REJUVENATION OF SERVICE-EXPOSED

IN 738 TURBINE BLADES

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Abstract

A HIP rejuvenation study for an aluminide coated, internally cooled IN 738, turbine blade from an aero engine is described. The study, which involved assessing changes in blade shape, microstructure and creep properties of the airfoil portions of the blades as a function of service time, has shown that airfoils have a tendency to lengthen along their longitudinal axis during service and also that the rate of lengthening increases with increasing service life. The paper discusses on that basis, when to apply HIP rejuvenation for cost-effective blade life extension. It is also shown that the microstructure of the blades is substantially modified by service. The γ' particles coarsen through agglomeration, continuous networks of $M_{23}C_6$ carbides form along the grain boundaries and the blade surface along the internal cooling passages oxidizes. The design of a HIP rejuvenation/heat treatment cycle to recover microstructures and creep properties is discussed. Finally, a rejuvenation cycle that incorporates a diffusion treatment for recoating the blade is proposed that is shown to restore the loss of creep ductility induced by service, while improving time to rupture by a factor of 3 relative to new blades.

Introduction

Ni-base superalloys are used as investment cast turbine blades in many aircraft gas turbine engines. In their conventionally cast polycrystalline equiaxed forms, these alloys derive their high temperature strength from the precipitation of γ' within the grain interiors, as well as from the precipitation of γ' and $M_{23}C_6/M_6C$ carbides at the grain boundaries, the formation of a serrated grain boundary structure and the segregation of trace elements such as B and Zr at the grain boundaries[1]. At service temperatures, microstructural changes occur in blade airfoils which decrease their creep strength and promote airfoil distortion[2]. These time dependent microstructural changes include coarsening of the γ' phase, changes in the grain boundary and grain boundary carbide morphologies and precipitation of brittle intermetallic phases such as σ-phase[2,3]. The loss of creep strength eventually promotes temperature and stress assisted creep cavitation which leads to internal cracking and ultimately to failure. While the majority of microstructural changes can be reversed by reheat treatments, the elimination of creep cavities is only possible through hot isostatic pressing, (HIPing)[4,5]. During HIPing, the blades are subjected to the simultaneous application of high temperature and high inert gaseous pressure, such that the cavities collapse and the cavity walls diffusion bond together. HIPing must be followed by heat treatment for the optimization of microstructure and creep properties[5,6].

In this paper we discuss the results of a rejuvenation case study conducted on an aluminide coated and internally cooled IN 738 aero engine blades with uncoated internal cooling passages. The objectives of the paper are threefold. Firstly, the paper discusses the basis for establishing the need for rejuvenation for a given blade set and for deciding at what stage of service life to apply the rejuvenation for safe and cost effective blade life extension. Secondly, the paper presents a strategy for designing a HIPing rejuvenation cycle for conventionally cast IN 738 turbine blades and thirdly, it discusses problems associated with applying rejuvenation technology to coated blades.

Experimental Materials and Methods

Three blade sets with service times since new (TSN) of 0h, 4200h and 8400h were evaluated in terms of blade lengthening and microstructural damage in the top, middle and bottom sections of the airfoil and in terms of airfoil creep or stress rupture properties. The creep tests were conducted over a range of stresses (70 to 315 MPa) and temperatures (800 to 927°C), using miniature flat specimens machined from airfoils near the trailing edge (concave side) of the blades. The specimen geometry and dimensions are shown in Fig. 1. The specimen thickness was limited by the airfoil wall thickness (1.14mm) of the hollow blades. The gauge length of the miniature specimen corresponded to the mid-airfoil section of the blades. Extensometers were attached to the specimen grips for monitoring creep elongation with the help of a linear voltage digital transducer.

Results and Discussion

Blade Lengthening

A survey of user experience has indicated that, as service time accumulates on these IN 738 blades, their twisted airfoil sections have a tendency to untwist, while the blades lengthen along their longitudinal axis. A plot of reported increase in blade length as a function of blade

Figure 1 (drawing labels):
30.7 mm
2.54 mm
3.18 mm DIA.
7.62 mm
10.00 mm
2.4 – 2.7 mm R
0.51 – 0.56 mm
1.52 mm
22 mm

Figure 2 (chart labels):
10
● EXPECTED TREND
▲ CANADIAN EXPERIENCE
BLADE LENGTHENING IN mm X 10^{-1}
8
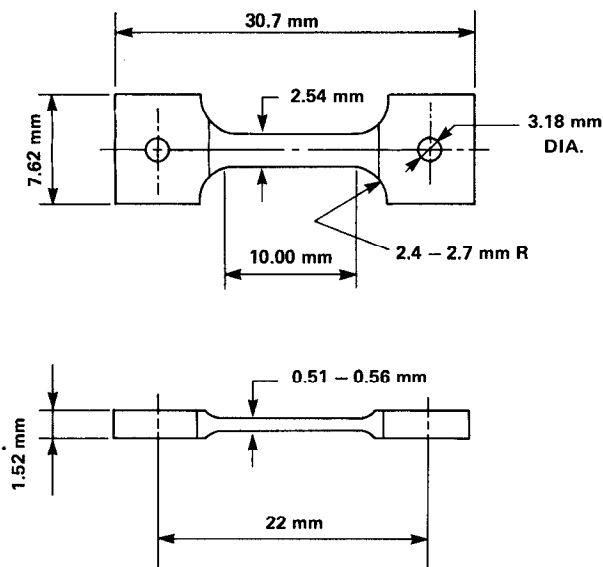REJUVENATE
6
4
2
0   2   4   6   8
BLADE LIFE IN h X 10^3

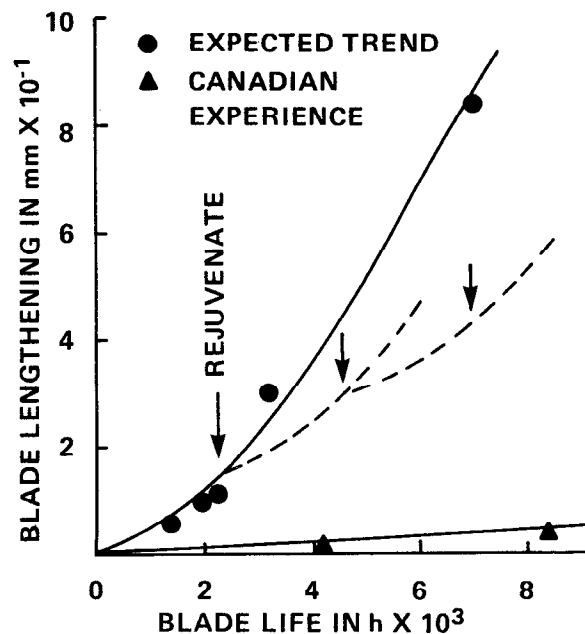Figure 1. Drawing of the miniature creep-rupture specimen machined from the trailing edge of the blade airfoils.

Figure 2. Diagram of blade lengthening versus blade service life illustrating how to decide when to HIP rejuvenate.

life is shown in Fig. 2. Rejuvenation would be expected to decrease the rate of airfoil growth and increase the life expectancy of the blades through the recovery of microstructure and creep properties. For example, assuming that the rejuvenation treatment is applied every 2300 hours, and that the creep properties are fully recovered each time, the blades would lengthen by only 0.43 mm over 7000 hours of service as compared to 0.84 mm for a blade with the same amount of service and no rejuvenation applied, Fig. 2. For a given lengthening allowable, blade life could be substantially increased in this manner. In order to decide when to apply the treatment for cost effective rejuvenation, airfoil growth rates and growth limits should be known. In the present case, less frequent than every 2300 hour rejuvenation would not be attractive because the rate of airfoil growth beyond that time becomes excessively large, as shown in Fig. 2. Furthermore, if the blade growth limits are too small, the rejuvenation may not be cost effective because the blades may be nearing their allowable growth limits after the first engine overhaul. Finally, since rejuvenation will not restore the original dimensions of the blades, the potential impact of dimensional changes for instance on engine performance or vibration characteristics should also be addressed.

The service exposed blades in the present study exhibited much lower lengthening values than those reported by other users, Fig. 2, which indicates that their operating conditions must have been less severe. The rejuvenation study was nevertheless conducted to establish whether any service-induced reduction in creep ductility of the blades, due for instance to grain boundary embrittlement, could be circumvented.

Microstructural damage

Considerable primary and secondary γ' coarsening and agglomeration was observed in the mid-airfoil sections of the service-exposed blades, Fig. 3, but no creep cavities were detected during optical and SEM examination. However, this does not rule out the possibility that
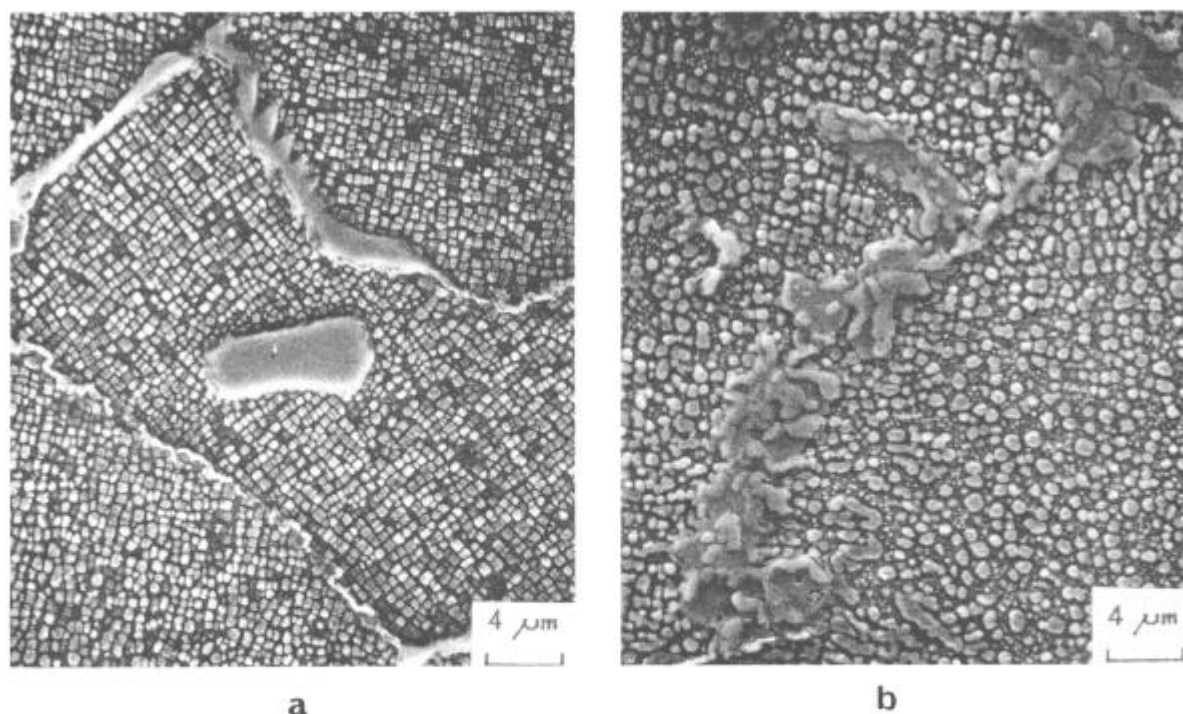
<div align="center">a   b</div>

Figure 3. Microstructures of the IN 738 blades in a) the new, fully heat-treated condition and b) after 8400 hours of service in an aircraft engine, illustrating that service induces coarsening of the γ' precipitate phase and modifies the morphology of the grain boundaries. (SEM micrographs).

ultrafine creep cavities were present that were not resolved by the optical or SEM techniques employed[7]. There was also some evidence of continuous networks of grain boundary $M_{23}C_6$ carbide formation in the service exposed blades, Figs. 3.

Both batches of service exposed blades had formed oxides along the uncoated walls of the internal cooling passages within the blade airfoils, where oxides were observed to have formed over a depth of approximately 25 μm, with some intergranular oxide spikes penetrating as deep as 125 μm below the surface, Fig. 4. None of these microstructural changes are unusual for blades after long time service in the oxidizing atmosphere of a gas turbine. Finally, shrinkage cavities, an inherent feature of investment cast components, were observed in both new and service exposed blades, Fig. 5.

Design of HIP Rejuvenation Cycles

The HIP plus reheat treatment conditions were selected on the basis of past experience with IN 738[8], IN 738LC[2], Inconel 700[9], Inconel X-750[1], Alloy 713C, Nimonic 105[5], Nimonic 115[9] and Nimonic 80A[17] turbine blades, which has shown that:

(a) The HIPing temperature should lie above the γ' and $M_{23}C_6/M_6C$ solvus temperature but preferably below the MC solvus temperature and should be selected to avoid incipient melting. HIPing above the γ' and $M_{23}C_6/M_6C$ solvus temperatures reduces resistance to plastic flow and ensures complete closure of shrinkage and/or creep cavities, while keeping the HIP temperature below the MC solvus temperature prevents rapid grain growth that otherwise occurs in these materials.
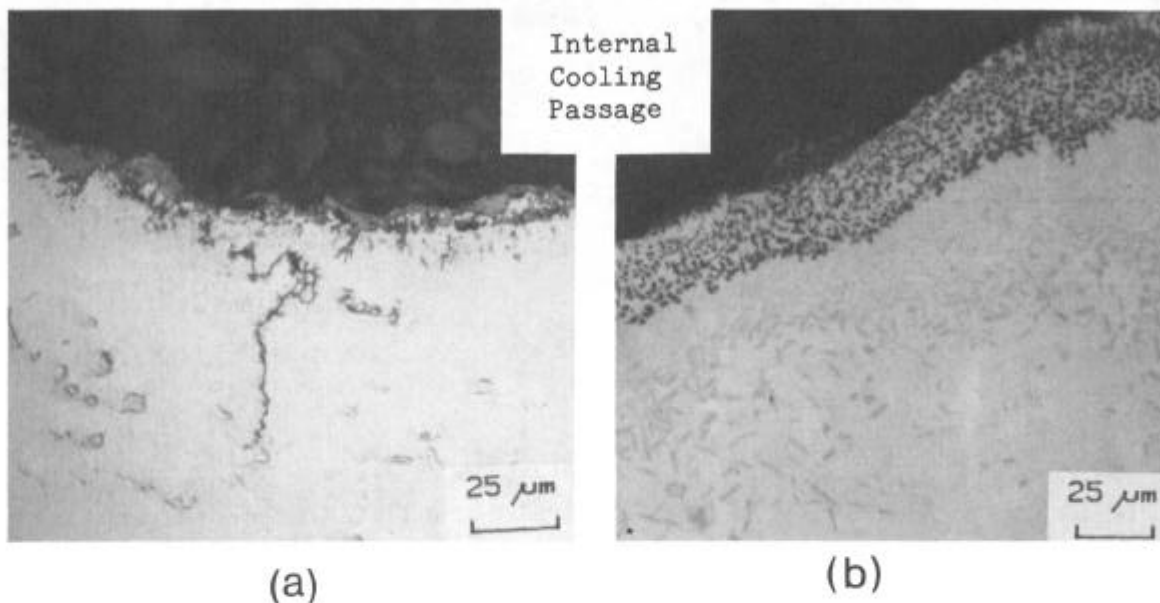
Figure 4. Evidence of oxidation of the internal cooling passages in service-exposed blades a) oxide spike penetrating to a depth of approximately 125 μm and b) oxidized and alloy depleted surface layer roughly 25 μm thick lining the walls of the cooling passages.

Rapid grain growth in IN 738 commences at 1225°C. Grain growth reduces the grain boundary area available for carbide precipitation which in turn can lead to continuous carbide film formation along the grain boundaries during post HIP ageing treatments, thereby embrittling the grain boundaries[6,9,11]. In addition, HIPing in the MC solvus temperature range can dissolve sulphocarbides and the free S can segregate at the grain boundaries[5], which can decrease the grain boundary cohesive strength.

(b) HIPing above the γ' solvus temperature destroys the original serrated grain boundary structure and, a controlled cool from the HIPing or post HIPing solution treatment temperature through the γ' precipitation range is necessary to reproduce the serrations[8,12]. Serrated grain boundaries suppress grain boundary sliding[13], the deformation mechanism that predominates at service stresses and temperatures[14]. Therefore, reproducing the serrated grain boundaries during rejuvenation is extremely important, Fig. 6.

(c) The post-HIPing aging treatments for controlled precipitation of primary and secondary γ' precipitates are usually (but not necessarily) the same as those designed for the virgin alloy. In a number of older Ni-base alloys, γ' precipitate sizes and distributions have been optimized on the basis of short term creep testing where the dominant deformation mechanism may not be the same as that operative at service stresses and temperatures[14].

Based on this past experience, three HIPing rejuvenation cycles were considered for eliminating the shrinkage cavities and rejuvenating the damaged microstructure of the service exposed blades. The three cycles consisted of:

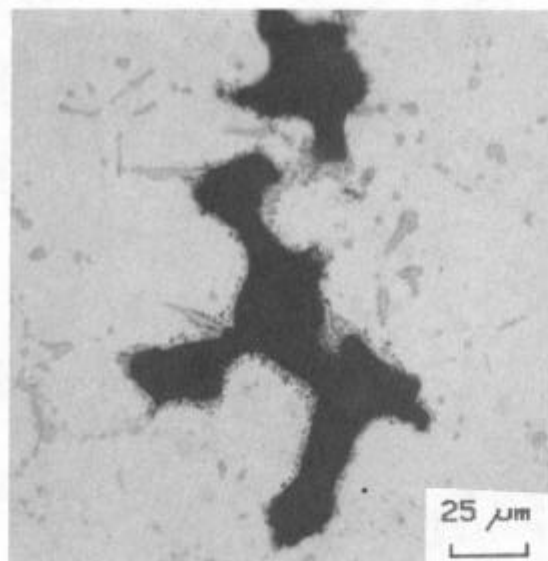Cycle 1: HIPing[8] + the standard high temperature-low activity aluminide coating cycle.

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Figure 5. Evidence of shrinkage microporosity present in the airfoil sections of the blades.
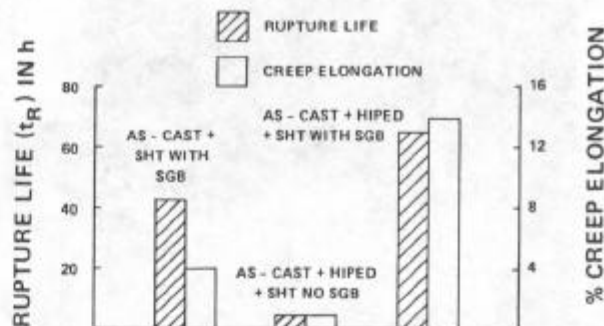

Figure 6. Effects of serrated grain boundaries on stress-rupture properties of IN 738 at 586 MPa and 760°C in the standard heat treated conditions. Serrated grain boundaries are formed by controlling cooling rate from the solution treatment temperature.(8)

Cycle 2:   HIPing + a silicon modified low temperature-high activity aluminide coating cycle.

Cycle 3:   HIPing at 1200°C/2h/105MPa/F.C. + Solution treat at 1200°C/2h controlled cool⟶ 1130°C/AC + the slurry coating heat treatment cycle.

All HIPing rejuvenation cycles were designed to avoid primary MC carbide dissolution, suppress rapid grain growth and regenerate the original serrated grain boundary structures. Cycle no. 1 produced a uniform distribution of primary and secondary γ' precipitates, Fig. 7(a), whereas cycle no. 3 produced a bimodal distribution of secondary spherical γ' with a mixture of $M_{23}C_6$ and MC carbides at the grain boundaries, Fig. 7(c). Cycle no. 2 which incorporated the slurry coating diffusion treatment of cycle no. 3 also incorporated a hold time at 1120°C and this produced some primary γ' precipitates in addition to the biomodal distribution of secondary spherical γ' precipitates, Fig. 7(b).

In some rejuvenated blades, the severity of oxide penetration along the walls of the internal cooling passages appeared to have increased during HIPing or post-HIPing heat treatments, Fig. 8. This increase could be due to contaminants present within the atmospheres of the HIP vessel or heat treatment furnaces used. Oxide penetration and spalling can reduce the load bearing capabilities of blades and intergranular oxide spike formation can locally increase the stress concentrations, thus increasing the risks of in-service stress-rupture failures. The influence of oxide formation was not studied in detail because, while machining the creep specimens, the oxides were removed from the gauge length.

Creep properties

The rupture life (t_R) of the new, service exposed and rejuvenated blades are presented in the form of a Larson-Miller (L-M) plot in Fig. 9. The t_R data for all materials generally fall within the scatterband for
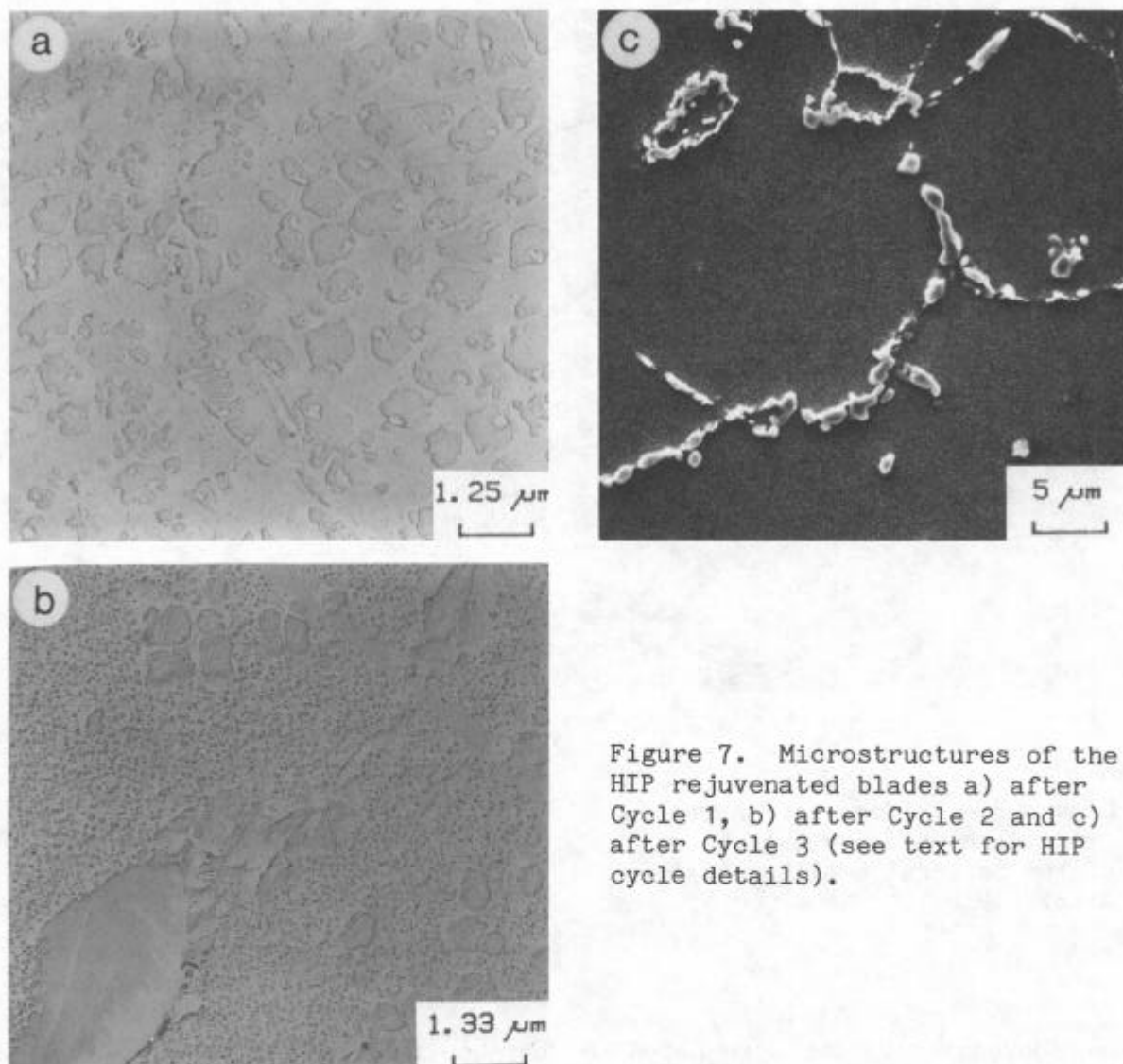
Figure 7. Microstructures of the HIP rejuvenated blades a) after Cycle 1, b) after Cycle 2 and c) after Cycle 3 (see text for HIP cycle details).

the new blades although some data points from blades subjected to rejuvenation cycle no. 1 fall below the lower bound at lower stresses, Fig. 9. Recent investigations have shown that the use of parametric functions is not the best method for assessing creep damage in turbine blades(2,5). Instead, it has been suggested that creep testing parameters should be selected in a manner such that a single deformation mechanism remains dominant under a given set of creep stresses and temperature. Information from these creep tests can then be used to decide whether resistance to a specific creep deformation mechanism has been restored in the rejuvenated blades(2). Following this suggestion, several creep tests (6 tests per material condition) were conducted at a low temperature and high stress (800°C/315MPa) where intragranular deformation is likely to predominate and at high temperature and low stress (927°C/76MPa) where grain boundary sliding is expected to predominate.

Relative to new blades, the service exposed blades showed a marginal reduction in t_R and a slight increase in rupture strain (ϵ_R) at 800°C/315MPa, Fig. 10a. The rejuvenation cycles nos. 1 and 2 improved t_R with a marginal decrease in ϵ_R, Fig. 10a. Assuming intragranular deformation prevailed under these testing conditions, these trends in t_R and ϵ_R are to be expected because service exposed blades contained coarse γ' precipitates whereas rejuvenation cycle nos. 1 and 2 considerably refined the γ' relative to both new and service exposed blades, thereby increasing grain strength. The minimum creep rates ($\dot{\epsilon}_m$)

Figure 8. Evidence of increased oxide penetration along the walls of the internal cooling passages after HIPing.
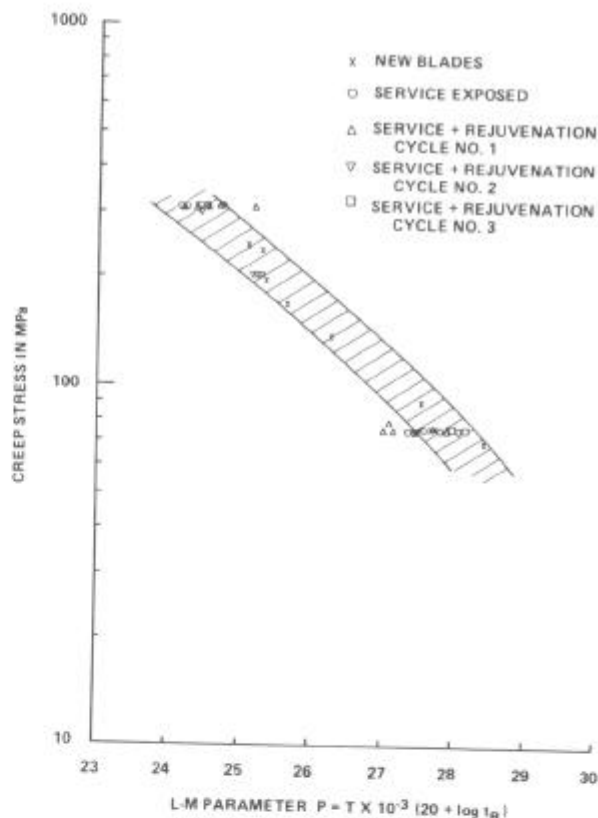
Figure 9. Larson-Miller parameter plots for new, service-exposed and HIP rejuvenated IN 738 blades.

for rejuvenated blades were noted to decrease by a factor of 5 relative to the new and service exposed blades which lends support to this argument.

At 927°C/76MPa, where grain boundary sliding was expected to predominate, the service exposed blades revealed a dramatic reduction in ϵ_R with a marginal increase in t_R relative to new blades, Fig. 10b. Since service exposed blades contained continuous networks of grain boundary $M_{23}C_6$ carbides, the reduction in ϵ_R under grain boundary sliding condition ought to be expected. It is however surprising to note that rejuvenation cycle no. 1 did not restore ϵ_R and t_R to as new condition whereas rejuvenation cycle no. 2 not only restored ϵ_R but improved t_R by a factor of 3 relative to new blades, Fig. 10b. It is suggested that these differences are related to differences in substrate microstructure and composition arising from differences in processing conditions during recoating of the blades.

The improved creep properties of blades subjected to rejuvenation cycle no. 2, where a high aluminum activity Si modified aluminide coating was applied, can be attributed to the precipitation of discrete grain boundary carbides and a uniform refinement of γ'. During grain boundary sliding, deformation is not confined to the grain boundary plane alone but is accommodated within a finite zone adjacent to the grain boundaries by intragranular flow(14). Thus, γ' refinement would be expected to decrease creep rate and increase t_R, whereas discrete grain boundary carbides would be expected to delay fracture and improve ϵ_R.
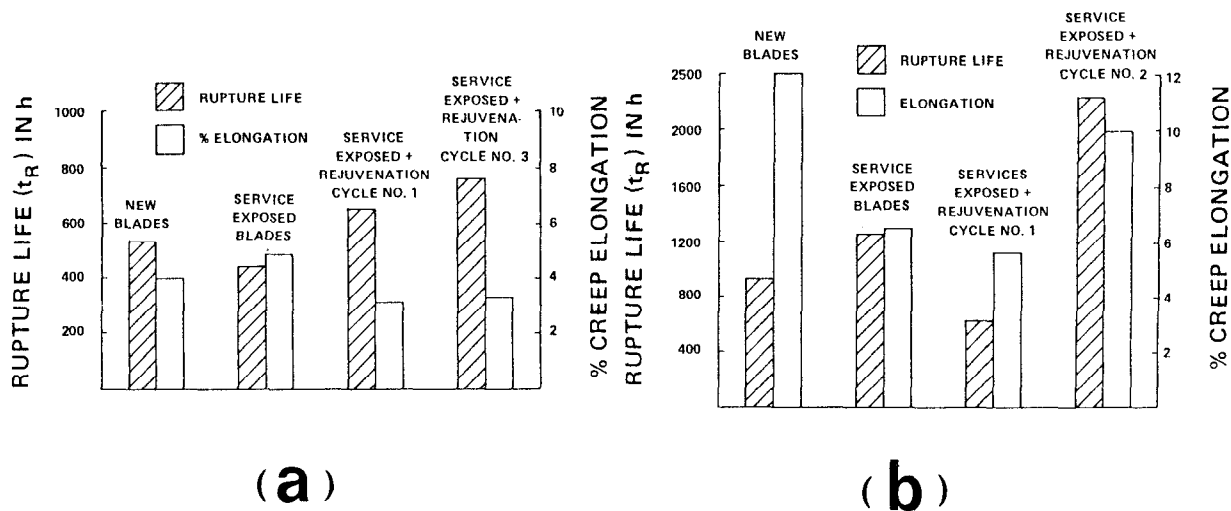
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Figure 10. Effects of service exposure (8400 hours) and rejuvenation treatments on the stress-rupture properties of IN 738 at a) 315 MPa and 800°C and b) 76 MPa and 926°C.

In contrast, blades subjected to rejuvenation cycle no. 1, where a low activity aluminide coating was applied, Ni and Cr are known to diffuse into the coating[15]. Since the blades were recoated as part of the rejuvenation treatment, the Cr content of the substrate may have been reduced considerably to cause premature creep failure during testing in air due to accelerated oxygen attack along the grain boundaries. Investigations are currently underway to validate this hypothesis.

Conclusions

The intrinsic loss of creep ductility induced by service in an IN 738 blade material can be fully recovered by HIP rejuvenation while time to rupture relative to new blades can actually be improved, providing post-HIP heat treatments are carefully designed. Internal surface degradation along blade cooling passages during HIP or post-HIP processing is however a concern that needs to be addressed.

Acknowledgements

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