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A Review of Thermo-Mechanical Fatigue behaviour in Polycrystalline Nickel Superalloys for turbine disc applications

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Abstract

Within the gas turbine engine, the high transient thermal stresses resulting from throttle movement from idle to high settings give rise to the phenomenon of thermo-mechanical fatigue (TMF). These effects have been widely explored for turbine blade materials, typically single crystal nickel alloys. More recently however, a combination of thinner disc rims and further increases in turbine entry temperature has lead to a situation where TMF in disc materials cannot be ignored. Turbine discs will usually be manufactured from polycrystalline nickel alloys, and as such it is now considered critical that TMF effects in this system of alloys is fully characterised. The current paper seeks to summarise the published work on TMF in polycrystalline nickel alloys for turbine disc applications, whilst introducing recent work at Swansea University. Previous work has concentrated particularly on the differences between In-Phase (IP) and Out-Of-Phase (OOP) loading. The current work from Swansea indicates that this approach is overly simplistic and that for a complete evaluation of the alloy, intermediate phase angles should also be considered.

Keywords: TMF, Nickel superalloys, Phase angle, Mean stress.

1. Introduction

In recent years there has been an increased awareness of the environmental impact of air travel. As such, it is clear that efficiency increases and the drive towards a more environmentally friendly engine are paramount to the well being of the aerospace industry. As would be expected, a significant portion of these improvements will be expected to come from engine designers in terms of gas turbine efficiency.

Increases in efficiency of the gas turbine will usually be achieved in one of two ways; either weight reductions or increases in temperature, resulting from the fuel being burnt at temperatures approaching the stoichiometric value. In either case, it is clear that there will be a significant impact on the materials used. The modern criteria for such materials include a high temperature fatigue and creep capability with suitable environmental and corrosion resistance properties. Consequently, nickel base superalloys have been introduced to commercial jet engines as safety critical component materials for applications such as turbine discs to allow for the greater stress requirements at elevated temperatures. These alloys offer an excellent intermediate solution where the lighter titanium alloys no longer have the temperature capability to operate, and more exotic higher temperature capable materials would be considered too expensive.

Polycrystalline nickel alloys exhibit excellent mechanical properties at elevated temperatures and enable a considerably higher engine thrust-to-weight ratio and fuel efficiencies to be achieved. Typical alloys include: Inconel®718 (IN718), a nickel-based precipitation strengthened alloy primarily used in the high pressure compressor and low pressure turbines of large civil gas turbines; Alloy 720Li, an age-hardened polycrystalline material commonly used in high pressure compressor and turbine discs; Waspaloy, an age-hardened alloy used for blade, disc and seal applications; Nimonic 90, a nickel-chromium-cobalt alloy used for exhaust nozzles and RR1000, a polycrystalline material produced through powder metallurgy, which is commonly used for elevated temperature disc applications in current and forthcoming Trent family engines.

2. Background to Thermo-Mechanical Fatigue

In a modern gas turbine jet engine, many components are simultaneously subjected to alternating mechanical and thermal loads, during, for example, start up and shut down operations. Such situations can lead to the nucleation and propagation of cracks through to failure, a phenomenon which is known as Thermo-Mechanical Fatigue (TMF). TMF is essentially a complex failure mechanism which is caused by combined thermal and mechanical loading cycles where both the stresses and temperature vary with time. Areas of the gas turbine particularly affected by TMF conditions include components in the combustion chamber along with turbine blades and discs. TMF is not a new phenomenon, and has been considered by designers for a number of years. Indeed, TMF is well acknowledged as being the primary life-limiting parameter for many engineering components exposed to elevated temperatures and Sims stated that 'by the mid seventies it was apparent that the general failure mechanism at high temperature for most high-pressure turbine components was that of thermo-mechanical fatigue' [1]. However, consideration of the phenomenon has been restricted by a lack of reliable mechanical test data and a tendency for designers to seek to extrapolate isothermal test data to predict TMF behaviour.

The requirement for increased operating temperatures, however, means that the complex nature of TMF needs more detailed consideration. Not only does the increased maximum temperature lead to a larger temperature range (and hence more significant TMF effects), but often pushes materials into a regime where additional damage mechanisms such as creep and environmental damage can contribute more to life reduction. Separating the effects of each can prove to be one of the more challenging aspects of this type of research.

Whereas extensive literature exists on the isothermal loading behaviour of a number of alloys, research on the effect of thermo-mechanical loading is limited. One of the major differences between the two forms of damage is constraint, which is a necessary ingredient in thermal fatigue [2]. Constraint can exist in any structural component due to thermal gradients, material anisotropy and geometry effects. In a typical isothermal scenario, deformation near a stress concentrator is often constrained by the surrounding material. However, if the structure is subjected to TMF, then the thermal strain will also be a contributing factor, where surrounding cooler material constrains the expansion of hotter material. In such a case, the mechanical strain can be calculated through the following equation:

$$\epsilon_{\text{mechanical}} = \epsilon_{\text{total}} - \epsilon_{\text{thermal}} \quad [1]$$

It is widely acknowledged that TMF loading is far more damaging than typical isothermal fatigue. This is the case even at the maximum operating temperature, where creep deformation would be expected to be most damaging [3, 4]. Indeed, in the case of nickel based superalloys there is evidence that TMF crack growth rates can be higher than isothermal rates from tests performed at the peak temperature in the thermo-mechanical loading cycle [5, 6].

As is the case in an isothermal fatigue test, there are a number of factors which affect the outcome of a laboratory TMF test, such as material properties, mechanical strain range and strain rate. However, there are further complicating factors unique to the TMF test, such as phase angle, temperature range and even loading direction. The phase angle is a term which is used to describe the phasing between applied strain and temperature, and can range from 0 to $\pm 180^\circ$. A 0°, or in-phase (IP) test is one in which the temperature and applied strain increase and decrease in proportion with each other. In a test with a phase angle of 180°, also known as an out-of-phase (OOP) test, the minimum temperature will be coincident with the peak applied strain, and vice versa. Based on these extremes it is clear that an infinite number of possible phase angles exist in between. Examples of possible phase angles are shown in figure 1(a) according to the TMF Code of Practice (TMF COP) [7] . The figure also suggests that, as mentioned above, loading direction (clockwise or anticlockwise) may also be a contributing factor to TMF life. Figure 1(b) shows an alternative method of phase angle investigation, utilised in work at Swansea University in which strain-temperature paths are kept consistent, although this results in different R ratio values for tests at different phase angles, if a consistent strain range is to be examined.

With a 90° phase shift, otherwise known as a diamond loading loop, the maximum and minimum strains occur at the mean cycle temperature. There are two forms of diamond loop, a clockwise diamond and an anti-clockwise diamond. Under a clockwise diamond loading condition, the strain initially increases in response to the temperature, whereas with anticlockwise loading the strain decreases (figure 2). The phase between the thermal and mechanical cycle is defined as the angular fraction of the cycle by which the mechanical cycle leads the thermal cycle. Therefore, for a negative phase shift the loop follows an anticlockwise track, where the maximum tensile strain is achieved during the cooling part of the cycle, and vice versa [8]. The actual waveforms and phasing between the mechanical strain and temperature experienced in a particular component can vary widely according to the geometry of the component and its use. Historically, the first TMF simulations that were used focused on a highly simplified version of the type of cycle experienced in components in service. However, with improvements in the capabilities of TMF testing, greater emphasis has been placed on a better representation of the true service conditions [9] as the importance of the strain-temperature-time function on the fatigue behaviour of the material is realised.

Clearly these variations in phase angle can have a marked effect on fatigue life by influencing material deformation characteristics. Considering a typical TMF cycle operating between 300°C and 700°C, an increasing mean stress is more likely to occur if peak strain occurs at the minimum temperature (out of phase test), since increased stress relaxation through creep will occur on the compressive part of the cycle, acting to increase the minimum stress in the test. However, in an In Phase test, where peak strain coincides with peak temperature, creep will now act to reduce the peak tensile stress, resulting in a decreasing mean stress. An example of how the phase angle may affect the shape of the stress-strain curve in this manner is shown in Figure 3 [10]. The impact of the different deformation mechanisms should also be acknowledged, with creep damage occurring

mainly at the grain boundaries of the material, whereas plasticity will be observed as deformation of the grains themselves.

The situation is further complicated by the fact that there is likely to be a strong influence of not only creep but also environmental damage on the fatigue behaviour of nickel alloys at higher temperatures [11]. The extent of these damage mechanisms will be dependent on the alloy in question, with alloys designed for higher temperature operation being less susceptible. To illustrate the effect of these mechanisms, it is useful to consider a simple in-phase and out-of-phase cycle. Under typical in-phase loading, the maximum temperature and strain occur together, whereas in an out-of-phase condition, or a 180° shift, the material experiences a tensile strain at the lowest temperature and compression at the maximum temperature. Consequently, oxidation damage is more likely to occur under OOP loading as an oxide film builds up under compression at the higher temperature but then ruptures and spalls during the subsequent low temperature tensile portion of the loading cycle where the oxide film is more brittle. Mechanical straining then leads to more cracking and exposes a new clean surface. The clean metal will rapidly oxidise and the process then repeats itself during the following cycle. Oxide cracking can also form under IP loading during the hot portion of the cycle. Then upon cooling, the oxide film undergoes a buckling delamination which exposes the clean metal surfaces. In contrast, IP loading is more likely to cause damage through creep during the high tensile stage at the highest temperature, as shown in Figure 3. The issue may be further complicated however by the fact that the crack propagation life usually dominates over the initiation life in TMF tests[12]. The effects of oxidation on crack propagation rates in these experiments may also be complicated. Whereas in IP tests oxidation will be most significant under tensile loading, and would be expected to accelerate crack growth, it can also have a positive effect by making crack closure more pronounced, and reducing the effective stress range. As such under certain test conditions crack growth in an OOP regime may show increased rates in comparison with IP tests.

3. Experimental methods of TMF Testing

Component features experiencing thermo-mechanical fatigue in the gas turbine engine are typically constrained by surrounding material. To idealise these in-service conditions, laboratory test specimens will typically be subjected to a combination of thermal and mechanical strains, which can be varied to most accurately replicate realistic operating conditions. A typical TMF test arrangement can be utilised to simulate the local conditions of components within a jet engine that experience the take-off, cruise, descent and landing cycles, as displayed in figure 4.

3.1 Test Set Up

As reported by Hähner et al [13], and subsequently described in more recent international standards [14,15] as they worked towards a European code of practice for strain controlled TMF testing, servo-electric and servo-hydraulic test machines have both been proven to be suitable for TMF testing purposes [1, 3-4, 7, 16-31]. This is provided that they are capable of applying a load in

both tension and compression, without a potential backlash as the load passes through zero, whilst in either total strain or load control so the two types of control can be changed during a test.

The load train usually requires water cooled specimen grips to allow a quick cyclic stabilisation of the longitudinal temperature distribution within the gauge length and to provide stable thermal conditions for the duration of the experiment. In a similar way to a low cycle fatigue test, any misalignment in the load train due to an angular and/or axial offset could prove critical in the outcome of a test and should not exceed 5% of the axial mechanical strain range [13].

3.1.1 Temperature Control - Grip and Induction Coil Design

Traditionally, the required temperatures for isothermal fatigue testing are generated using a conventional radiant furnace. However, this method is unsuitable for thermo-mechanical fatigue purposes since the heating and cooling rates are not fast enough to implement the desired phase angles within the required time intervals. Therefore, TMF testing demands a more rapid and accurate dynamic control of the test specimen's temperature to produce the necessary thermal cycle. One method is through employing a radio frequency induction heating facility with cooling achieved by introducing compressed air in the core and outside of the hollow specimen. This heating system uses an alternating current passed through a water cooled copper induction coil to create a magnetic field. When the test-piece is then placed within this field a current circulates which flows against the resistivity of the alloy, producing eddy currents in the structure which are capable of creating faster heating rates. The coil design for induction heating is another issue. Andersson et al [32] performed a series of experiments to quantify the effect of temperature gradients existing over the gauge length of a cylindrical test-piece. The tests were carried out with different induction heating coil set ups and different heating/cooling rates and showed that the internal temperature differs from the external temperature. Furthermore, the distance between the heated gauge length and the cooled specimen ends should be kept as large as possible to avoid 'barrelling' problems due to excessive cooling of the gauge length ends. Andersson et al [32] also found that for thermal based strain compensation and temperature measurement in the gauge length, the placement of the coil was not found to be critical [32]. According to work undertaken by Kühn et al [23] towards the code of practice, meaningful results are gained provided that the extreme temperature values are kept to $\pm 5^{\circ}\text{C}$, which is within the capability of most TMF facilities.

Clearly, whichever heating or cooling method is utilised, the design of the test specimen will have a significant influence on heating and cooling rates. Hollow specimens offer the possibility of rapid temperature transitions which are desirable as the cycle time can be reduced allowing for shorter overall test programmes. However, solid specimens will clearly offer more representative data for many component features within the gas turbine engine, but cooling rates particularly can be an issue. Natural cooling may be sufficient for some applications, although forced air cooling may be applied to decrease the cycle time. Disparities in cooling rate between the forced air cooled surface and the centre of the specimen require consideration, with unwanted residual stress development a by product of an overly ambitious cooling rate.

Whereas induction heating is the most widely used technique [7, 16, 18-21, 25, 28-29, 31-35], Yuan and Kalkhof [36] stated that this form of heating may well have an influence on crack propagation in

the highly stressed crack tip vicinity. This is due to magnetic flow perpendicular to cracks influencing the uniformity of the temperature field, and in fact all techniques which involve passing a current through the specimen might affect the crack tip region [36]. Hence, the development of potential thermal gradients should be minimised to ensure that similar conditions are experienced at the crack tip between load cycles [5].

Evans et al [21] undertook an analysis on the different coil designs that could be utilised for induction heating. They found that a problem arose whilst using large grips in their test apparatus as they acted as heat sinks drawing heat away from the specimen, so a redesign was required to eliminate the possibility of heat loss. The initial design in figure 5(a) consisted of an evenly wound coil to induce an even amount of heat input along the specimen surface, but without an allowance for water cooled grips. A modified design was introduced in order to provide more heat at the ends of the specimen, but the design fell outside the limits specified in the code of practice [7]. A final coil arrangement was devised consisting of two metal inserts. The inserts provided a suitable means of connecting the grips to the specimen, and as they were not cooled, they drew less heat away from the surface enabling a more consistent temperature profile across the whole specimen (Figure 5(b)) [21] to be achieved. It should however be emphasized that this was an issue unique to the individual equipment and this combination of induction coil shape and metallic inserts is not a requirement for all TMF facilities.

Radiation lamps have also been implemented in a number of different systems as an alternative heat source. Pernot et al [37] and Mall et al [38] performed a series of TMF tests where quartz lamps were employed to heat the specimen surface. It is clear that this is a useful technique in the production of thermo-mechanical fatigue crack growth behaviour, potentially resolving the issues of crack tip heating associated with induction heating. However, the benefit is often offset by the difficulty of the experimental setup, in particular achieving a consistent temperature profile, and also by the high expense of the quartz lamp system.

Jacobsson et al [5] performed a number of TMF crack propagation experiments and used two Leister fans to produce the required heat. One of the fans was connected to a Leister air heater to produce an outlet temperature of 900°C, whilst the other was operated to cool the specimen for the cooling part of the cycle. The two air flows were alternately directed at the test specimen, to enable the heating and cooling cycles during chosen periods of time [5]. On separate occasions, Jacobsson et al [39-40] performed further TMF crack propagation tests and used heating wires to achieve the fast heating and cooling rates that are required. In this case, the wire is wound around the test-piece and a current passed through to heat the specimen locally, giving a homogeneous temperature in the centre of the specimen. The cooling part of the cycle was achieved through water cooled copper pieces attached to the specimen [39-40].

3.1.2 Temperature Measurement

Accurate temperature measurement during a TMF test is usually achieved through one of two means. These are either spot welded ribbon type or coaxial contact thermocouples, or non contact infrared thermometry through a pyrometer. Both methods have their own limitations however. When thermocouples are welded to a specimen, they can create a flaw on the surface, or a potential

cold-spot location. Cracks are commonly found to initiate from such regions. Consequently, welded thermocouples should be located in a low stress region such as the specimen shoulder, unfortunately where there is also the steepest thermal gradient. As a consequence, the temperature can be highly sensitive to any errors that occur in positioning the thermocouples. Nonetheless, this method is still commonly used [1, 7, 16, 20, 21, 23, 27, 29, 31, 39-41]. Ribbon type thermocouples are capable of measuring the direct temperature in the centre of the specimen's gauge length without risk of damaging the surface of the alloy. However, since they are wrapped around the specimen rather than welded to the surface, care must be taken to ensure that there is sufficient thermal contact between the test-piece and the thermocouple without any potential degradation during the period of the test through oxidation or roughening of the surface. Coaxial thermocouples require the machining of accurate holes in the specimen surface to match the diameter of the thermocouple for their insertion [13]. In terms of the type of thermocouples that are appropriate, type N and K are usually suitable for the majority of purposes up to 850°C. Type R and S thermocouples should be used at elevated temperatures (>850°C) as they offer superior high temperature stability [7].

Infrared pyrometers offer an alternative where no contact is required. However, pyrometers are highly sensitive to any changes in the surface emissivity of the specimen which may result from oxidation [13]. Furthermore the TMF Code of Practice [7] does not recommend using this method of temperature measurement but pyrometry has still been used on several occasions [9, 21, 42], provided a stable emissivity value can be established for the duration of the test, via pre-oxidation at a suitable temperature, or other methods. Codrington et al [35] chose to use thermocouples over optical measurement methods such as pyrometry, based simply on cost and ease of use. They also argued that through using thermocouples, the need for spectral emissivity data for the test materials, a necessity for pyrometry, was not required [35].

4. Test Results

4.1 Total fatigue life approach

There is a limited amount of data available on the subject of thermo-mechanical fatigue in polycrystalline nickel alloys for turbine disc applications. Most of the work available has focussed on turbine blade alloys (polycrystalline and single crystal), examples of which can be found in the referenced papers [1, 22, 30, 43-53], due to the high temperature requirements of turbine blades operating within the gas stream, and the rapid temperature cycles that develop in blades. These alloys can be required to operate at gas temperatures in excess of 1600°C, far in excess of the melting point of the alloy. This is overcome by the passing of cooling air taken from the compressor through the blades in conjunction with thermal barrier coatings. However, this does not stop the alloy being subjected to large temperature fluctuations and hence introducing significant elements of thermo-mechanical fatigue.

Increased temperatures are required to develop more efficient engines and as such increasing the operating temperature of turbine blades is clearly beneficial. The developments made in single crystal fatigue properties, particularly in terms of TMF, have made this possible. However, the increased temperatures also have a significant effect on disc materials, made from polycrystalline

nickel alloys, particularly at the rim. Coupled with the fact that designers are now moving towards thinner disc rims as a weight saving strategy, hence allowing more rapid temperature fluctuations, it is clear that TMF effects in these alloys must now be quantified.

In seeking to describe the TMF behaviour of polycrystalline nickel alloys it is important to consider that there are a significant number of mechanisms which will influence fatigue life. Not only will R ratio, phase angle, peak temperature, peak strain and creep/environmental resistance of the alloy have a significant influence, but also the manner in which they interact will have an impact.

Early results in a test programme at Swansea University have indicated that mean stress may have a significant role in determining the TMF life of the polycrystalline alloy RR1000. A test programme was conducted where specimens were cycled at a constant strain range of $\Delta\epsilon = 1\%$, with the phase angle varied for each test. IP, -45° CW, -90°CW, -90°ACW, -135°ACW and -180° OOP tests were conducted, and as demonstrated in figure 6a, a relationship between the mean stabilised stress (measured at $N_f/2$) and TMF life is observed. Further IP and -180° OOP tests conducted at higher strain ranges also indicated that the decreases in mean stress promoted by increased plasticity tended to promote longer than expected TMF lives, whereas the converse was true for -180° OOP tests performed at high strain ranges.

A simplistic attempt to rationalise this behaviour could be considered through the approach shown in figure 7, which attempts to describe the behaviour observed in this limited test programme. The figure illustrates how IP TMF, OOP TMF and isothermal fatigue (IF) tests performed at the peak cycle temperature in the TMF tests ,would be expected to behave for an R ratio of 0 (or $-\infty$ in the case of the OOP). For high strain ranges, IP cycles would be expected to show the longest fatigue lives, followed by isothermal and OOP. In the case of the IP test, peak temperature and peak strain coincide and as such significant creep would be expected to occur in the tensile part of the cycle, as indicated in Figure 3. Conversely in the compressive part of the cycle, creep influences are minimal and significant plasticity occurs. Both of these factors act to lower the mean stress experienced during the cycle, resulting in a longer fatigue life. In the OOP cycle for high strains, the roles are now reversed with plasticity occurring in the tensile part of the cycle and creep in the compressive part. This results in an increase in mean stress (Figure 3) and a resultant shorter fatigue life. Furthermore the lives are reduced by the influence of oxidation. As previously described, in the OOP tests oxidation now occurs more readily in the compressive part of the cycle at the higher temperature, with possible spallation at the lower temperature, higher strain portion. Isothermal fatigue tests tend not to show such a reduction in mean stress as the IP cycle, since creep occurs on both tensile and compressive parts of the cycle, due to the consistently high temperature. As a result, there is an element of reversibility of the creep damage and the mean stress reduction is not as significant as in the IP cycle, leading to a shorter fatigue life.

As shown in Figure 7, as the strain range is decreased, a crossover in the results would be expected. Below this point, it is envisaged that only small amounts of plasticity, if any, would be seen in the test. In this case, similar reasoning can be applied as for the high strain results. In OOP experiments with little plasticity, mean stresses would be expected to remain negative, leading to a longer fatigue life. Whilst there may not be significant differences between the relatively high mean stresses in IP and isothermal tests, the combination of thermal and mechanical stresses involved in the IP experiment would be expected to be more damaging, hence leading to a reduced fatigue life

compared to the isothermal test. Since the model is based only on a limited set of TMF data, it is perhaps useful to attempt to interpret existing data sets using this approach.

Work performed by Marchionni et al [29] on Nimonic 90 over a temperature range of 400-850°C and an R ratio of -1, clearly demonstrates the critical nature of TMF. As demonstrated in figure 8, when compared with isothermal fatigue tests performed at the peak operating temperature (in this case 850°C), significant variations are found between IP and OOP TMF lives. A factor of three reduction in fatigue life is evident when comparing IP to OOP tests, with isothermal tests at 850°C lying between these two extremes. Also indicated is the superior life of isothermal tests at 400°C.

Whilst the approach described above offers a rationale for ordering IP and OOP tests when a $R=0/R=-\infty$ cycle is employed, the arguments do not completely describe the effects seen in these $R=-1$ tests. The work by Marchionni et al [29] is consistent with the model only in that IP tests at the lower peak strains show shorter TMF lives than OOP tests. Further investigation in fact indicates that although some of the trends are similar, the reasoning in $R=-1$ tests differs from that described above. Marchionni [29] found that mean stresses were higher in OOP tests, which would then be expected to produce a shorter fatigue life. Similar results have been highlighted by Pahlavanyali et al [8] in which IP tests in Nimonic 90 showed shorter lives than OOP experiments for low strain ranges under $R=-1$ loading. The authors here indicate that since the stress ranges of the tests are remarkably similar, variations in fatigue life would be expected to be related to mean stress. However, the authors state that the failure modes of the two test types differ significantly with OOP tests failing by a transgranular mode of failure whereas IP show a more intergranular type of failure.

In order to understand the nature of these results, it is necessary to consider the fact that the fatigue lives of these specimens are extremely short, ranging only from approximately 80-2000 fatigue cycles. For lives this short, it is most likely that fatigue cracks were initiated as early as the first cycle, and hence essentially the entire life of the test occurs with a propagating fatigue crack. Results by Jacobsson [39] indicate that crack propagation rates under TMF conditions tend to be a function of the peak operating temperature in IP experiments, and the minimum operating temperature in OOP tests, since these are the temperatures experienced when the crack is fully open. Based on this, it seems reasonable to assume that the IP tests show shorter lives because the crack propagation rate in this type of experiment is faster than that under an OOP condition. An example of this is shown in the tests performed at 0.8% strain range. The peak stresses during the IP experiment range from around 300-450MPa, which will be applied at 850°C. Conversely in the OOP test the peak stress is 600-700MPa, which will be applied at 400°C. It seems reasonable to assume that the crack propagation rates in the IP test under these conditions will be faster than under a OOP condition.

Work by Evans et al [21] investigated TMF behaviour in IN718 either at an R ratio of 0, for IP tests, or $-\infty$ for OOP. It should be pointed out that the best fit line to the IF data at 300°C seems questionable, and clearly further data to help define the curve would be desirable, however there is clear value in consideration of the TMF data. Again, using the model proposed, it is consistent that the IP tests exhibit a longer fatigue life than OOP. At the temperatures involved in these experiments (300-680°C), significant plasticity usually occurs for peak strains greater than approximately 0.4%. As described, this results in a compressive mean stress in the IP tests, whereas a tensile mean stress is observed in the OOP, which as a consequence show a reduction in fatigue life, as evident in Figure 9.

Comparisons of the hysteresis loops of experiments carried out at 1% strain range indicate that this is indeed the case with the mean stresses as following; IP (-200MPa), IF (50MPa), OOP (100MPa). Although mean stresses in the IF and OOP tests are comparable a significant debit in life exists for the OOP tests compared to the IF experiments. This is due to the mechanisms involved in TMF having a more detrimental effect than those in isothermal fatigue. For this same reason IP tests show similar fatigue lives to IF tests despite large differences in mean stress. It is interesting to note that fractography of the specimens showed similar trends to the work by Marchionni et al [29] in which significant evidence of creep and environmental damage was observed in the IP condition. It would be anticipated that further reductions in the peak strain range of these tests would result in a crossover in the IP and OOP fatigue lives, as described previously.

Further illustrating the damaging nature of thermo-mechanical fatigue in comparison to isothermal fatigue, the work by Liu et al [54] shows many similarities to the work by Marchionni et al [29], although this time the alloy in question is K417. R=-1 testing is again employed and as such, mechanical strain ranges are relatively small. IP tests however show a significant reduction in fatigue life, presumably for the same reasons as described earlier. Perhaps more interesting, is that IF tests at 850°C show the longest fatigue lives. The authors further strengthen the argument that TMF is more damaging than IF through the observation that multiple crack initiation sites were found in the TMF specimens, whereas IF tests usually showed only a single initiation site.

It rapidly becomes clear that although mean stress clearly influences the ordering of IP, OOP and IF tests, there are further complicating factors. The first of these is that as a mechanism for fatigue in the material, TMF is more damaging than isothermal fatigue. The second, as has been clearly demonstrated is that the extent of life spent in crack initiation/propagation can significantly affect the ordering of these tests. It has been demonstrated here that in experiments that give short fatigue lives, where crack propagation dominates, the ordering of tests may differ from that expected based on mean stresses. The work undertaken by Beck et al [24], on IN792, further helps to illustrate this effect, Figure 10. Below approximately 850°C IP tests show similar if not improved fatigue lives when compared to the OOP. However, at temperatures higher than this IP fatigue data begin to reduce as fatigue lives become shorter and are most likely dominated by fatigue crack propagation. However, it should be noted that there is some scatter in the data and other interpretations of these results could be made. It is also in some ways misleading to directly compare the IF tests to the TMF results, which are based on zero to maximum/minimum cycles ($\Delta\varepsilon=1\%$). The IF tests are undertaken using an R=-1 cycle with a peak strain of 0.5%, and would subsequently see considerably less plasticity than the TMF cycles, resulting in extended fatigue

The work by Pahlavanyali et al [8] was conducted as part of Project Thermomechanical Fatigue: The Route to Standardisation and sought to investigate the effect of small phase angle shifts on the lives of IP, OOP and Counter Clockwise Diamond (CCD) TMF tests, in Nimonic 90. The authors found that phase shifts of up to 20° (in either direction) did not affect the failure mechanism, but did indeed have an influence on the deformation behaviour and consequently the TMF life. The work allowed recommendations to be made for the code of practice that an upper limit of $\pm 2^\circ$ on deviations in the phase angle should be applied.

Interestingly, the work by Pahlavanyali et al [8] seemed to be one of the few published programmes of work which investigated the effect of phase angles other than the typical 0° and 180°. Recently,

work at Swansea University has investigated more extensively the effect of phase angle on the TMF life, through the use of the phase angles illustrated in Figure 1(b). Testing was conducted according to this figure in order to provide more consistent strain-temperature paths, and produce a constant strain range of 1%. This however results in varying peak/minimum strains and hence R ratios. As shown in Figure 6(a), at the temperatures considered in this work (300-700°C) there appears to be a power law relationship between phase angle and life, and also that direction around the cycle (either clockwise or anticlockwise) does not deviate strongly from this trend. Based on the previous arguments it is therefore also unsurprising that a power law relationship exists between mean stress and life in these tests, Figure 6(b).

Mention should also be given to the extension of TMF tests into a multiaxial regime. Limited work has been undertaken in this field, although the most notable research is by Kalluri et al [25] in which a cobalt based superalloy, Haynes 188, was tested under axial-torsional TMF loading, hence introducing a second phase angle, ϕ . Clearly these are complicated tests which require detailed understanding. However, due to the complex stress fields which occur within the gas turbine engine, particularly blades and discs, it is surely an area which requires further investigation.

4.2 Crack propagation results

So far, only the deformation and crack nucleation behaviour of polycrystalline nickel alloys has been considered in detail. However, in safety critical applications such as gas turbine discs, some integrity assessments, for example to understand the consequences of handling damage, are based on the crack propagation behaviour of the alloy. As such, it is critical that crack propagation rates are available for the in-service conditions, and clearly this encompasses the effects of TMF.

The requirement for this sort of test data has long since been recognised, and some of the earliest work on the subject utilising polycrystalline nickel alloys was performed by Marchand and Pelloux [16]. Rather than the specific effects of TMF loading on the crack growth rates of Inconel X-750 and B1900+Hf, the materials employed, the work focussed more on the development of the test techniques. It is however possible to interpret the results from the test programme. Interestingly, the authors attempt measurements of TMF crack growth rates using an induction coil setup. There is still debate as to the effectiveness of this type of approach, particularly in terms of localised crack tip heating. However no mention is made of any effects seen in the work and good quality hysteresis loops were gained. Derivation of crack growth data is inconclusive with regards to the difference between IP, OOP and isothermal loading, although it appears that for B-1900+Hf, reductions in crack growth rate were actually seen in the IP and OOP tests when compared with the isothermal data.

Most of the subsequent useful work in TMF crack growth in polycrystalline nickel alloys has been performed by the research group in Lund University, Sweden, by Jacobsson et al [5, 39, 40]. The authors have explored different methods of acquiring crack growth data. One of the successful approaches [5] was the use of a cold air convection based setup to produce the thermal cycle. The two Leister fans used could produce a continuous room temperature flow of 1100 l/min. As such, one fan was connected to an air heater with an outlet temperature of 900°C allowing temperature control by varying the setup of the two fans. The authors state that thermal gradients were kept below 10°C circumferentially and crack growth measurements were recorded by potential drop

techniques. The authors generated a large amount of test data on IN718 through this technique, and it appears to be of high quality. Different R ratios were tested and it appears that at R=0, IP tests show a slightly faster rate than OOP tests, although the situation is reversed under R=-1 loading conditions. Care should be taken in the interpretation of these results though, since as stated by the authors, the crack closure contributions also significantly influence the tests. The experimental techniques however, are of great value and the data also has the advantage that the thermal cycle applied is 200-550°C, which should mean that the issue is not clouded by severe creep or environmental interactions in IN718.

Further work by the same authors investigates TMF crack growth by an alternative method [39]. Here, an environmental scanning electron microscope (ESEM) was utilised, with heating provided by a resistance wire applied to the specimen and cooling by water flowing through the grips. The work was again performed on IN718 and the test data again seems to be reasonable. A direct comparison is available for isothermal data at 550°C and IP and OOP TMF tests with a thermal cycle of 300-550°C.

In this case there is little difference between the IF and the IP data, although OOP tests do show a significantly reduced crack growth rate. This, perhaps, is to be expected since in the OOP tests, the peak temperature occurs when the test piece is under a compressive load and hence crack growth does not occur at this temperature. This point is further emphasised in the work by the fact that 300-550°C and 300-630°C OOP tests show identical crack growth rates, presumably similar to the growth rates expected in isothermal 300°C experiments. The authors indeed conclude that crack growth rate was determined by the temperature at which maximum mechanical load was applied. Microstructural observations further reinforced these conclusions.

5. Conclusions

It is clear that TMF is still very much a developing field. As stated earlier, although much focus has been placed on TMF of turbine blade alloys, increased operating temperatures have now meant that disc materials also need to be considered. Clearly there is limited research available on the topic of TMF in polycrystalline nickel alloys, from which turbine and HP compressor discs are generally manufactured. There is scope for extensive research not only in providing data for crack growth models, but perhaps more importantly for deformation models based on low cycle fatigue behaviour which is influenced by TMF.

Whilst the effects of in phase and out of phase loading have been investigated, it is clear that three significant factors influence TMF life. The mean stress achieved during the test can have a marked influence on fatigue life, and is particularly influential in non-symmetrical loading conditions (i.e R=0 or R=-∞ rather than R=-1). It has been shown however, that in shorter TMF tests, this is not enough to completely describe the material behaviour. Secondly, if conditions are conducive then fatigue cracking can occur as early as the first cycle, at which point the controlling factor becomes the crack propagation rate in the test. Then finally, it has also been demonstrated that TMF cycling is inherently more damaging than pure isothermal testing. Particularly there seems to be an apparent difference between IF and TMF behaviour in the non symmetrical behaviour of the stress-strain loop. In isothermal fatigue tests, there is the possibility of reversing some of the creep damage that

occurs due to near fully reversed loading, whereas in TMF tests the differing material behaviour at minimum and maximum temperatures prohibits this and in conjunction with the non-symmetric nature of environmental damage can significantly complicate the issue of TMF life. It is therefore clear that any attempts to produce valid fatigue life models for TMF loading, there needs to be a consideration for each of these factors in order to affect an accurate prediction.

In order to achieve this goal, experimental control of tests will be critical, and as such the code of practice for TMF [7] and international standards [14, 15] are a great benefit. However, experimental techniques for measuring crack propagation rates can still be improved, allowing for more extensive data generation. It is also likely that the further complication of multiaxial TMF will require investigation in order to fully describe the in-service conditions of these materials. Either way, challenges clearly exist in both experimental techniques and modelling approaches which must be addressed for the next generation of robust physical models.

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Figure captions

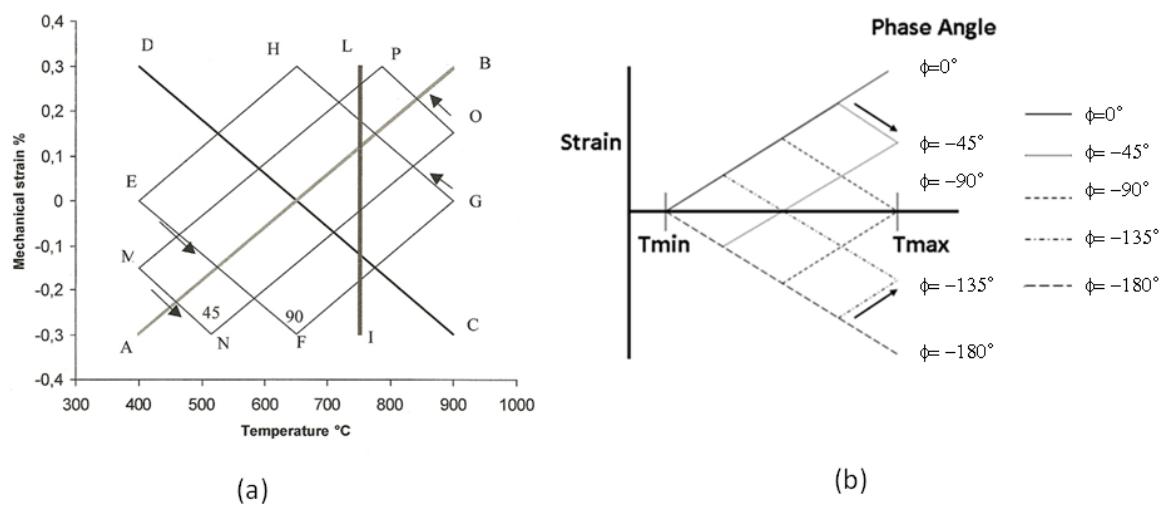


Figure 1: (a) Description of Phase angles (AB – IP, CD OOP, EFGH - 90° ACW, EHGF - 90° CW, MNOP - 45° ACW, IL – Isothermal (b) Phase angles utilised in the testing at Swansea.

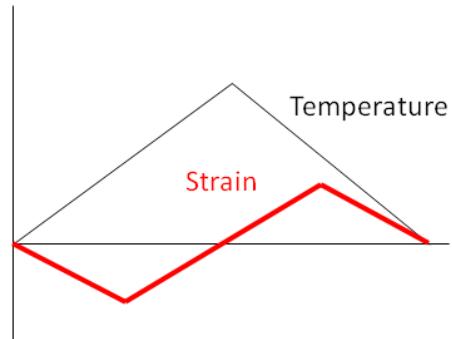
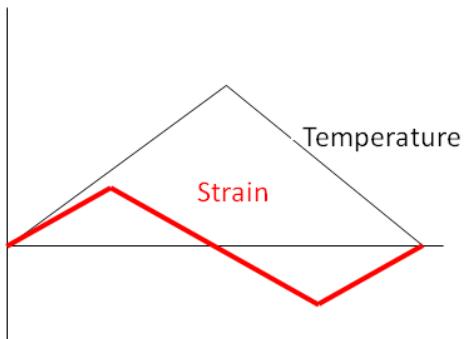
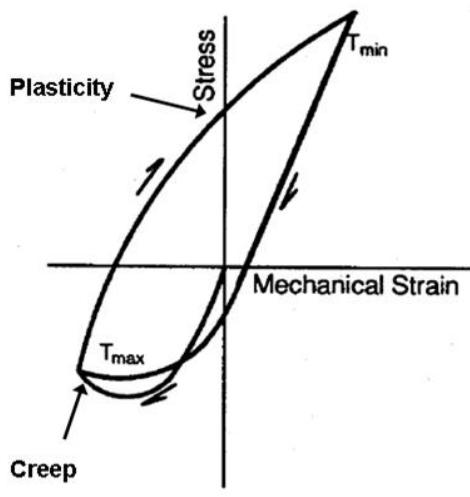
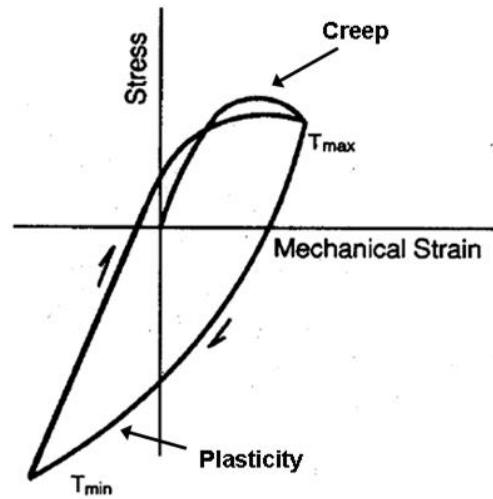


Figure 2: Clockwise Diamond (CD) and Anti-Clockwise Diamond (ACD) TMF waveforms.



Out-of-Phase



In-Phase

Figure 3: Resultant effect of phase angle on characteristic stress-strain hysteresis loop shape [2].

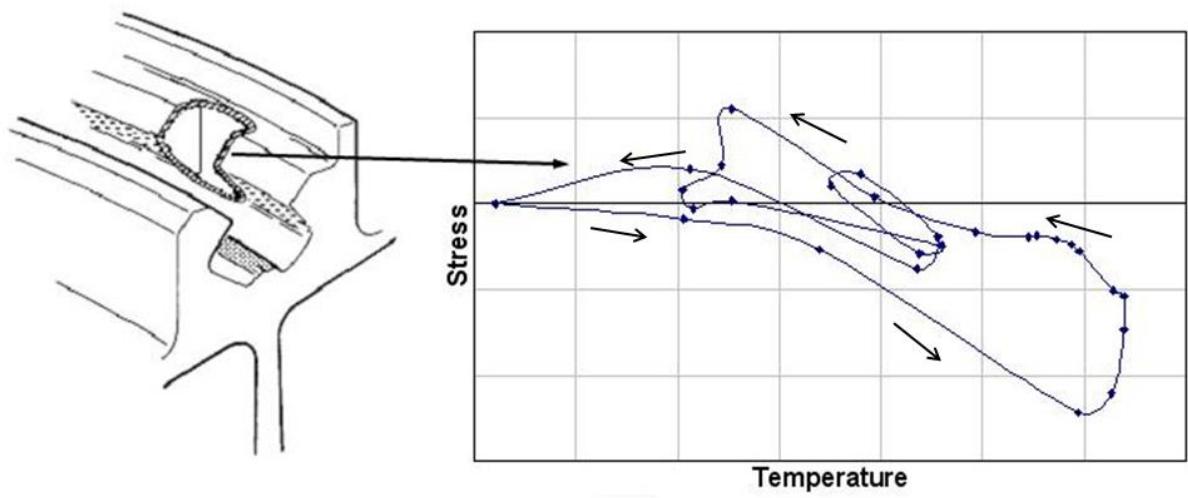
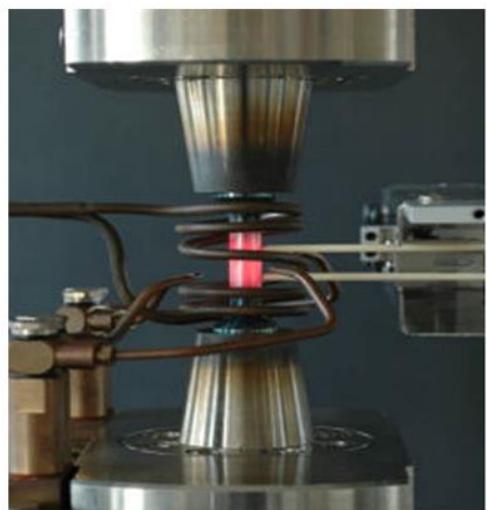


Figure 4: 'Peanut' diagram of stress vs. temperature for the loading slot of a high pressure compressor disc.



(a)



(b)

Figure 5: (a) Original evenly wound RF coil used by Evans et al [16]. (b) Final coil and grip design used by Evans et al [20].

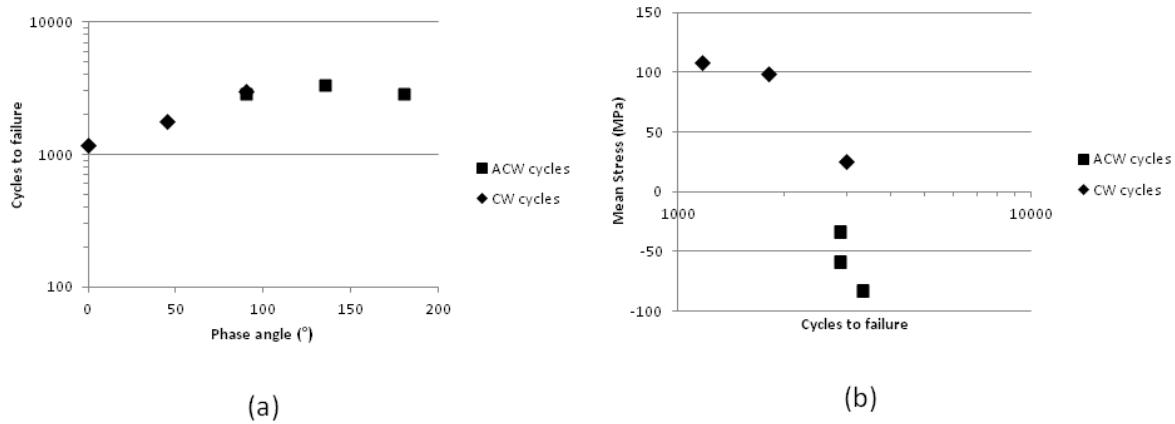


Figure 6: (a) Effect of loading angle and loading direction on fatigue life in RR1000 (b) Relationship between mean stress and cycles to failure in RR1000. Phase angles according to figure 1(b).

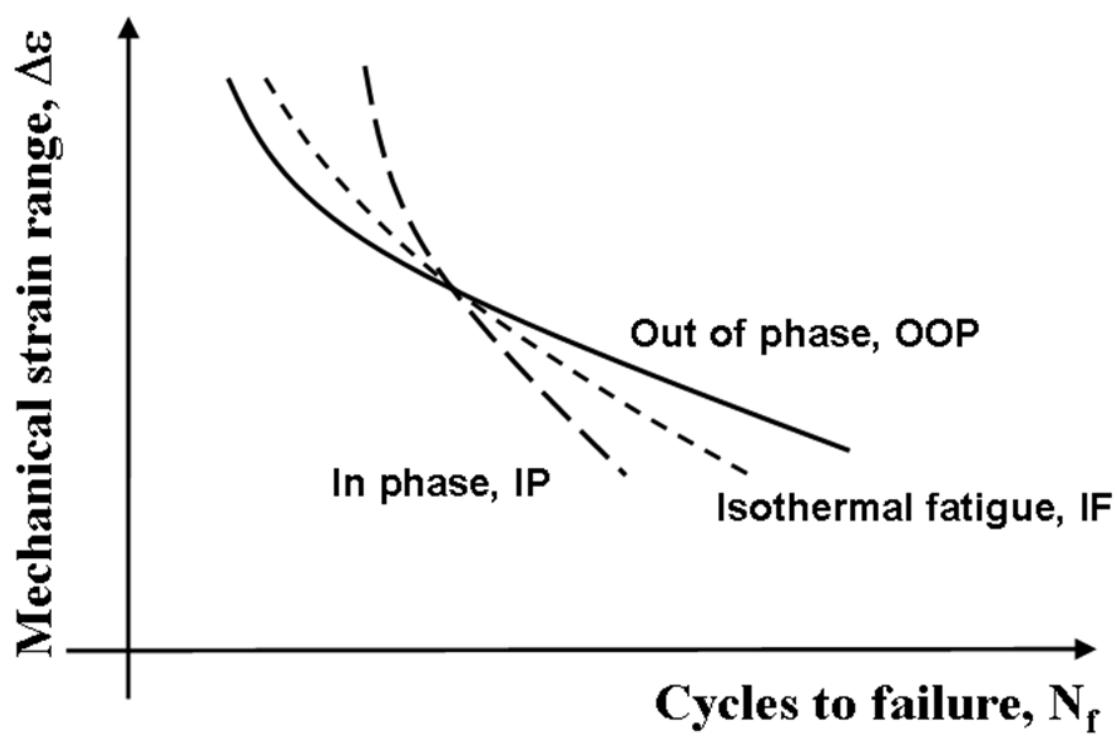


Figure 7: A model for TMF behaviour under zero-max ($R=0$, IP) or zero-min ($R=-\infty$, OOP) conditions

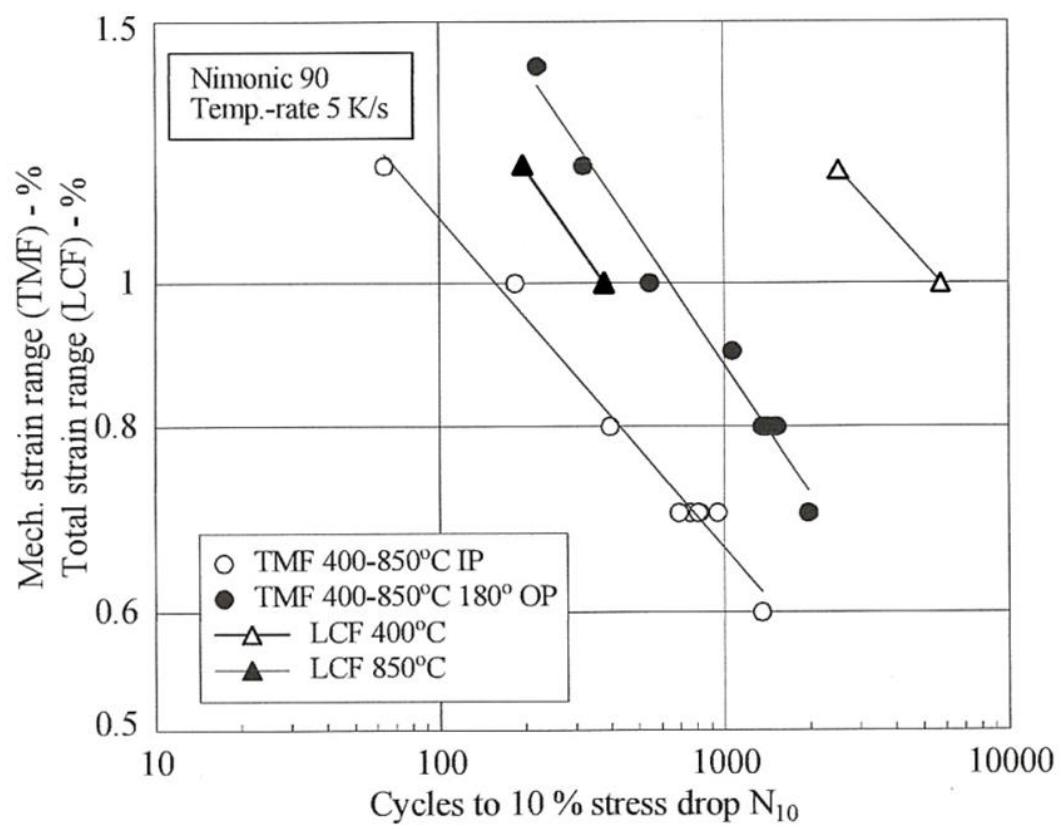


Figure 8: Comparison of TMF and IF lives in Nimonic 90 with a peak temperature of 850°C [29]

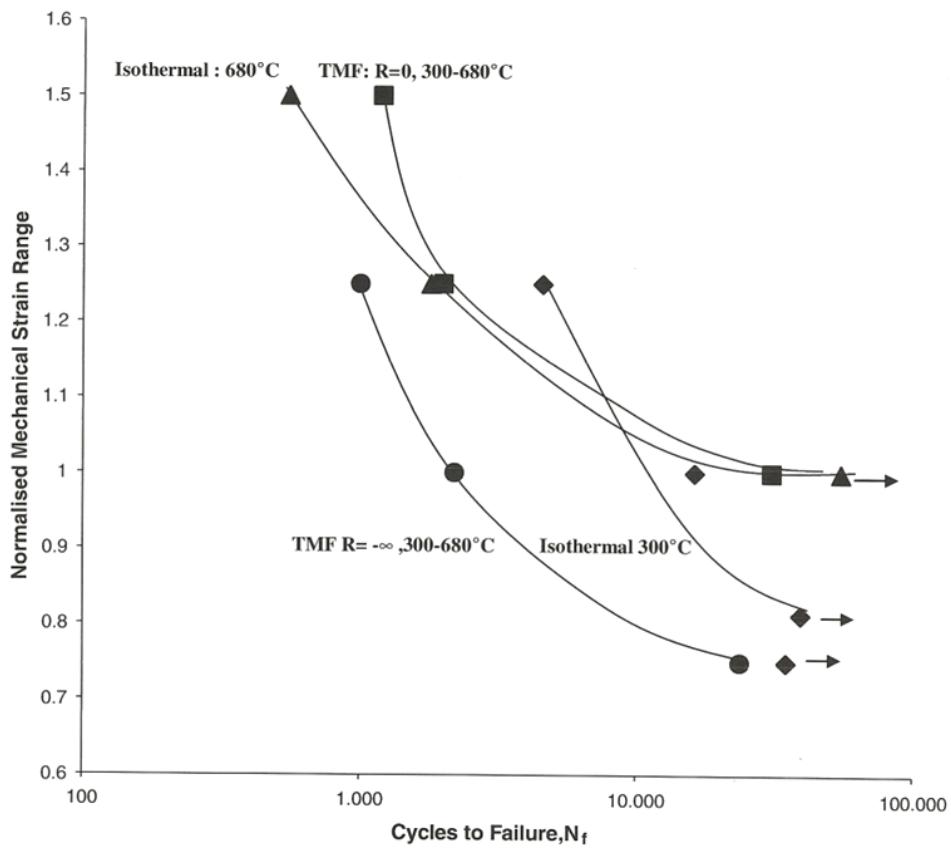


Figure 9: Comparison of IP, OOP and IF data in IN718 showing a correlation between IP and IF 680°C results [21].

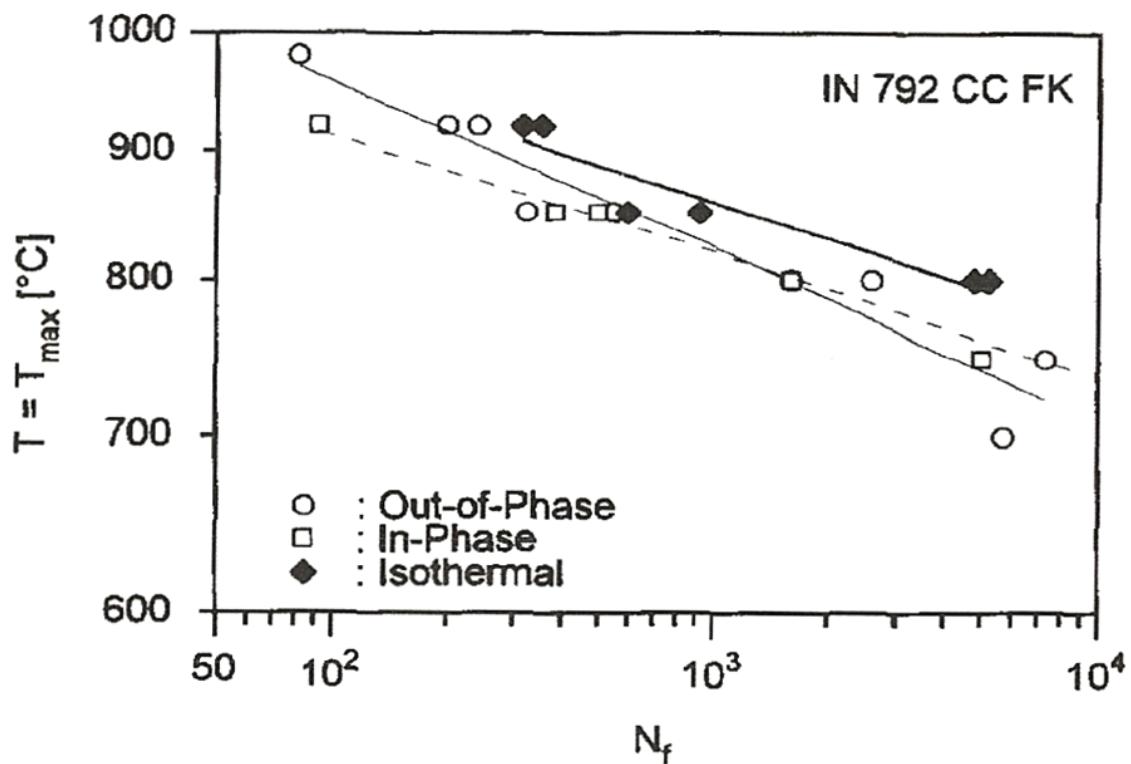


Figure 10: Comparison of the effects of IF, IP and OOP loading in IN792 over the peak temperature range 700-1000°C. Strain range = 1%. [24]