of the pertinent details of the materials systems considered: Thermal Barrier Coatings (TBCs), used in the hot part of gas turbine engines. Extensive reviews of these systems can be found for example in [20–23], and some of the key aspects of TBCs will be summarized in the following (as it pertains to this work).

TBCs enable higher operating temperatures, thus increased fuel efficiency. Through active internal cooling of the gas turbine blades and vanes, a thermal gradient is sustained over the TBC. The TBC consist of two layers (Fig. 2) deposited on the super alloy: (i) A metallic, aluminum rich, bond coat providing oxidation resistance; and (ii) a ceramic topcoat (typically yttria stabilized zirconia, YSZ) providing thermal protection. Depending on the application, each layer is typically 75–200 μm thick. The bond coat oxidizes during exposure to the high operating temperatures, forming a thin oxide scale (predominantly alumina, Al₂O₃) in the interface between the bond coat and the top coat. The thermally grown oxide (TGO) is initially less than 0.5 μm and grows up to 7–10 μm before failure. As the TGO grows – depleting the bond coat on aluminum – the structure evolves: interfacial cracks initiate, grow and coalesce, leading to final spallation of the coating. Moreover, due to the aluminum depletion, the properties in the bond coat changes with time. Thus, fatigue failures of TBCs are typically associated with thermal cycling, inelasticity (e.g., creep and plasticity), and material evolution, finally leading to spallation of the coating.

One particular failure evolution in TBCs is related to morphological instability of the TGO, characterized by local imperfections in the TGO that grow on a cyclic basis, eventually causing crack propagation in, and spallation of, the top coat, e.g., [1,19,24–28]. The morphological instabilities develop during thermal cycling and not during isothermal conditions. Thus, the cyclic response is critical to simulate. This failure mechanism is driven by a combination of three non-linear constitutive behaviors in the coating: (1) high temperature inelasticity in the TGO, (2) growth strain in the TGO, and (3) cyclic yielding in the bond coat. The growth strain is induced due to the oxidation process when the new alumina is formed. The high temperature inelastic strength of the TGO is often referred to as “growth stress.” The lateral component of the growth strain is limited by the growth stress and once the TGO stress reached the level of the growth stress, the lengthening strain is reallocated into thickening strain. The growth stress can be measured experimentally [29–31].

In this paper, we will use morphological instabilities as a sample problem since this failure mode is fairly well developed. However, the method we describe can be employed to any situation with evolving structure or properties.

2.2. Basic finite element model

In this feasibility study, we will investigate a two-dimensional finite element model, simulating a cylindrical geometry, Fig. 3A. The commercially available program ABAQUS [12] is used. The model is a variant of the model that was previously investigated by Karlsson and co-workers [10,19] to explore and explain morphological instabilities in TBCs. In this simplified model – designed to capture the necessary and sufficient parameters for morphological instabilities – only two of the coating’s layers are present: the TGO in shape of a center ring, surrounded by the bond coat. To reduce the model size, only 1/4 of the circular cross section is meshed, and appropriate boundary conditions are applied, as indicated in Fig. 3A. In the current simulations, we assume generalized plane strain elements. When the morphological instabilities are considered, the radial change of the TGO-bond coat interface is monitored.

As described above, a key feature in the failure of TBCs is the formation and growth of the TGO. Thus, it is critical to model this behavior in a satisfactory manner, while maintaining a tractable numerical scheme. Here, we adapt a method developed previously [19,24,25]. In this model, the TGO is considered linear-elastic, ideal-plastic at growth, with yield strength \( \sigma_{\text{TGO}}^{\text{Y}} \) corresponding to the growth stress. Even though the real mechanism in the TGO is creep, the simplification serves to accumulate inelastic strain during each cycle [24,25]. The growth strain in the TGO, \( \varepsilon_{\text{g}} \), is imposed as stress-free strain through the user subroutine UEXPAN [12]. The accumulation of growth strain is the component that drives this system. For simplicity, we will assume constant growth strain rate in each simulation.

![Fig. 2. A cross section of a thermal barrier coating before thermal exposures (courtesy Jin Yan, University of Delaware and Marion Bartsch, the German Aerospace Center).](image-url)
Fig. 3. (A) Finite element model used as a sample structure. State variables will be monitored along the line A–A. (B) Illustration of one load cycle. (C) Schematic of the general behavior of the Mises stresses in the bond coat during a cooling-heating cycle where $r = 0 \iff$ original inner TGO surface.

To maintain a tractable numerical scheme, we only assign evolving properties to the TGO in this feasibility study. However, as will be evident in the following, the method can incorporate any type of evolving material properties in multiple layers of the structure. Factors such as cyclic phase transformations [32] could also be incorporated.

Of particular interest is to explore the sensitivity for the heating-cooling sequence, which is approached in the following manner: The structure is initially stress-free at 1000 °C. Each thermal cycle consists of three parts: (1) cooling to 0 °C, (2) reheat to 1000 °C and (3) high temperature exposure at 1000 °C, where the TGO grows by
imposing the stress-free strain. The loading sequence is illustrated in Fig. 3B. The material properties used are summarized in Table 1. We note that the system is driven by the sequence of cooling–heating–growth strain. Each cooling-heating cycle resets the system, allowing more growth strain to be imposed at high temperature [10,19]. A system subjected to isothermal conditions with the same accumulated time at temperature will not see a significant radial increase. The typical structural response is visualized in Fig. 3C, where the stresses according to von Mises is shown for the bond coat at 0 °C and 1000 °C. The yielding during cooling results in residual stresses after reheating. The TGO stresses are closed to 1 GPa (not shown for simplicity).

2.3. Implementation of cycle jumps to FEM-model

The basic FE-model is created using ABQUS/CAE and ABAQUS Scripting Interface (an extension to “Python” programming language)[12]. A schematic of the implementation and interaction between the various routines used is shown in Fig. 4. A set of cycles (including all necessary incremental steps) is conducted in ABAQUS. ¹ Based on this solution, the “global evolution function” can be approximated. The “global evolution function” describes the extrapolation and the cycle jump is based on this function. All variables are extrapolated (stress, strain, displacement).

Since we cannot modify the information in the files containing the FEA results (e.g. the file “*.res”), the insertion of the extrapolated values into the FEA model by performing a “RESTART” command cannot be considered. Thus, we input the extrapolated values by utilizing the user-subroutine UMAT and prescribed displacements in user-subroutine DISP. The extrapolated displacements are applied at each node during the first step after extrapolation. These displacements are imposed incrementally (as if a displacement controlled loading was considered). During the last

¹ We note here that ABAQUS has a routine called “Direct Cyclic Analysis.” This method predicts the steady state condition for transient problems, where the stresses eventually will cycle between constant values. The method presented in this paper, deals with a problem with evolving stress field, where stresses and strains (in general) will continue to change throughout the life.

Table 1
Materials properties

<table>
<thead>
<tr>
<th>Layer</th>
<th>Behavior</th>
<th>E (GPa)</th>
<th>ν</th>
<th>σ_Y (MPa)⁰</th>
<th>Thermal expansion 10⁻⁶ × 1/°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bond coat</td>
<td>Linear elastic–ideal plastic</td>
<td>190</td>
<td>0.3</td>
<td>200</td>
<td>14</td>
</tr>
<tr>
<td>TGO</td>
<td>Linear elastic–ideal plastic</td>
<td>380</td>
<td>0.2</td>
<td>10,000; T  ≤ 900 °C</td>
<td>8</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>1000; T ≥ 1000 °C</td>
<td></td>
</tr>
</tbody>
</table>

⁰ The yield strength for the TGO varies linearly in between the two temperature intervals. This property distribution allows the TGO to respond inelastic at elevated temperatures but elastic at all other temperature intervals.

![Fig. 4. A schematic of the implementation of cycle jumps.](image-url)
increment in this step (at where the nodal displacements have reached the extrapolated displacements), the extrapolated stress and strain are imposed in the integration points. If a poor extrapolation was conducted, leading “too far away” from the equilibrium position, the analysis will fail to converge. This is the first indicator on how well the global evolution function extrapolated the cycles that were spanned. However, as will be seen later, convergence is not an indicator that a true solution is obtained. Indeed, an extrapolation scheme including a control function is needed to control the extrapolation function – both “slope” and “length.” The formulation and behavior of the control function will be discussed next.

2.4. Extrapolation scheme

We present here a simple approach for an extrapolation scheme with a “control function,” allowing the program to determine automatically a suitable length of the cycle jump. The method discussed here is most suitable for systems evolving in a quasi-linear manner. However, we will see that the extrapolation scheme will capture highly non-linear behavior of the evolving structure by automatically conducting shorter or no jumps. Thus, the extrapolation scheme compromises between the computational efficiency and the accuracy of the solution. The method described in the following will guide the program to automatically determine the length of the cycle jump, perform multiple jumps and – which will be seen in the result section – will stay close to the cycle-by-cycle solution. The control function developed here computes first the allowed jump-length for each variable in a control set at each integration point or/and node. The control set may contain all or a part of the variables involved in finite element analysis. The allowed jump length is computed based on an “accuracy control value,” which has to be specified by the user for each variable used in the control set. The minimum of all the allowed jump length values is selected as the common jump length and is used to perform the jump. Details of the procedures are described in the following.

Let us consider the method for computing the jump length for each variable extrapolated. Assume that a FE-based solution has been obtained such that it contains at least two, up to \( N_c \), computed loading cycles. For each variable, \( y \), of interest (e.g. components of stress, strain, displacement), \( y = y(t, \mathbf{M}) \), where \( t \) is the time and \( \mathbf{M} \) is any material point in the structure, at least three consecutive characteristic values are available. By characteristic values, we understand that those values are taken from the same relative position in time within the loading cycle.

For simplicity, we assume that the three values are extracted at the end of three consecutive cycles, and are defined by the points \( P_1(t_1, y_1), P_2(t_2, y_2), P_3(t_3, y_3) \), Fig. 1.\(^2\)\(^2\)

From these values, information pertaining to the global evolution of variable \( y \) can be extracted. The increments by which the variable \( y \) changed during the last two cycles are \( \Delta y(t_1) = y_1 - y_2 \) and \( \Delta y(t_2) = y_2 - y_3 \), respectively. Alternatively, expressed in terms of discrete slopes, we have \( s_{12}(t_1) = \Delta y(t_1)/\Delta t_{cycle} \) and \( s_{23}(t_2) = \Delta y(t_2)/\Delta t_{cycle} \) respectively, where \( \Delta t_{cycle} = t_1 - t_2 = t_2 - t_3 \) represents the cycle length.\(^3\)

The allowed jump length for each extrapolated parameter is dictated by the following criterion:

\[
\frac{|s_p(t_1 + \Delta y^M_{ly,\text{jump}}) - s_{12}(t_1)|}{|s_{12}(t_1)|} \leq q_y, \tag{1}
\]

where \( q_y \) is a relative error \((q_y > 0)\), \( \Delta y^M_{ly,\text{jump}} \) is the time spanned by the jump for material particle \( \mathbf{M} \), and \( s_p(t_1 + \Delta y^M_{ly,\text{jump}}) \) is the predicted slope at the moment after the jump, obtained by linear extrapolation as:

\[
s_p(t_1 + \Delta y^M_{ly,\text{jump}}) = s_{12}(t_1) + \frac{s_{12}(t_1) - s_{23}(t_2)}{\Delta t_{cycle}} \Delta y^M_{ly,\text{jump}}. \tag{2}
\]

Thus, the algorithm, by means of relation (1), ensures that the predicted slope at the time after the jump will be “close enough” to value of the slope before the attempted jump.

The value of \( q_y \) is a user specified input parameter and may be different for each variable in the control set. In our approach, we consider \( q_y \) as a constant, but it could also be a time dependent and automatically controlled parameter. The appropriate values of \( q_y \) can be obtained by comparing cycle-by-cycle analysis to a jump analysis for the initial portion of simulation (i.e. a reasonable number of cycles).

The value of allowed jump length is now easily obtained by substituting Eq. (2) into Eq. (1), and considering that the time-increments can have only positive values:

\[
\Delta y^M_{ly,\text{jump}} = q_y \Delta t_{cycle} \frac{|s_{12}(t_1)|}{|s_{12}(t_1) - s_{23}(t_2)|} \tag{3}
\]

So far, we have considered the computation of the allowed jump length for each variable in the control set, at each material point (integration point). However, the jump cycle length computed at each point will not be the same for all parameters considered. Since \( q_y \) is supplied by the user, the most simple and efficient way to determine the common jump length, \( \Delta t_{\text{jump}} \), is to set it as the minimum of the computed allowed jump length for each variable.\(^4\)

\[
\Delta t_{\text{jump}} = \Delta t_{cycle} \left| \min \{ \Delta y^M_{ly,\text{jump}} \} / \Delta t_{cycle} \right|, \tag{4}
\]

\(^{3}\) For simplicity, in examples presented in this work, the cycle length is considered constant although this is not a restriction for the method as long as the cycle length \( \Delta t_{cycle}(t) \) is much smaller than the life time of the structure.

\(^{4}\) We mention here that in [18] a statistical approach is used to calculate the global jump length once the local computation has been done. This might be seen redundant in the context of the criterion proposed in this paper.
where \([g]\) denotes the floor function (thus, returning the greatest integer less than or equal to \(g\)) and \(\Delta_{\text{jump}}\) is the global jump length.

Finally, the algorithm uses the Heun integrator to perform the extrapolation of all the variables necessary for the next finite element analysis (after the cycle jump):

\[
y(t_1 + \Delta_{\text{jump}}) = y(t_1) + \frac{1}{2}[s_{12}(t_1) + s_p(t_1 + \Delta_{\text{jump}})]\Delta_{\text{jump}}
\]

or, by substituting Eq. (2) (where now \(\Delta_{y,\text{jump}}\) is replaced by \(\Delta_{\text{jump}}\)) into (5), we get:

\[
y(t_1 + \Delta_{\text{jump}}) = y(t_1) + s_{12}(t_1)\Delta_{\text{jump}} + \left[s_{12}(t_1) - s_{23}(t_2)\right] \frac{\left(\Delta_{\text{jump}}\right)^2}{2\Delta_{\text{cycle}}},
\]

(6)

Higher order formulas can be used as well in order to perform the extrapolation, but they require more consecutive computed cycles by finite element analysis. In addition, the cycle length must be sufficiently small compared to the total time covered by the analysis, such that the approximation of the derivatives involved in describing the global evolution by using the per-cycle incremental information provided by finite element analysis to remain accurate.

The number of intermediate full cycles simulated by FEA (after one jump, before the next), \(N_{\text{FEA},j}\), is adjusted automatically based on the previously computed jump length. This adjustment is empirical and can be expressed as

\[
N_{\text{FEA},j} = N_{\text{FEA},j-1} + \Delta_+ \quad \text{if} \quad \left(\frac{\Delta_{\text{jump},j}}{\Delta_{\text{cycle}}}\right) \leq 1,
\]

\[
N_{\text{FEA},j} = N_{\text{FEA},j-1} - \Delta_- \quad \text{if} \quad \left(\frac{\Delta_{\text{jump},j}}{\Delta_{\text{cycle}}}\right) \geq N_{\text{FEA},j-1} - \Delta_- \geq N_{\text{min}},
\]

\[
N_{\text{FEA},j} = N_{\text{FEA},j-1}, \quad \text{otherwise}.
\]

(7)

where \(j\) is jump that was just performed. \(\Delta_+\) and \(\Delta_-\) are user supplied parameters, representing the increase and decrease, respectively, in the number of FE computed cycles, and \(N_{\text{min}}\) represents the minimum allowed \(N_{\text{FEA},j}\) (\(j = 1, 2, \ldots\)). This approach aims to minimize unnecessary jump computation when the global evolution is highly non-linear.

Having established the controlled cycle-jump procedure, it is now of interest to investigate how various factors might influence the extrapolation accuracy and the computational efficiency. These issues together with the importance of having a reasonable procedure for determining the jump length are addressed and presented in the following section.

3. Results

3.1. Reference simulation

Two reference simulations were conducted which include all individual cycle increments and steps required to reach the targeted time (or number of cycles). The reference calculations are used for comparison of the cycle jump simulations, to access the accuracy of the technique. None of the results from the reference simulations were used when conducting the cycle jump simulations.

In both simulations, 500 cycles (i.e., 1500 ABAQUS steps: one load cycle constituting three ABAQUS steps, Fig. 3B) were calculated. The two cases considered are “low growth strain rate,” \(\varepsilon_g = 10^{-4}\) and “high growth strain rate,” \(\varepsilon_g = 10^{-3}\), where the growth strain rate corresponds to the amount of growth strain applied during each cycle.

The lengthening and thickening component of the growth strain are assumed to equality, for simplicity. The eigenstrain imposed in the TGO drives the system, making the state parameters evolve with each cycle.

Consider the evolution of stresses and plastic strain in a material element (integration point) on the bond coat, close to the TGO, Fig. 5. For both low and high growth strain rate, the effective stress according to von Mises, “Mises stress,” starts at zero stress, reaches a maximum when the low temperature is reached (0°C), decreases as the structure is reheated (back to 1000°C), and increases when the growth strain is applied during the first cycle (Fig. 5A).

This is the local response for the structure, repeated for each cycle. However, the values of the maximum and minimum stresses change as the structure is cycled. This behavior corresponds to the global evolution of the structure. In particular, we see that the Mises stress increases on a cyclic basis until yield is reached after about seven cycles for the case of low growth strain rate (Fig. 5A). When high growth strain rate is considered, the material element reaches yield...
during the second cycle. For both cases, the stress at the lower temperature is constant after yielding has occurred, at 200 MPa corresponding to the yield strength of the material, whereas the stresses at the higher temperature continue to change slightly with each cycle.

The non-linear constitutive response can easiest be monitored by considering the plastic strain. To this end, we will monitor the equivalent plastic strain \( \varepsilon_{\text{eq}}^p \), for classic plasticity theory defined as

\[
\varepsilon_{\text{eq}}^p = \sqrt{\frac{2}{3} \varepsilon_{ij}^p \varepsilon_{ij}^p}
\]

where \( \varepsilon_{ij}^p \) is the equivalent plastic strain increment and \( \varepsilon_{ij}^p \) are the individual components. (The equivalent plastic strain should not be confused with current plastic strain which takes the sign of deformation into consideration [19]). For the case considered, the equivalent plastic strain accumulates with each cycle after yielding has occurred (Fig. 5B). Significant more equivalent plastic strain accumulates for the high growth strain rate.

The long term behavior is illustrated in Figs. 6–8, where the states after the high-temperature growth strain application steps are shown. Again, let us consider the evolution of the Mises stress and the equivalent plastic strain, but this time we will view it as a function of the radius along the 45° line (as indicated in Fig. 3). For low growth strain rate, Fig. 6A, a stress maximum is seen close to \( r = 10 \) μm. The location of maximum stress corresponds to the location of the plastic zone during the cooling step. The yield zone moves toward higher radii with each cycle (Fig. 6A and B). Thus, most of the structure remains elastic during the 500 cycles. The scenario for the high growth strain rate changes somewhat (Fig. 7A and B). In this case, the plastic zone moves rapidly towards the edge of bond coat as the structure is cycled, resulting in overall yielding after 283 cycles. After this time, overall yielding will occur at each cycle (at low temperature).

The development of the plastic zone directly influences the radial displacement of the boundaries, elucidated in Fig. 8. In this Figure, the evolution of the inner TGO surface and the TGO/BC interface are monitored as a function of time. For the case of low growth strain rate, the location
274 long cycle jump, six additional full cycles are conducted. (We will later see that complete cycles after a cycle jump tend to bring the solutions closer to the true solution.) Thus, 300 cycles are simulated with one cycle jump, and 20 initial and 6 final FEA calculated cycles. Comparing the results from the cycle jump to the reference calculation, Fig. 9, it is evident that the Mises stress is not correct for larger coordinates ($r > 32 \mu m$), and that the plastic strain in the bond coat is off by a factor of about three for all locations. Obviously, the cycle jump does not capture the larger region of yielding that has evolved in the bond coat. Thus, convergence after a cycle jump does not guarantee a true solution.

### 3.3. Cycle jumps with control

Next, we will discuss the results achieved when implementing the extrapolation scheme containing control function as described in Section 2. In all cases, 500 cycles are simulated and compared with the reference simulations. Tables 2 and 3 summarizes the simulations conducted for low and high growth strain rate, respectively. Here, the control set includes only the displacements components, $u_1$ and $u_2$, i.e., deformation in the (global) $x$- and $y$-direction, respectively. However, the set could contain any state variable, such as stress and/or strain tensors components.

Fig. 8. Radial displacement as a function of time for (A) low growth strain rate and (B) high growth strain rate.

Fig. 9. For high growth strain rate, an uncontrolled jump compared to the reference simulations: (A) Mises stress and (B) equivalent plastic strain; 300 cycles, one jump spanning 274 cycles. State shown after high temperature exposure (1000 °C), along the line A–A in Fig. 3. $r = 0 \iff$ original inner TGO surface.
about 300 cycles, a behavior observed earlier in Fig. 8. The

tions, but will move outwards (positive displacements) after

(negative displacements) during the first part of the simula­

tion. Fig. 10 shows that the surface moves inwards

shorter cycle jumps or no jumps during the mostly non-linear

transition induces a strong non-linear response. The cycle

jump technique captures this behavior well by conducting

jump technique are compared to the reference configuration

difference simulation.

where $\Delta_2 = 2$, $\Delta_1 = 1$.

The evolution of the structure during the simulation when

subjected to high growth strain rate is illustrated in Fig. 10,

where the radial component of the interface between the

TGO and the bond coat is monitored as a function of time

(Figs. 11 and 12). For $q_{u_1} = q_{u_2} = -2.0$, the solution

obtained from the jump simulation deviates from the refer­

cence simulation.

To quantify the computational efficiency from the cycle

jump technique, we introduce the ratio:

$$R = \frac{L_{\text{jumps}}}{L_{\text{tot}}}$$

(9)

where $L_{\text{jumps}}$ is the total time covered by the jumps and $L_{\text{tot}}$ is the total time considered for the analysis. Thus, the more efficient (e.g., longer jumps) a calculation is, the higher va­

Table 2 and 3 overlap the reference simulations except

the case of $q_{u_1} = q_{u_2} = -2.0$, thus we only display two cases in

Figs. 11 and 12. For $q_{u_1} = q_{u_2} = -2.0$ the solution

deviates from the reference simulation.

The accuracy, or errors, of the cycle jump simulations can be determined by comparing the results to the reference simulations. The errors can be quantified by considering the relative error of the state variables. The relative error, $\delta E$, is defined by

$$\delta E = \frac{|y_{\text{ref}} - y_{\text{jump}}|}{y_{\text{ref}}} \times 100 \%,$$

(10)

where $y_{\text{ref}}$ is the value of the state parameter at the integra­

tion point, and indexes ref and jump indicate reference
and cycle-jump based simulation, respectively. One way to represent the overall error of the jump model is to consider the average relative error,

$$\delta E = \frac{1}{N} \sum_{i=1}^{N} \delta E_i$$

and the standard deviation of the relative error

$$s = \sqrt{\frac{1}{N} \sum_{i=1}^{N} (\delta E_i - \delta E)^2}$$

where $N = 16207$ is the total number of integration points in the model. From Tables 2 and 3, we see that both the average and the standard deviation of the relative error increase for both Mises stresses and equivalent plastic strain, for both growth strain rates, as the control parameters increase.

A careful evaluation of Fig. 10 shows that the intermediate, complete cycles between the jumps tend to bring the solution back towards the results from the reference simulations. This suggests that conducting more intermediate simulations will in general increase the accuracy of the simulation. In this case, the accuracy does not compromise the computational efficiency, as seen in Tables 2 and 3.

At present, we have not verified the numerically achieved results experimentally. We assume that the solution obtained by the cycle-by-cycle finite element simulations is correct (within the context of the physical problem defined). Thus, when the cycle-jump scheme gives the same solution as the cycle-by-cycle solution, the cycle-jump technique developed here is considered correct.

### 3.4. Cycle jumps beyond the reference simulations

Last, we present results from a simulation beyond the reference simulation. For the case of low growth strain rate, a simulation corresponding to 7000 cycles was conducted.

In this case the control parameter values were set to $q_{u1} = q_{u2} = 0.25$ for the first 2000 cycles, then to $q_{u1} = q_{u2} = 0.15$ for the analysis segment between 2000 and 3500 cycles and to $q_{u1} = q_{u2} = 0.05$ for the final portion, between 3500 and 7000 cycles. By studying the equivalent plastic strain (Fig. 13B), it is seen that the structure...
slowly moves from elastic–plastic state, to experience overall yielding after about 3000 cycles. The evolution of the Mises stresses (Fig. 13A) concur with this, showing similar response as when the higher growth strain rate was used in which case overall yielding was observed. In this case, we cannot access the accuracy of the results, since no reference simulation exists.

The modification of control parameters was required since in a long analysis the selected values of the control parameters became too large, leading to inaccurate extrapolation at the end of very long cycle jumps. This situation is easy correctable in our extrapolation scheme. We selected to reduce the values of the control-parameters and restarting the analysis from a moment when the solution still was considered accurate. Alternatively, a maximum allowed jump length could be imposed. Both methods may be performed automatically by the code, analyzing the FEA response immediately after a jump. By studying the finite element solution in the first cycle after a jump, the jump accuracy validation can be performed and the jump computation procedure can be restarted with modified control parameters, this procedure being the subject of our in-progress work. We are currently extending the code to incorporate an automatic adjustment of the control parameters.

4. Concluding remarks

A method for accelerated numerical simulations of structures subjected to cyclic loading has been developed and is presented in this paper. Of particular interest is a class of structures where the properties evolve with time. As inspiration for the work, we have used a common failure evolution seen in thermal barrier coatings, referred to as morphological instabilities or “ratcheting,” even though the proposed method is applicable to a range of systems where the structural properties evolve, including incorporating damage parameters.

The method discussed and developed in this work is based on combining a cycle jump technique with finite element simulations. A key part of the technique is an extrapolation scheme, containing a control function, allowing the program to automatically determine a suitable length of the cycle jump, performing multiple jumps and ensuring that the solution is accurate. By user-input, the control function can be controlled through a parameter that defines an allowable error. Thus, the extrapolations scheme compromises between computational efficiency and accuracy of the solution. To visualize the accuracy of the proposed method, a reference simulation is conducted, containing all increments and steps. However, knowledge from this simulation is only used to verify the results.
The method discussed here is most suitable for systems evolving in a quasi-linear manner. However, it is seen that the extrapolation scheme will capture highly non-linear behavior of the evolving structure by automatically conducting shorter or no jumps. Thus, the method stays close to the solution obtained from the cycle-by-cycle simulation even with high non-linear evolution, however compromising the computational efficiency.

In all, we show that this relatively simple approach to accelerated numerical simulations, capturing the development and evolution of structures subjected to cyclic loading, can give reliable solutions while saving significant computational efforts.

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References