Overload effects on the fatigue crack propagation behaviour in Inconel 718

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Linköping, May 2012
Preface

This master thesis has been compiled during the spring of 2012 at the Division of Solid Mechanics, Linköping University. The research has been funded by the Swedish Energy Agency, Siemens Industrial Turbomachinery AB, Volvo Aero Corporation and the Royal Institute of Technology through the Swedish research program TURBO POWER, the support of which is gratefully acknowledged.

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Erik Lundström
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Abstract

In this master thesis, work done in the TURBO POWER project *High temperature fatigue crack propagation in nickel-based superalloys* during spring 2012 will be presented. The overall objective of this project is to develop and evaluate tools for designing against fatigue in gas turbine applications, with special focus on the crack propagation in the nickel-based superalloy Inconel 718. Experiments have been performed to study the effect of initial overloads, and it has been shown that even for small initial overloads a significant reduction of the crack growth rate is received. Furthermore, FE simulations have been carried out in order to describe the local stress state in front of the crack tip since it is believed to control, at least partly the diffusion of oxygen into the crack tip and thus also the hold time crack growth behaviour of the material. Finally, an evaluation method for the stresses is presented, where the results are averaged over an identifiable process/damaged zone in front of the crack tip.
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Gas turbines used in energy production processes or as aero engines operate at high temperatures, often where most materials are not suitable for usage. This is where the nickel based superalloys are used, to enable operation even in the hottest sections of a turbine. However, in the need of making gas turbines more efficient, increasing temperatures will lead to more severe conditions for the materials. This is important to handle, since e.g. an increased crack propagation rate will have a strong influence on the life of a component. In addition to this, future stationary gas turbines will operate at conditions where they may encounter more starts/stops due to the increasing amount of solar and wind power generation, which cannot always be trusted in terms of reliability of delivering power. This will probably lead to operating cycles which do not look like those found today. Thus, there is an interest with respect to both stationary turbines and aero engines to study the effect of overloads/underloads in combination with high temperature hold times and its impact on the fatigue crack growth behaviour.

Models for predicting fatigue crack propagation are not yet developed to the extent needed for use in specific applications involving hold times and high temperature, therefore the usual way to handle these problems are to,

- Reduce the temperature for better safety margins, thus a more conservative design will be the result.
- Decrease the service intervals for engine inspections and component replacements, resulting in higher maintenance costs and less operation time.

The need for understanding the fatigue crack propagation behaviour of different load cycles for these materials in this specific context is therefore of utter importance. However, the results for understanding and solving these problems will have several positive outcomes when increasing the turbine temperature and thus subjecting the turbine to higher loads: increased thermal efficiency, lower fuel consumption, lower operating cost and less pollution.

In the work done several mechanical tests have been performed to show the influence of different load cycles. More specifically, the case of an initial overload before a hold time has been studied. Further, FE simulations of the same initial overloads have been carried out, in order to understand the effect they have on the crack growth behaviour. Finally, several other mechanical tests and FE sim-
ulations with other evaluation methods have been performed. However, these are not presented here.

1.1 Applications

A gas turbine can be used for a number of applications; as a propulsion of a generator, as a compressor for oil/gas pipelines, as a power source for ship propellers or water jets or, as most known, as a power source for aircraft. The vast amount of air a gas turbine can utilise compared to a piston engine of the same size makes it produce 20 times more power per engine volume [1]. Thus, a gas turbine is an excellent choice when external power generation or other machinery is needed.

The gas turbine mainly consists of three parts; the compressor, the combustor and the turbine, see Figure 1. The compressor raises the pressure and temperature of the incoming air and sends it through to the combustor. In the combustion chamber, the pressurised air is mixed with fuel and ignited. The hot gases that are gained from this process are led into the turbine where rotational power is produced to provide propulsion for e.g. a generator in a stationary turbine, or to drive a fan in an aero engine. The turbine also provides the energy needed for the compressor after the startup sequence has ended. A typical application for the material in focus of this study, Inconel 718, is found in the turbine discs where high loads and temperatures are present.

![Figure 1: Gas turbine SGT-600 interior, courtesy of Siemens.](Figure 1: Gas turbine SGT-600 interior, courtesy of Siemens.)
Nickel-based superalloys

From their appearance in the 1940s, nickel-based superalloys have had an enormous impact on the development of gas turbine engines. With their unique high temperature performance capabilities, with operation temperatures up to 0.6T_{melt}, and resistance to mechanical degradation over time when considering hostile operation environments like sea weather (e.g. as a power source for an oil-drilling platform) and unclean fuels, they are the best choice when designing a gas turbine engine [2]. The choice of having nickel as the main base for superalloys is due to its stable FCC (face centered cubic) structure which is both ductile and tough. The stability of the FCC structure of nickel from room temperature up to its melting point of 1455°C, and the low diffusion rates makes it creep resistant, which is of major concern when considering high temperatures [2].

A nickel based superalloy mainly consist of four different phases [2–5],

- $\gamma$ which is the FCC matrix phase of the alloy. It does not undergo any phase transition during operation which would lead to poor high temperature properties. This phase mainly consists of Ni together with e.g. Fe, Cr, Co and Mo.

- $\gamma'$ is an ordered precipitate phase of FCC structure. The main components are Ni, Al, Ti, Nb, and Ta. In many alloys, it is the main strengthening component.

- $\gamma''$ is an ordered phase of BCT (body centered tetragonal) structure. In Ni Fe alloys like Inconel 718 (IN718) which contains Nb it is believed to be the main strengthening component. The phase is metastable and can in an overaged condition transform to $\delta$ phase, see below.

- $\delta$ is a nonhardening precipitate in grain boundaries used to increase creep resistance (prevent boundary sliding) and grain size control.

2.1 Inconel 718

As mentioned previously, the specific Ni-based superalloy considered here is Inconel 718 in forged form, which is one of the most common one used in turbine applications due to its relatively low cost and good operating conditions up to 650°C. The
basic weight composition of IN718 is shown below in Table 1 [2]. However, many other components may be added for strengthening the alloy.

Table 1: Weight composition for IN718

<table>
<thead>
<tr>
<th>wt%</th>
<th>Ni</th>
<th>Cr</th>
<th>Mo</th>
<th>Nb</th>
<th>Al</th>
<th>Ti</th>
<th>Fe</th>
<th>C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Balance</td>
<td>19.0</td>
<td>3.0</td>
<td>5.1</td>
<td>0.5</td>
<td>0.9</td>
<td>18.5</td>
<td>0.04</td>
<td></td>
</tr>
</tbody>
</table>
Fracture by fatigue is a major issue when dealing with critical components subjected to cyclic loading. A small crack may have been introduced at the manufacturing process, grown from a material defect or created by the accidental action of a remote force. This crack may start to grow when subjected to repeated cyclic loading, a behaviour controlled by a number of different factors, such as

- The stress intensity factor $K$
- The loading ratio $R$
- Temperature or environment conditions, especially in combination with hold times

The stress intensity factor $K$ is a measure of the crack tip condition. Furthermore, the range $K$ is defined as $\Delta K = K_{\text{max}} - K_{\text{min}}$. For uniaxial mode I loading of a cracked plate one gets the expression seen in Equation (1) below

$$\Delta K_I = \Delta \sigma_0 \sqrt{\pi a} \cdot f$$

(1)

where $\sigma_0$ is the remote stress, $a$ is the crack length and $f$ is a geometry factor.

An empirical equation frequently used to calculate the fatigue life is Paris’ law (2) below

$$\frac{da}{dN} = C \Delta K^m$$

(2)

where $C$ and $m$ are material constants determined by mechanical testing. However, this equation may have to be modified in order to handle the influence of crack closure $\Delta K_{\text{eff}} = K_{\text{max}} - K_{\text{open}}$ c.f. [6–9].

When introducing a hold time, a linear superposition model may be proposed to account for both the cyclic part and the hold time part, see Equation (3).}

$$\frac{da}{dN} = \left( \frac{da}{dN} \right)_{\text{cycle}} + \left( \frac{da}{dN} \right)_{\text{hold time}}$$

(3)
A model as such can incorporate the effect of various fracture mechanisms which may act during a hold time, such as creep deformation, oxidation and diffusion. A number of different models based upon this decomposition are found in the literature, c.f. [10] for a review.

Others have proposed models with a more physical approach, based on physical interpretation of metallurgical and mechanical observations c.f. [11, 12].

3.1 Crack propagation in Inconel 718

The loading conditions may have a considerable influence on the material behaviour. For instance, with sufficiently long hold times compared to pure cyclic loading, the crack growth switches from transgranular fracture to intergranular, increasing the crack growth rate several times c.f. [13]. This is due to grain boundary weakening which seems not yet fully understood. For the fracture mechanisms to take place in the grain boundaries there are mainly two dominating theories, namely, dynamic embrittlement (DE) and stress accelerated grain boundary oxidation (SAGBO). DE is based on grain boundary diffusion of oxygen (could be other gases as well) into the crack tip, where the embrittlement of the grain boundary takes place and allows the crack to advance into new material. In the fresh material oxygen diffuses into the grain boundary and the process starts over. SAGBO on the other hand assumes that the oxide at the grain boundaries crack, thus moving the crack onwards and exposing fresh material, for a more comprehensive description of the two processes see e.g. [14] or [15].

3.2 Damaged zone

During sufficiently long hold times a zone at and around the crack tip is created by the embrittling of the grain boundaries, by some kind of mechanism discussed above. This region consists of unbroken ligaments and islands of unbroken material which have been left over where the crack has propagated [15], and is here denoted as the damaged zone. At unloading followed by reloading, the damaged zone is reduced, and if the specimen is subjected to cyclic loading afterwards without hold times a substantial reduction of the crack growth rate is noted, as the crack now grows through less and less damaged material [3, 16]. It is to be noted that the PD method used for monitoring the crack growth during mechanical testing (see Chapter 4) only measures values representing the beginning of the damaged zone, the whole concept can be summarized in Figure 2.
3.3 Methods for reducing the crack growth rate in Inconel 718

Studies of the crack growth behaviour in IN718 during high temperature hold times have been made in different environments, involving crack propagation in air, vacuum as well as other gases [14]. It has been shown that the reduction of crack growth in vacuum is enormous, changing from intergranular to transgranular fracture mode, implying that oxygen is of major concern [14]. To reduce the crack growth rate in air, which is the atmosphere turbines operate in, a number of different methods are proposed. Many of them involve mixing additives to the alloy which strengthens the grain boundaries. Some of the suggestions are to add boron, carbon, hafnium or zirconium in small amounts to get either a lowering of the grain boundary energy or the diffusion rate in the grain boundaries [15], but also trace amounts of phosphorus and magnesium have been shown to increase the life [17]. Other factors which might increase life is the grain size; large grains increase the creep resistance, but in contrast small grains on the surface improve the crack growth rate.
initiation resistance [2]. A further suggestion is to elevate the temperature for a short time to get stress relaxation at the crack tip, thus reducing the tensile stresses in front of the crack tip [17]. Another factor which has been shown to have an effect on the rupture time is the load cycle shape with different overloads/underloads etc., such as present in the operation of gas turbines, see below.

### 3.4 Influence of different load cycles

A typical gas turbine operating cycle for an aero engine gives an idea of what loads it will be exposed to, see Figure 3. During operation, typically during take off to cruise speed and from cruise to landing, the gas turbine is subjected to different types of overloads/underloads, the same applies for a stationary gas turbine at start and stop, especially when considering new solar and wind powerplants. The engine rating seen in Figure 3 is a measure of what load the turbine can be exposed to under how long time [1], which can also be related to the turbine entry temperature (TET), these measurements can closely be related to what overloads/underloads that e.g. a turbine disc is exposed to. The overloads/underloads can have great impact on the fatigue life, due to stress redistributions at the crack tip which affects the diffusion of oxygen into the crack tip, c.f. [14, 15]. Two examples which have proved this are, partial unloadings before a hold time which has been seen to give a reduction of the crack growth rate [18], and overloads at the end of a hold time cycle followed by an unloading, which also reduced the crack propagation rate [19]. Thus there is a large interest in investigating the effect of different load cycles, since such a knowledge will be of value in the development of future gas turbines.

![Figure 3: Typical operating cycle for a gas turbine in a civil aircraft, here as engine rating or turbine entry temperature (TET) vs. time, see also [1, 2].](image-url)
4.1 Experimental procedure

Mechanical testing was performed under load control with a base load of 650 MPa for all testing using Kb-type specimens of IN718 with a rectangular cross section of 4.31 x 10.20 mm, see Figure 4. The material had a grain size of about 10 \(\mu m\) and was solution treated and aged according to AMS 5663 standard. One initial notch (depth 0.14 mm, width 0.32 mm) was introduced by an Electro Discharging Machine (EDM), see Figure 5. From this initial notch, a pre-crack was grown by a 10 Hz, R=0.05 cyclic load at room temperature, which gave a semicircular crack with a total depth of about 0.5 mm, thus assuming a=c in Figure 5.

The machine used for the mechanical testing was an MTS servo hydraulic machine with a load capacity of 160 kN, an Instron 8800 control system with the software WaveMaker for the load control and an MTS furnace with three temperature sensors (model 652.01/MTS with temperature control of model 409.81) [13, 16].
Elevated temperature tests were performed in air at 550°C (isothermal conditions), with different initial overloads but with a hold time of 2160 s in all cases, see Figure 6. Tests were performed for overloads of 2.5%, 5% and 15% and for a reference load with no overload for comparison purposes, all at $R=0.05$ and laboratory conditions (air pressure, humidity etc.). In addition to this a pure cyclic crack growth test was performed at 0.5 Hz, with the same conditions as mentioned above.

The crack advance was monitored by the Direct Current (DC) Potential Drop (PD) method [20], with a Matelect DCM-2 and a two channel pulsed DCPD system. The
PD method does not depend on visibility, thus it is well suited for high temperature tests in a closed furnace. The PD method uses two pairs of spot welded wires; a wire on each side of the crack and one pair of wires on the back acting as reference probes. The ratio between the two pair of wires was used to evaluate the crack length in order to avoid unnecessary oscillations in the results. Through the wires a current of 10 A was run to obtain PD values, which were fitted to an experimental calibration curve in order to receive the crack length [13, 16].

4.2 Evaluation

Data was recorded every two seconds to receive enough data points for accurate evaluation. However, the PD method has an accuracy of only 0.01 mm, therefore a continuous mean value evaluation of all the crack lengths was performed. This is done by taking all the identical crack lengths and identifying them to their unique cycle number, which will be a mean value of the cycles identified with the same crack length. For the evaluation process a Matlab code (which for this thesis has been slightly rewritten) was used to perform these evaluations and calculating the crack growth increments $da/dN$ and the stress intensity factor $\Delta K$. The handbook solution used for evaluating the mechanical tests is based on a semi-elliptic surface crack with the assumption that $c=a$ [21] thus giving a semi circular crack shape as mentioned previously. The evaluated data can then used to calibrate Paris’ law type expressions (4) for the crack propagation behaviour.

$$\frac{da}{dN} = C\Delta K^m$$

However, it is to be noted that a simple expression of the above type is only valid for a certain/specific overload type, overload size, R-value, temperature and hold time.
By the knowledge of the importance of a model for investigating the overload effect on critical components, a simulation work was initiated. It is believed that the stress normal to the crack plane in front of the crack tip influences the transportation and/or the detrimental effect of oxygen at the crack tip, and thus also the hold time crack growth behaviour. The commercial FE software Abaqus version 6.10 [22] and Ansys version 13 [23] have been used, using small deformation context and a direct full Newton method. However, in this thesis only Abaqus simulations will be presented. The purpose of the work was to investigate the stress normal to the crack plane in front of the crack tip, both by stationary and propagating cracks. The stationary crack will provide the principal results for the stresses, and the propagating crack with its plastic wake will provide for a more real fracture condition, as the material behind the crack tip has flown plastically. However, no hold time was applied as the influence of creep at the temperature studied (550°C) was assumed negligible.

5.1 Geometry and boundary conditions

Simulations were done on a 2D model representing a central crack in a rectangular plate under plane strain conditions, as this case was thought to provide a simple but yet relevant representation of the test specimen conditions, see Figure 7a. By using two symmetry planes only a quarter of the plate had to be simulated, thus reducing the simulation time considerably, see Figure 7b where also the boundary conditions of the model are shown. The plate was subjected to a load specified as a distributed load of 650 MPa $K_{\text{hold}}$ with R=0.05 according to the mechanical testing.

By investigating the handbook solution for the different $K_{\text{hold}}$ values for the different simulations, see Equation (5), a suitable crack length could be chosen by the knowledge of the damaged zone measured in [16] and that this $K_{\text{hold}}$ lies in range of what the mechanical testing has been performed in. More specifically, this gave $K=30 \text{ MPa}\sqrt{m}$ where the pure cyclic loading was initiated in [16], see also Chapter 3, which gave final crack length of 0.66 mm for all of the simulations.
5.2 Constitutive model

With a propagating crack, a plastic wake will as mentioned previously, be formed behind the crack tip, consisting of plastically deformed material. Therefore, an appropriate constitutive model has to be chosen in order to describe this behaviour in an accurate way. In these simulations a non-linear kinematic hardening model (Chaboche) with isotropic softening as well as a perfectly plastic model were used (in order to see what effects the hardening model has on the stress state), the models and parameters used are described in Appendix A.

5.3 Contact properties

To prevent the symmetry conditions from being violated during the load reversal a rigid body has been created, see Figure 7b, and used for both the stationary crack and the propagating one. Contact is prescribed between the initially bonded nodes
on the slave surface and the master surface (rigid body). As a node is debonded
(released), the tractions acting on the node are ramped down to zero according to
a specified scheme. After this has been done to a node, contact searching between
the slave- and the master surface will be started. This is also the procedure for
the stationary crack where, however, no initially bonded nodes are used. The con-
tact properties specified for these surfaces, all which are recommended by Abaqus
for initially bonded surfaces [22], are frictionless conditions and a “hard” contact
pressure-overclosure normal to the crack surfaces.

5.4 Mesh

The need of simulating both stationary and propagating cracks has to be taken
into account when creating the model, this in order to receive results which can be
compared against each other by the use of exactly the same mesh for all analyses.
This means that the mesh not only needs to be fine enough to correctly describe
the state at the crack tip, but also to allow for a relevant propagation of the
crack. The number of elements needed can be found in a number of different
studies, mainly suggesting that one should describe the theoretical plastic zone
given by Irwin with sufficiently many elements, see Equation (6) for plane strain
conditions, c.f. [6, 25, 26]. Therefore, a model consisting of a coarse mesh at the
outer parts see Figure 8, and a more refined mesh at the area where the crack is to
be propagated, has been adopted, see Figure 9. All of the elements are rectangular
four noded fully integrated elements, due to suspected plane-strain locking which
might otherwise be received with higher order elements [9], except for the transition
mesh area separating the inner and the outer course mesh where triangular three
noded elements are used. As suggested in [27], the mesh was made rectangular
with a 2:1 aspect ratio in/of the elements at the refined area close to the crack tip,
which also substantially reduces the computational cost. The chosen dimensions
of the smallest elements at the crack tip were 5 \( \mu \)m in length and 10 \( \mu \)m in height.

\[
r_p = \frac{1}{3\pi} \left( \frac{K_I}{\sigma_y} \right)^2
\]

(6)
5.5 Nodal release scheme

No fracture condition was applied to the model, thus the nodes were released at specified time points in the simulations. The current crack tip is user controlled by specifying how long each crack increment will be, here chosen to be one element, see Figure 10.
5.5. NODAL RELEASE SCHEME

Figure 10: Crack length released as a function of time, current crack tip is $l_3 = l_1 + \Delta l_{23} + \Delta l_{32}$, see also [22].

To create a plastic wake behind the crack tip one element at minimum load was released every second load cycle, see Figure 11, as it has been shown to make little difference when during the load cycle the nodes are released [9]. By doing this for four plastic zones, a sufficiently large plastic wake was created. This will affect the stress normal to the crack plane when compared with the results of the stationary crack. After all the nodes have been released, the model at final crack length was subjected to ten load cycles, this in order to reach a steady state condition. The same number of load cycles (ten) were applied for the stationary cracks, thus making a comparison between possible.

Figure 11: Nodal release scheme, see also [24, 29].
CHAPTER 5. FE SIMULATIONS

5.6 Implementation and evaluation

The propagating crack model requires a lot of load steps in order to be completed, in total 193 different steps for the hardening model and 151 different steps for the perfectly plastic model. This was accomplished by a automated routine using FORTRAN, creating everything needed for the Abaqus input file. A non-linear model like this with a lot of elements, in total 9331 four noded and 1532 three noded elements, requires substantial amount of computer resources, therefore the simulations were run on a Linux cluster containing eight quad core processors.

All of the simulations require a lot of time increments, which were set automatically, to speed up the process and to ensure that all the specified points at certain loads are reached and not excluded by some time increment being too large, specified time points were given at which output data were to be written. These were at maximum load, hold time and minimum load.

Evaluating the results can be time consuming with a lot of different simulations, therefore a Python script together with another FORTRAN and Matlab program were used to post process the results.

5.7 Additional simulations

To see if there would be any difference by using eight noded fully integrated elements, one simulation was carried out with ten load cycles for the stationary crack and the perfectly plastic constitutive model.

In addition to this, the simulations in [18] were recreated and evaluated to see if the results were similar, see Figure 12. This was done from peak load in steps of 10%, to 50% partial unloading from peak load with R=0.1. Fully integrated four noded elements, with the same mesh and the same $K(=30 \text{ MPa}\sqrt{m})$ as in the other simulations were used. However, the same constitutive model as in [18] was not adopted as those parameters were not available, here instead the hardening one in Appendix A was used.
5.7. ADDITIONAL SIMULATIONS

Figure 12: Simulations from [18], x displays the amount of partial unloading and measurements are done after partial unload from peak load.
6.1 Mechanical testing

Results from the mechanical testing are found in Figure 13, including initial overloads of 2.5%, 5%, 15% and a reference load (0%), together with the pure cyclic crack growth test at 0.5 Hz. The results of these evaluated test are presented with respect to $\Delta K$ and $da/dN$.

Figure 13: Mechanical test results.
6.2 FE simulations

The same initial overload levels as investigated in the mechanical experiments are evaluated, i.e. 2.5%, 5%, 15% and the reference load with 0% initial overload.

FE results are here shown for the stress normal to the crack plane in front of the crack tip, at a distance chosen according to the damage zone which was measured to 0.2 mm for $K=30 \text{ MPa}\sqrt{\text{m}}$ in [16]. The stresses are all taken at the same load level, this is at $K_{\text{hold}}$ after the initial overload has taken place. If the specimen was subjected to an overload, the point in time where the stresses normal to the crack plane have been measured was after the initial overload, at the same load amount as a cycle with no overload. This procedure was applied in all load cases except for the results recreated from [18], see Figure 14. Moreover, the results were taken at the two integration points of each element lying closest to the crack plane, this to avoid any numerical extrapolation errors which might otherwise be received.

![Diagram](image)

Figure 14: The point in time for which the stress normal to the crack plane was measured, x displays the amount of overload.

The results for the hardening model are shown in Figure 15, 16, 17 and 18.
Figure 15: Stress normal to the crack plane in front of the crack tip, with stationary crack and hardening model, after the first overload.

Figure 16: Stress normal to the crack plane in front of the crack tip, with stationary crack and hardening model, after the 10\textsuperscript{th} overload.
Figure 17: Stress normal to the crack plane in front of the crack tip, with debond procedure and hardening model, after the first overload.

Figure 18: Stress normal to the crack plane in front of the crack tip, with debond procedure and hardening model, after the 10\textsuperscript{th} overload.

The results for the perfectly plastic model are shown in Figure 19, 20, 21 and 22.
6.2. FE SIMULATIONS

Figure 19: Stress normal to the crack plane in front of the crack tip, with stationary crack and perfectly plastic model, after the first overload.

Figure 20: Stress normal to the crack plane in front of the crack tip with stationary crack and perfectly plastic model, after the 10th overload.
CHAPTER 6. RESULTS

The difference in behaviour found with eight noded elements is seen in Figure 23 at 0.66 mm final crack length and $K=30 \text{ MPa}\sqrt{m}$.
Figure 23: Stress normal to the crack plane in front of the crack tip, with stationary crack and perfectly plastic model, eight-noded elements after the first overload.

The simulations run for recreating the results from [18] were also evaluated and are seen in Figure 24, all after the first load cycle.

Figure 24: Stress normal to the crack plane in front of the crack tip, with stationary crack and hardening model, after the first overload.

6.3 Further evaluation

As one can see in the Figures above the first elements closest to the crack tip is strongly dependent on cycle number, element type etc. One idea for evaluating the stresses is therefore to normalise them and then integrate over a chosen distance,
here taken as the damaged zone of 0.2 mm; this instead of evaluating the stresses at a single point which due to numerical uncertainties might be questionable. The results were evaluated as follows below and is also summarised in Equation (7)

1. The distance from the crack tip was normalised with respect to 0.2 mm which was the size of the damaged zone, with the distance to the first integration (here denoted as x(1)) point removed at all points in order to receive the result 1.0 for the reference load of 0% initial overload (see point 2 and 3 below).

2. The stress normal to the crack tip was normalised for every value with respect to the reference load.

3. The output gained from this procedure was integrated to receive one average reduction value for each simulation.

\[
\text{int } \sigma_{\text{y,normalised}} = \int_{x(1)}^{1} \frac{\sigma_{y}^1(x - x(1))/0.2}{\sigma_{y}^0(x - x(1))/0.2} dx
\]  

(7)

The results gained from this evaluation is found plotted against the corresponding initial overload in Figure 25 and 26 below.

![Figure 25: Integrated results for the different initial overloads and constitutive models for the stationary crack simulations.](image)
Figure 26: Integrated results for the different initial overloads and constitutive models for the debond simulations.
In the experimental results in Figure 13, one sees that even a small initial overload affects the crack propagation rate enormously. The small initial overload of 2.5% massively reduces the crack growth rate and for 15% initial overload the devastating hold time effect is almost canceled, as it is almost in line with the pure cyclic crack growth test. One may speculate about the reason for their effect on the hold time crack growth behaviour. However, one explanation could be that for 15% initial overload the fracture surfaces end up in compression and that the hold time of 2160 s is not enough for the crack to grow out of this area. The same would apply to the cases of 2.5% and 5% initial overload but with not as large compressive zone as created for 15% initial overload. The tests with the two smaller initial overloads could have enough hold time to again increase the crack growth, but at this moment the hold time would have ended and a new overload would be applied, and thus again reduces the crack growth. An initial overload before a hold time is thus a factor which influences the crack growth shown in IN718, as the mechanical testings have shown a remarkable reduction of $da/dN$.

From the results of the FE simulations one can see a big difference between the 15% overload simulation and with the rest; the stress relaxation received here can be compared with the almost canceled hold time effect seen for the corresponding crack growth rate in the mechanical testing, see Figure 13. However, the stress near the crack tip is strongly dependent on which element type one uses, compare Figure 19 and 23, both after the first initial overload which applies for all simulations. The eight-noded element type is therefore not recommended to use due to plane strain locking, as also observed for the elements closest to the crack tip where the stresses oscillate from one element to the other, as previously mentioned in [9].

Since the first elements at the crack tip can not be fully trusted due to the large plasticity received, the suggested evaluation method is to evaluate the stresses along a distance rather than at a single point. This was done with the results seen in Figure 25 and 26. However, as mentioned this is just one evaluation method and it is strongly dependent on what distance one chooses to integrate over.

With the evaluated results in Figure 25 and 26 one sees that the reduction of the stresses between each overload is elastic and does not introduce any plastic flow, as the linear fit to each curve is almost perfect to each curve.

The difference between the different constitutive laws is also observed not to have any big influence if comparing the results against each other. Of course one can note
a difference when comparing each individual stress value, but the major distribution of the stresses normal to the crack tip appears to be the same.

When comparing the principal stationary crack results with the nodal release procedure results, one sees that the latter has a much less significant difference between the stress curves. This can be explained with the plastic wake created behind the crack tip as it is propagated onwards, which also raises the stress in front of the crack tip due to plastically deformed material behind it, compare e.g. Figure 19 vs. 21.

The evaluated results recreated from [18] seen in Figure 24 suggest that the stress values received are in good agreement with those seen in [18]. Although the information about the simulations done in [18] are not fully described one can draw the conclusion that the simulations were done in a similar fashion as here regarding element type, constitutive law etc.. However, no comparison between the results from the initial overload simulations and those in [18] can be made as they are two completely different load cases.

For the first few elements closest to the crack tip one can see a difference when comparing the first and 10th initial overload in all the simulations. By comparing Figure 15, 16, 17, 18, 19, 20, 21 and 22, one sees that there is a difference between the stresses. Although it should not be possible for the hardening constitutive model as it contains a softening law, this is clearly seen between e.g. Figure 15 and 16, this also applies for the other hardening constitutive law simulations and also for the perfectly plastic model. However, this behaviour is most likely due to the enormous plasticity induced in the first few elements at the crack tip, and that they are heavily distorted due to this. Thus, this also increases the motivation for an evaluation method that takes a more global perspective rather than a local one.

However, if comparing the overall look of the integrated stress values in Figure 25 and 26, one sees that after the 10th initial overload with the hardening constitutive model the curve has dropped a bit due to the softening behaviour applied. If compared with the perfectly plastic model which does not have this behaviour an anticipated difference is observed.

7.1 Conclusions

- A reduction of the stress normal to the crack plane in front of the crack tip is observed for each initial overload, which is believed to partly reduce crack growth rate in IN718, as seen in $da/dN$ for the mechanical testing. The most possible outcome of the initial overloads is the reduction of oxidation in front of the crack tip and/or a reduced damage rate in the oxidation affected region.

- Although the hold time effect is vastly reduced with initial overloads, the assumption is that this applies for long cracks and small scale yielding, less
can be said about how short cracks would behave.

- The different simulations for different element types, debond vs. stationary crack, and hardening vs. perfectly plastic constitutive relation reveals that it might be misleading to study only the first few elements in front of the crack tip. An alternative could be to use some kind of averaging procedure.
Outlook

During the time spent with this thesis a number of additional factors have been discovered that would give a deeper insight of the fatigue behaviour in IN718. Some of these are presented below and would in future work be of interest to investigate. Some of the topics below will be handled in the ongoing TURBO POWER project in which this work is being performed.

Based on only isothermal testing, not all can be said about what the behaviour under true TMF conditions would be, c.f. [30] (the load of the turbine is increases with increasing temperature, e.g. at start up and shut down).

It would be of interest to investigate different hold times, since by doing this there could be established a connection to how long time different overloads will affect the crack propagation behaviour.

Also, if considering mechanical testing, it would also be interesting to investigate the influence of grain size and temperature (isothermal) on the fracture behaviour.

To limit the numerous possibilities of post-processing, only the stresses were chosen to be processed in this work. As an example, crack closure can also be evaluated from the debond simulations, however it is debated whether crack closure occurs in plane strain or not, c.f. [9] for a review, the results from this can at least be evaluated. However, it is unclear if it would have any influence as the initial overloads are so small and would probably not introduce any crack closure before the hold time.

One of the most interesting things to be further studied is the microstructure of the test specimens from the mechanical testing. The knowledge gained from such investigations would probably show if the initial overloads have had an influence on the whole hold time or if its effect is reduced after some time.

Not to be forgotten, the FE-simulations in this study are not applied with any creep law to simulate time dependent deformation behaviour. Some curve fitting has been carried out by the author in an attempt to try to establish a simple Norton creep law in order to take secondary creep into account, but the parameters received were far to small. IN718 is also known to experience little creep. Furthermore if a diffusion law could be established, which would open up for simulation of the embrittlement of the grain boundaries this would be of high value, in the strive.
towards a better understanding and handling of the crack propagation under high temperature hold times.

Further, additional FE simulations with an even more fine mesh have been carried out, with element sizes as small as 0.05 µm at the area around the crack tip. In these simulations, it is shown that for e.g. 15% initial overload both compressive stresses normal to the crack tip and a reversed plastic zone are found, the equivalent stress from the 15% overload can be seen in Figure 27. These results would in future work be most interesting to investigate.

![Figure 27: Equivalent stress with 0.05 µm elements and perfectly plastic model after the first overload.](image.png)
Bibliography


The constitutive model for a material is the key component in receiving accurate results. This is further accentuated when simulating the fracture behaviour in a more accurate way by propagating a crack and thereby creating a plastic wake behind the crack tip. Depending on what load cycles the component is exposed to, the constitutive equations must be able to describe the material behaviour correctly. This is also true for fatigue analyses, i.e. the mean stress and the stress amplitude is of great concern, [31, 32]. Different constitutive laws have been set up in order to take effects like mean stress relaxation (cycling between two fixed strain values) and ratchetting (cycling between two fixed stress values) into consideration, see Figure 28, which are present when the loading is non symmetric (R ≠ –1).

![Figure 28: Material behaviour when loading is non symmetric (R ≠ –1), (a) mean stress relaxation, (b) ratchetting.](image)

A Non-linear kinematic hardening model

The original constitutive model for describing the behaviour illustrated above is the Armstrong-Frederic (AF) one, which combines kinematic hardening and dynamic recovery, c.f. [33, 34]. The backstress model for a von Mises material can be written
as Equation (8) below

\[
\dot{\alpha}_{ij} = \frac{2}{3} C \dot{\varepsilon}_{ij}^p - \gamma \alpha_{ij} \dot{\varepsilon}_{ff}^p
\]  

(8)

where \(C\) is the kinematic hardening modulus and where \(\gamma\) is the rate at which the hardening decreases. In the case of a von Mises material the effective plastic strain rate is written, \(\dot{\varepsilon}_{ff}^p = \left(\frac{2}{3} \dot{\varepsilon}_{ij}^p \dot{\varepsilon}_{ij}^p\right)^{1/2}\). However, since the AF model predicts a too large ratchetting and a too fast mean stress relaxation it is not suitable for most situations [35].

B Superposition of AF models

With the shortcomings of the AF-model in mind, Chaboche developed a model taking advantage of the backstress behaviours by superposing several of them, see Equation (9), c.f. [33, 34].

\[
\dot{\alpha}_{ij}^n = \frac{2}{3} C^n \dot{\varepsilon}_{ij}^p - \gamma^n \alpha_{ij} \dot{\varepsilon}_{ff}^p
\]

\[
\dot{\alpha}_{ij} = \sum_{n=1}^{m} \dot{\alpha}_{ij}^n
\]  

(9)

By superposition of the backstresses there is an improvement to the overestimation of the ratchetting behaviour. For instance, a stress-strain curve shows different behaviour for different strains, e.g. low vs. high strains. By using three backstresses the whole strain range can be covered, and by setting one of the \(\gamma^n = 0\) linear kinematic hardening can be obtained for high strains [36], this is further shown in Figure 29. However, in the simulations done in this work, the plastic strain levels received at the crack tip, due to the strain singularity, will be very large. To achieve saturated levels on the stress due to the high strain levels, all the \(\gamma^n\) values are set \(\neq 0\).
C. ISOTROPIC HARDENING

In addition to the kinematic hardening behaviour, where the yield surface is translated, an isotropic hardening component is needed to describe the change of yield stress throughout each cycle. This is done by defining the yield surface as a function of the equivalent plastic strain $\varepsilon^p$ with the use of e.g. an exponential law \[37\], see Equation (10) below

$$\sigma^0 = \sigma^0_0 + Q_\infty (1 - e^{-b\varepsilon^p})$$  \hspace{1cm} (10)

where $\sigma^0_0$ is the initial yield surface, $Q_\infty$ is the maximum change of the yield surface and $b$ is the rate at which the yield surface changes \[22\].

D. Constitutive description for Inconel 718

The numerical values for $C^n$ and $\gamma^n$ are in this work found by using an automatic procedure in the FE-software Abaqus \[22\], this by calibrating a 1.6% half-cycle test at the elevated temperature of 550°C, see Figure 30 and Table 2, one of the $\gamma$ parameters which was received automatically = 0 was as mentioned previously modified to $\neq 0$ in order to receive a saturated stress level. Furthermore, the isotropic softening parameters were found in \[38\], see Table 3, while the rest of the material parameters are shown in Table 4.
Figure 30: Calibration of material parameters.

Table 2: Numerical values of backstress components

<table>
<thead>
<tr>
<th>$C^n$ [MPa]</th>
<th>$\dot{\alpha}_{ij}^1$</th>
<th>$\dot{\alpha}_{ij}^2$</th>
<th>$\dot{\alpha}_{ij}^3$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>65290</td>
<td>40864</td>
<td>5917</td>
</tr>
<tr>
<td>$\gamma^n$</td>
<td>1633</td>
<td>377</td>
<td>54.6</td>
</tr>
</tbody>
</table>

Table 3: Isotropic hardening parameters

<table>
<thead>
<tr>
<th>$Q_\infty$ [MPa]</th>
<th>b</th>
</tr>
</thead>
<tbody>
<tr>
<td>-196</td>
<td>445</td>
</tr>
</tbody>
</table>

Table 4: Material parameters

<table>
<thead>
<tr>
<th>$E$ [GPa]</th>
<th>$\sigma_0$ [MPa]</th>
<th>$\nu$</th>
</tr>
</thead>
<tbody>
<tr>
<td>170</td>
<td>890</td>
<td>0.3</td>
</tr>
</tbody>
</table>

In addition to this, a perfectly plastic model has been calibrated in order to compare with the hardening one, $\sigma_y=1010$ MPa for $R_{p0.2}$ see Figure 31, to see what effects the constitutive behaviour has on the stress state.

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Figure 31: Calibration of perfectly plastic model.