

Improved Creep Life Prediction of Turbo-charger Turbine Wheels Using Measured Operating Temperature and Grain Size Effects

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Abstract

With the increasing demand for carbon neutrality, highly efficient internal combustion engines operating at stoichiometric air-fuel ratios ($\lambda = 1$) over the entire operating range have become increasingly widespread. Consequently, turbochargers and their key components, turbine wheels, are exposed to higher temperatures and mechanical loads than ever before. Furthermore, the application of variable nozzle (VN) turbochargers to gasoline engines has increased in order to recover engine output and improve low-speed torque under stoichiometric operation. Under such operating conditions, turbine wheels are subjected not only to elevated exhaust-gas temperatures and higher rotational speeds but also to high-frequency vibrations caused by VN vane passing. Therefore, material and microstructural designs that simultaneously provide excellent creep resistance and high-cycle fatigue (HCF) resistance are required.

In this study, the accuracy of CAE-based life assessment was improved by incorporating a temperature distribution obtained through CFD calibration using experimentally measured temperature data of a turbine wheel, together with creep properties considering location-dependent grain-size variations within the turbine wheel. Based on the proposed methodology, guidelines for optimum life design as well as material and microstructural design of next-generation high-temperature turbocharger turbine wheels capable of withstanding severe thermal and mechanical loading conditions are presented.

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1 Introduction

In recent years, gasoline engines for automotive applications have increasingly adopted stoichiometric operation ($\lambda = 1$) over the entire operating range, including high-load conditions, in order to improve fuel economy and exhaust emissions performance. In particular, turbocharged engines, which are the focus of the present study, achieve high power density by effectively utilizing exhaust-gas energy. At the same time, appropriate control of exhaust-gas temperature under high-load operation is essential. Therefore, the Miller cycle has been widely adopted because it can improve thermal efficiency and reduce exhaust-gas temperature without relying on fuel enrichment.

On the other hand, as the specific output of turbocharged engines continues to increase, exhaust-system components, including turbocharger turbine wheels, are required to maintain reliable performance under increasingly severe high-temperature conditions. To meet these requirements, two approaches are available: the adoption of more heat-resistant materials and the effective utilization of conventional heat-re-

sistant materials by maximizing their performance and extending their applicable temperature range. The latter approach is particularly attractive because it can suppress the increase in CO₂ emissions associated with the manufacturing of advanced heat-resistant materials and therefore contributes to the reduction of environmental impact from a well-to-wheel perspective.

ALLOY 713C, a cast Ni-based superalloy, has been widely used for turbocharger turbine wheels because of its excellent balance of high-temperature strength, castability, and environmental performance during manufacturing. Therefore, extending the operating temperature range through more effective utilization of ALLOY 713C, rather than replacing it with more advanced superalloys, is of significant industrial importance. To achieve this objective, it is necessary not only to accurately determine the actual temperature and stress conditions experienced by turbine wheels in service, but also to establish a life-design methodology that accounts for variations in material properties while satisfying the required service life.

A number of studies have been reported on creep-life prediction and durability evaluation of turbocharger turbine wheels using high-temperature spin tests [1]. Furthermore, life-prediction methodologies considering creep-damage evolution have been proposed for aircraft turbine blades [2]. However, the influence of grain-size distributions formed during casting on creep life has not been sufficiently considered in these studies.

In general, the creep behavior of polycrystalline materials is strongly affected by grain size, and creep rates are known to decrease with increasing grain size [3]. In aircraft gas turbines, directional solidified and single-crystal superalloys are employed to suppress grain-boundary-related high-temperature damage [4]. In contrast, automotive turbine wheels exhibit location-dependent grain sizes due to variations in solidification conditions during casting. In particular, grain refinement tends to occur in thin blade-tip regions because of their higher solidification rates [5]. Therefore, quantifying the grain-size distribution within actual turbine wheels and incorporating its influence into creep-life assessment is expected to improve the accuracy of rational life design for ALLOY 713C turbine wheels.

In this study, a creep-life design methodology considering both actual operating temperatures and grain-size effects was investigated for turbine wheels manufactured from ALLOY 713C. The temperature distribution was identified through CFD calibration based on experimentally measured turbine-wheel temperatures, and location-dependent creep properties were incorporated into the life assessment. Furthermore, creep life and fatigue strength were comprehensively evaluated to establish design guidelines for turbine wheels capable of operating under the increasingly severe thermal environments expected in future high-temperature gasoline engines.

2 Turbocharger Configuration and Target Component

Fig. 1 shows a typical cross-sectional structure of a turbocharger. A turbocharger is a boosting device that recovers exhaust-gas energy and supplies compressed air to an engine. The compressor side consists of a compressor impeller and a compressor housing, whereas the turbine side consists of a turbine wheel and a turbine housing. The compressor impeller and turbine wheel are connected by a common shaft, and the exhaust-gas energy recovered by the turbine wheel is used to drive the compressor impeller.

The bearing system supporting the shaft, the lubricating oil passages, and the cooling-water passages used to cool the turbocharger are located within the bearing housing.

The turbine wheel, which is the focus of the present study, is directly exposed to high-temperature exhaust gases and is therefore susceptible to durability issues such as creep damage and high-cycle fatigue (HCF). In particular, the continuing increase in the specific output of modern turbocharged engines requires turbine wheels to maintain durability under increasingly severe high-temperature operating conditions. Accordingly, this study investigates a life-design methodology for ALLOY 713C turbine wheels that considers both actual operating temperatures and grain-size effects.

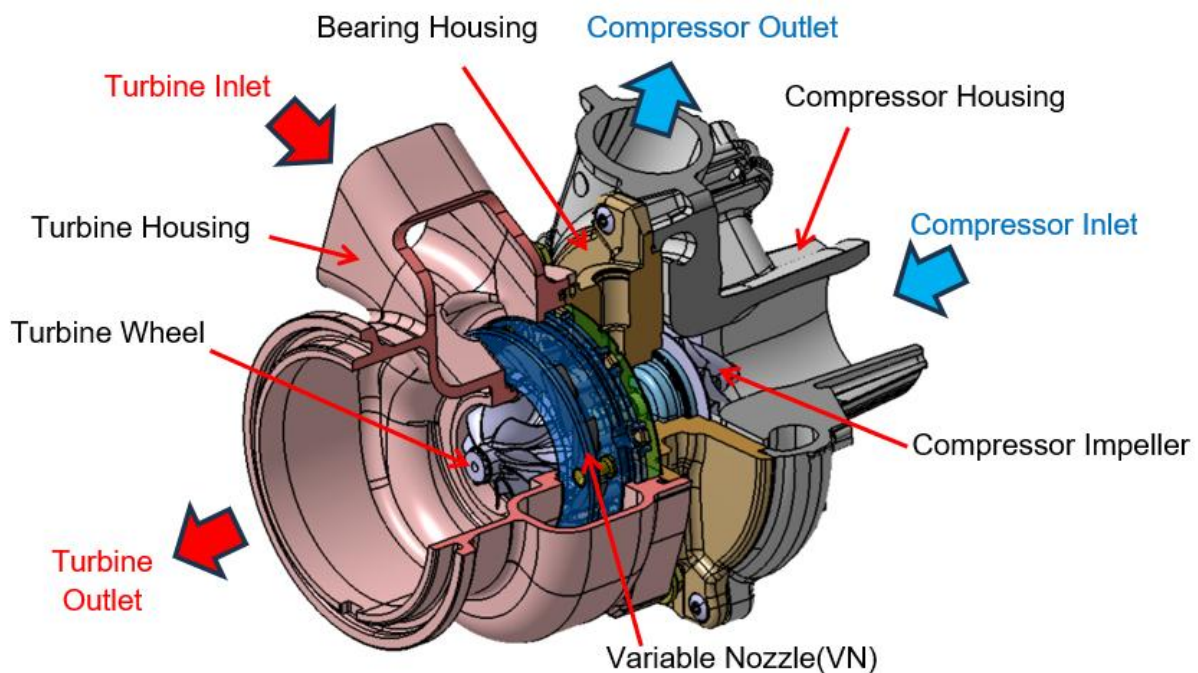


Fig. 1 Typical cross-sectional configuration of a turbocharger.

3 Design Procedure

Fig. 2 shows the design procedure employed in this study. The workflow on the left side of the figure illustrates the procedure used to improve the accuracy of turbine-wheel temperature prediction.

4.1) To identify the thermal boundary conditions of the turbine wheel, the temperatures of an actual turbine wheel were measured using a Thermal History Coating (THC). In addition, exhaust pressure pulsations and exhaust-gas temperatures were measured.

4.2) An engine performance simulation model was developed using the one-dimensional (1D) engine simulation software GT-SUITE. The model was calibrated so that the calculated exhaust pressure pulsations and exhaust-gas temperatures agreed with the measured values.

4.3) Computational fluid dynamics (CFD) analysis was performed using STAR-CCM+ to calculate the thermal-flow field around the turbine wheel and the resulting temperature distribution within the turbine wheel.

4.4) The temperatures predicted by the CFD analysis were compared with the temperatures measured using THC to validate the proposed turbine-wheel temperature analysis methodology.

The workflow on the right side of the figure illustrates the procedure used to establish a creep material model considering the grain-size distribution in an actual turbine wheel.

5.1) The microstructure of an actual turbine wheel was observed, and the grain-size distribution at different locations was investigated.

5.2) Using the creep database published by the National Institute for Materials Science (NIMS), the effect of grain size on the minimum creep rate was quantified and incorporated into the creep-life assessment methodology.

5.3) The influence of grain size on creep rupture life was evaluated using the same database.

5.4) Based on these evaluation results, a grain-size-dependent creep material model was developed, and material data for finite element analysis were generated for each grain size.

Next, the temperature analysis methodology and the grain-size-dependent creep material model were combined to predict creep life and verify actual turbine-wheel failures.

6.1) The proposed thermal boundary condition identification method and grain-size-dependent creep material model were applied to a turbine wheel that had experienced actual creep failure.

6.2) Creep life analysis of the turbine wheel was performed using the finite element analysis software Abaqus.

6.3) The damage location predicted by the analysis was compared with the actual failure location to validate the proposed life-design methodology.

6.4) The effects of grain size were evaluated from both creep-life and fatigue-strength perspectives, and guidelines for grain-size design of turbine wheels suitable for high-temperature applications were established.

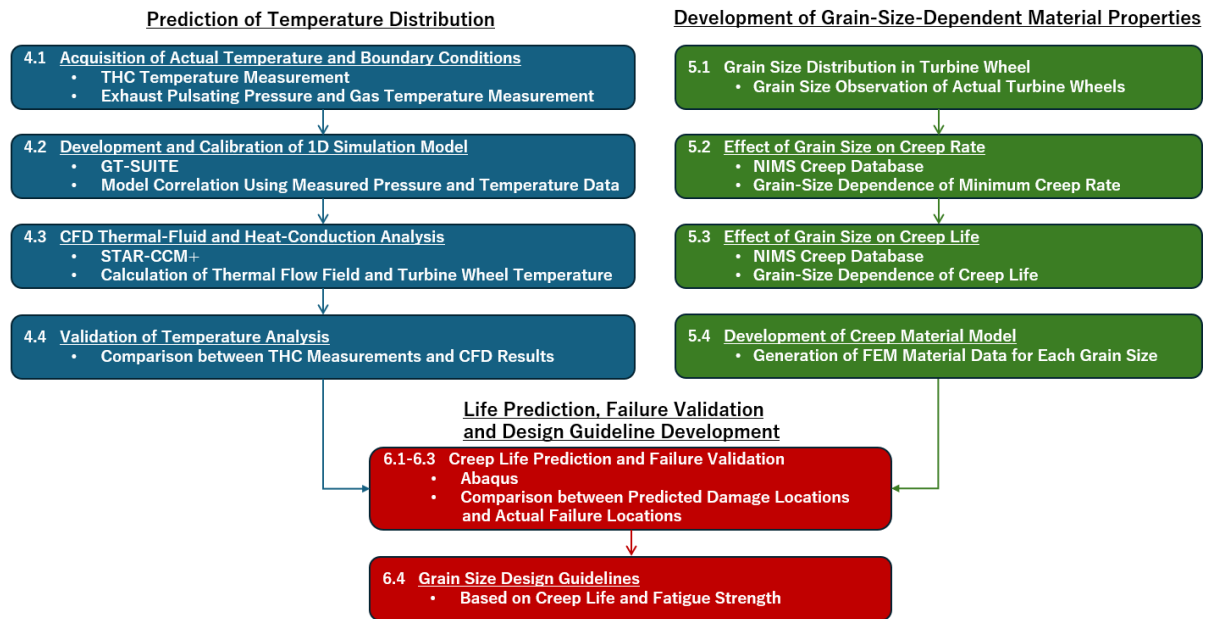


Fig. 2 Evaluation procedure for creep life prediction of turbine wheel

4 Prediction of Temperature Distribution

4.1 Acquisition of Actual Temperature and Boundary Conditions

To measure the turbine-wheel temperature, a test engine equipped with a variable nozzle (VN) turbocharger was used. The measurements were conducted at 5000 rpm under wide-open-throttle (WOT) conditions. A Thermal History Coating (THC) was applied to evaluate the turbine-wheel surface temperature. THC is a temperature-sensing technique that determines the maximum temperature experienced by a component based on changes in luminescence characteristics caused by thermal exposure. In recent years, this technique has been applied to the evaluation of temperature distributions in turbocharger compressor impeller and turbine wheels [6,7].

The measured gas temperatures at the turbine inlet and outlet are shown in Fig. 3, while the measured pressure pulsations at the turbine inlet and outlet are shown in Fig. 4. These measured gas temperatures and pressure pulsations were used as boundary conditions for the one-dimensional (1D) simulation and CFD analysis described later.

The turbine-wheel surface temperature distributions obtained using THC are shown in Figs. 5(a) and 5(b). Temperature data were acquired at 2,522 measurement points, enabling evaluation of the temperature distribution over the entire circumference of the turbine wheel. The highest temperature was observed near the blade inlet region. In contrast, the disk region exhibited normalized temperatures approximately 0.2 to 0.3 lower than those at the blade inlet region. This is considered to be because the blade

inlet region is directly exposed to high-temperature exhaust gas, whereas the disk region is cooled by both the cooling water and lubricating oil supplied through the bearing housing. Such a temperature distribution is consistent with the turbine-wheel temperature distributions reported in the literature using THC measurements [7]. These results indicate that a significant temperature gradient exists within the turbine wheel.

To facilitate comparison between the THC measurements and the heat-transfer analysis results described later, the measured THC temperature data were circumferentially averaged. The averaged temperature distribution was then mapped onto the finite-element mesh using the nearest-neighbor interpolation method. The resulting mapped temperature distribution is shown in Fig. 6.

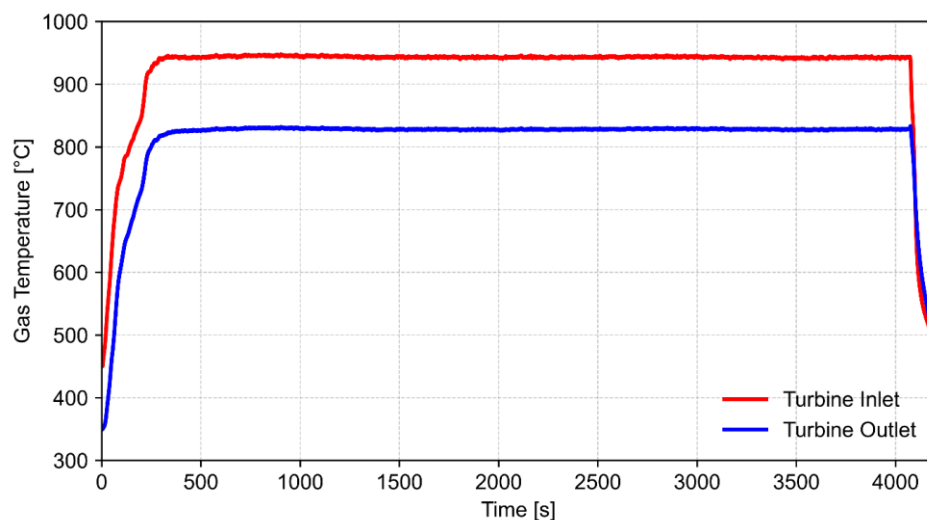


Fig. 3 Measured turbine inlet and outlet gas temperatures

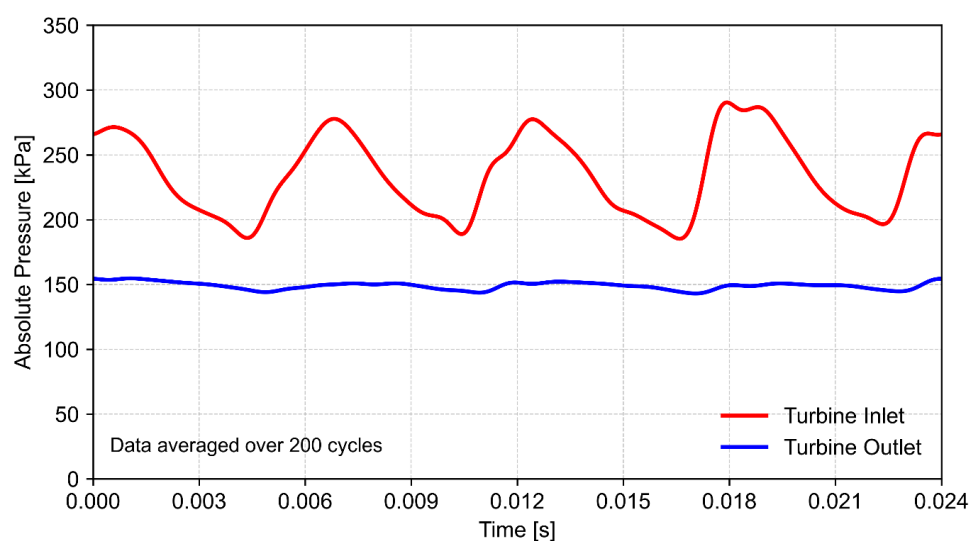


Fig. 4 Measured turbine inlet and outlet pulsating pressures

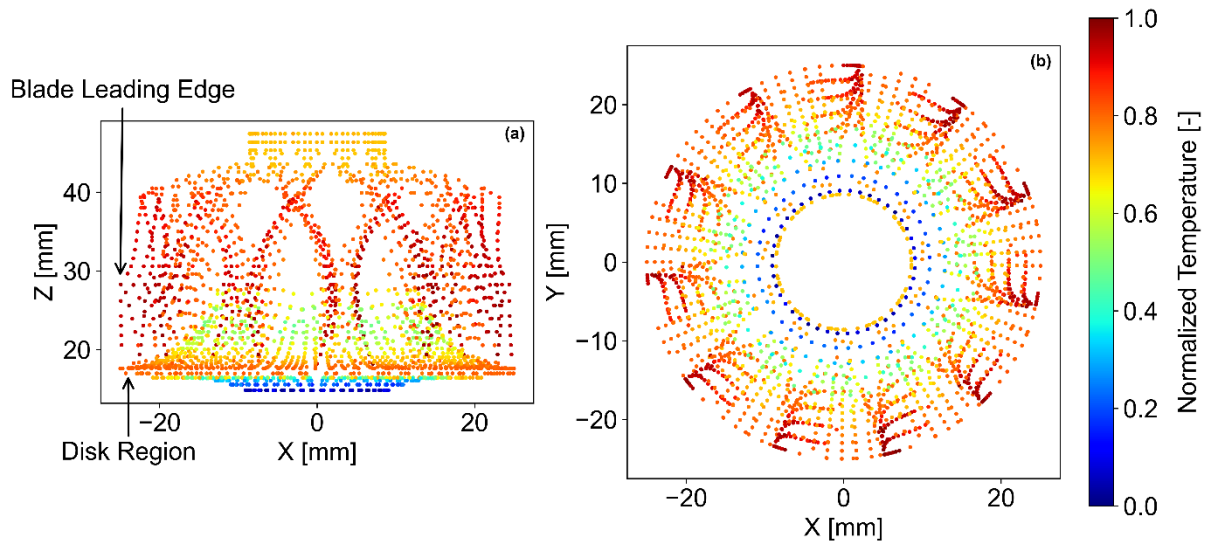


Fig. 5 Normalized turbine wheel surface temperature distribution measured by THC: (a) X-Z view and (b) X-Y view

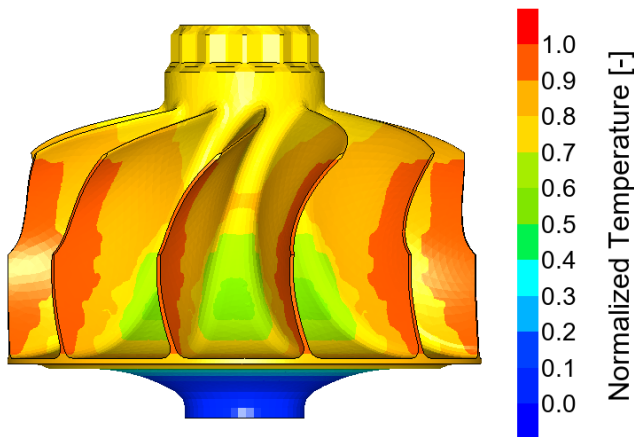


Fig. 6 Circumferentially Averaged THC Temperature Distribution Mapped onto FEM Mesh

4.2 Development and Calibration of 1D Simulation Model

To reproduce the measured turbine inlet and outlet pressure pulsations and gas temperatures obtained during turbine-wheel temperature measurements on a test engine equipped with a variable nozzle (VN) turbocharger, a 1D simulation model was calibrated. The measurement locations for the turbine inlet and outlet pressure pulsations, mean pressures, and steady-state gas temperatures are shown in Fig. 7.

A comparison between the calculated and measured pressure pulsations at the turbine inlet and outlet is presented in Fig. 8. In addition, the calculated and measured mean pressures and steady-state gas temperatures at the turbine inlet and outlet are summarized in Tab. 1.

Good agreement was obtained between the simulation results and the measured data in all cases, confirming that the 1D simulation model used in this study can accurately reproduce the flow and thermal conditions in the exhaust system.

In the CFD analysis described later, the transient gas mass flow rate and gas temperature obtained from the calibrated 1D simulation were used as boundary conditions.

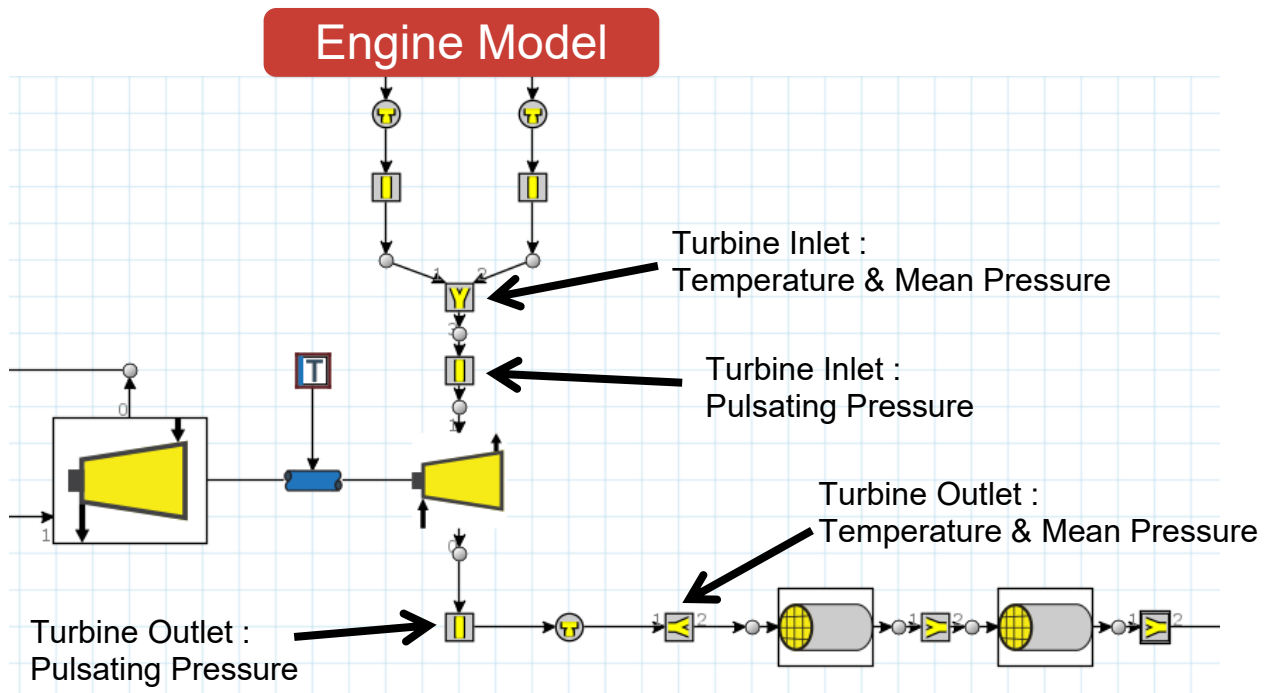


Fig. 7 Measurement locations used for calibration of the 1D simulation model

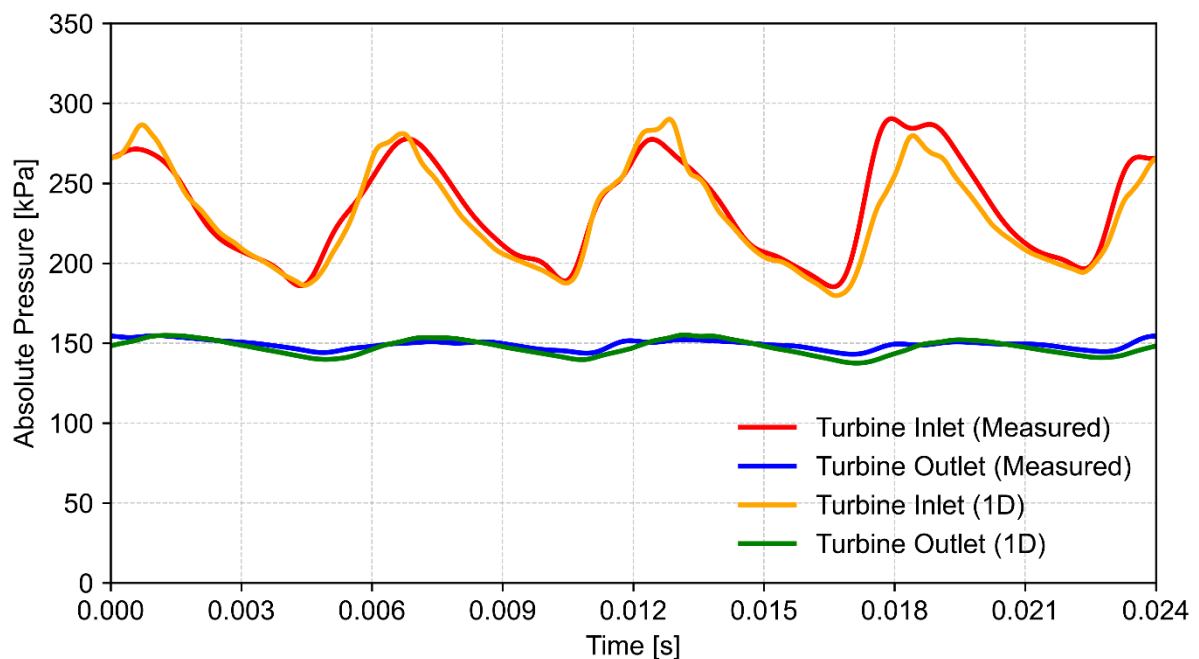


Fig. 8 Comparison of measured and predicted pulsating pressures

Tab. 1 Comparison of measured and predicted mean temperatures and pressures

	Turbine Inlet		Turbine Outlet	
	Temperature [°C]	Mean Pressure [kPa abs]	Temperature [°C]	Mean Pressure [kPa abs]
Measured	943.4	236.2	827	147.8
1D	943.8	240.0	836	147.4

4.3 CFD Thermal-Fluid and Heat-Conduction Analysis

To define the thermal-flow field around the turbine wheel, a CFD analysis was performed using STAR-CCM+. The analysis model and boundary conditions are shown in Fig. 9(a), (b), (c), and (d). The turbine inlet and outlet gas temperatures and gas mass flow rates obtained from the 1D simulation described in the previous section were used as boundary conditions. Because good agreement was obtained between the measured turbocharger rotational speed and the 1D simulation results, the rotational speed predicted by the 1D simulation was used in the present study. The $k-\omega$ SST turbulence model was employed [8]. In addition, the Multiple Reference Frame (MRF) method was adopted to represent the rotation of the turbine wheel. The computational domain included the entire turbocharger system, consisting of the gas passage, turbine wheel, turbine shaft, turbine housing, heat shield, bearing housing, cooling water, and lubricating oil.

First, an unsteady conjugate heat-transfer analysis was performed over one engine cycle, and the heat-transfer coefficient and fluid temperature at each solid surface were obtained. The cycle-averaged heat-transfer coefficient and fluid temperature were then calculated using Eq. 1 and Eq. 2, respectively.

$$\bar{h} = \frac{1}{N} \sum_{i=1}^N h_i \quad \text{Eq. 1}$$

$$\bar{T}_f = \frac{1}{N} \sum_{i=1}^N T_{f,i} \quad \text{Eq. 2}$$

Here, $\bar{h}$ is the cycle-averaged heat-transfer coefficient, $\bar{T}_f$ is the cycle-averaged fluid temperature, h_i is the heat-transfer coefficient at time step i , $T_{f,i}$ is the fluid temperature at time step i , and N is the total number of time steps within one engine cycle.

The calculated cycle-averaged heat-transfer coefficient and cycle-averaged fluid temperature were applied to all solid surfaces in contact with the fluid, and a steady-state heat-conduction analysis was performed. For solid surfaces exposed to ambient air, constant boundary conditions of an ambient temperature of 25 °C and a heat-transfer coefficient of 60 W/m²K were applied.

In this study, the unsteady thermal-fluid analysis and the steady-state heat-conduction analysis were repeated until the temperatures of all solid components converged.

The normalized temperature distribution of the entire turbocharger obtained from the analysis is shown in Fig. 10(a) and (b). A high-temperature region was formed around the turbine wheel owing to the influence of the hot exhaust gas. In contrast, the temperature decreased toward the bearing housing due to cooling by the cooling water and lubricating oil. As a result, a temperature gradient was formed from the turbine wheel toward the bearing housing.

This temperature distribution is in good agreement with previous analyses that considered heat transfer among the turbine wheel, heat shield, bearing housing, and turbine housing [9,10], as well as the effects of the surrounding thermal boundary conditions [11]. Therefore, accurate prediction of turbine-wheel temperatures requires thermal analysis of the entire turbocharger system, including the cooling water and lubricating oil.

For comparison with the THC measurements described later, the turbine-wheel temperature distribution obtained from the CFD analysis was circumferentially averaged and subsequently mapped onto the FEM mesh.

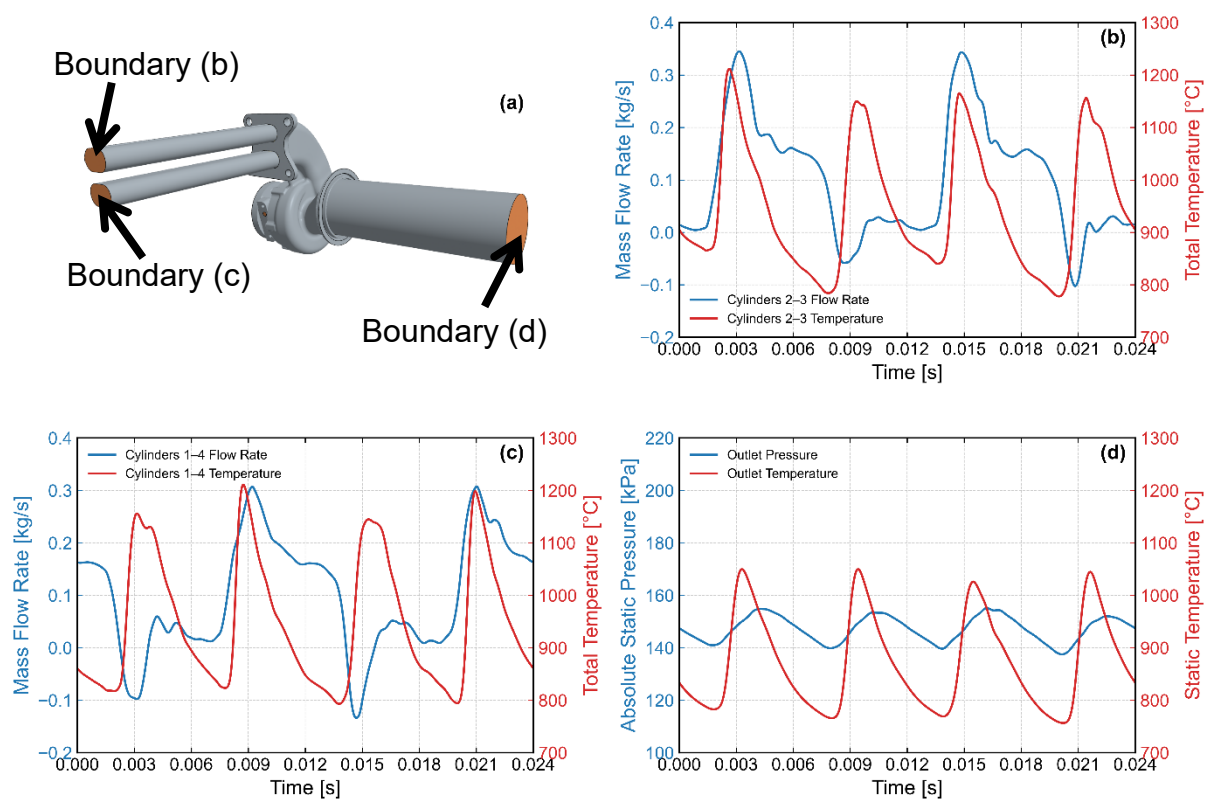


Fig. 9 CFD model and boundary conditions:
 (a) CFD model,(b) cylinders 2–3 inlet boundary condition,
 (c) cylinders 1–4 inlet boundary condition,(d) turbine outlet boundary condition.

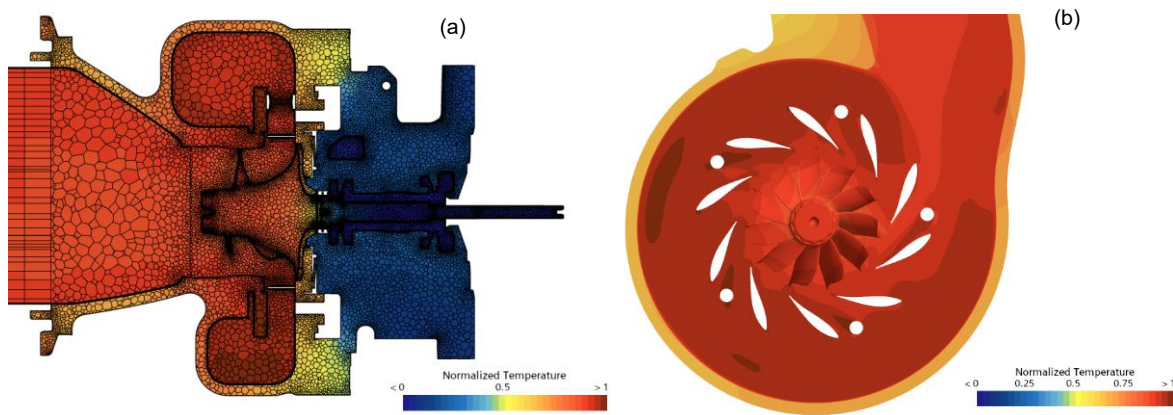


Fig. 10 Predicted temperature distribution in the turbocharger assembly:
(a) sectional view and (b) turbine-side view

4.4 Validation of Temperature Analysis

For validation of the CFD analysis, the temperature distribution predicted by CFD was compared with the THC measurement results. Fig. 11(a) shows the THC measurement results, while Fig. 11(b) shows the CFD results. Both results exhibited a similar trend, with the highest temperature located near the turbine wheel blade inlet and decreasing toward the shaft center. These results confirm that the CFD analysis successfully reproduced the overall characteristics of the temperature distribution.

Fig. 12 shows the temperature prediction error distribution between the THC measurements and the CFD results. Relatively large errors were observed in the lower shaft region of the turbine wheel, with some localized areas exceeding 10%. In contrast, in the blade region, which is the primary area of interest for creep damage assessment, the temperature prediction error was generally within approximately 4%, indicating that the temperature distribution in the blade region was reproduced with sufficient accuracy.

The larger errors observed in the shaft region can be attributed to two factors: (1) nearest-neighbor interpolation was applied to THC-unmeasured regions during the mapping of THC measurement data onto the FEM mesh, and (2) the limited number of THC measurement points around the shaft reduced the interpolation accuracy, resulting in locally amplified errors.

From these results, it was confirmed that the CFD-based heat transfer analysis developed in this study can accurately reproduce the temperature distribution of the turbine wheel. In particular, sufficient accuracy was achieved in the blade region, which is critical for creep damage assessment. Therefore, the proposed analysis method was considered suitable for application to the life prediction analyses presented in the subsequent Sections.

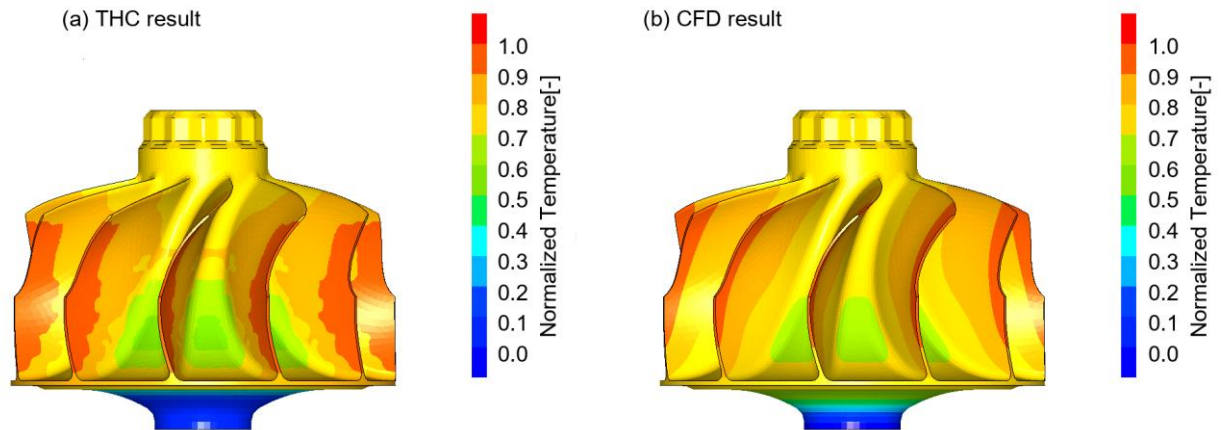


Fig. 11 Comparison of normalized temperature distributions obtained by THC (a) and CFD (b).

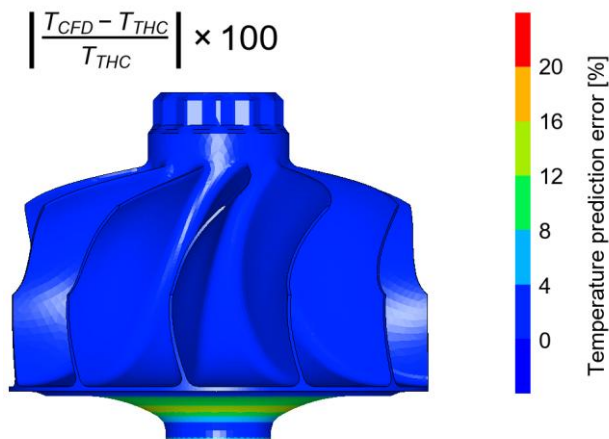


Fig. 12 Temperature difference between CFD and THC results.

5 Development of Grain-Size-Dependent Material Properties

5.1 Grain Size Distribution in Turbine Wheel

To evaluate the grain-size distribution in turbine wheels, metallographic observations were conducted on three different turbine wheels.

In this study, grain size was defined as the maximum grain length in the centrifugal-force direction in order to evaluate the grain size governing creep damage under centrifugal loading [12]. The definition of grain size is shown in Fig. 13.

The representative observation locations of the turbine wheel are shown in Fig. 14, and the microstructures of the three turbine wheels are shown in Fig. 15. The observations revealed that relatively coarse grains were formed in the central region of all turbine wheels, whereas fine grains were formed in the blade-tip region. This is attributed to the higher solidification rate in the blade-tip region due to its smaller wall thickness, which suppresses grain growth compared with the central region.

The measured grain sizes at each location are summarized in Tab. 2. Each value represents the average of multiple measurements obtained from an individual turbine wheel, and the average values of the three turbine wheels are also presented. Based on the average values of all turbine wheels, the grain size in the central region was approximately 4.3 mm, whereas that in the blade-tip region was approximately 0.5 mm, indicating a difference of nearly one order of magnitude.

These results demonstrate that a significant grain-size distribution exists within the turbine wheel. In the next section, the effects of grain size on the creep rate and creep rupture life are investigated.

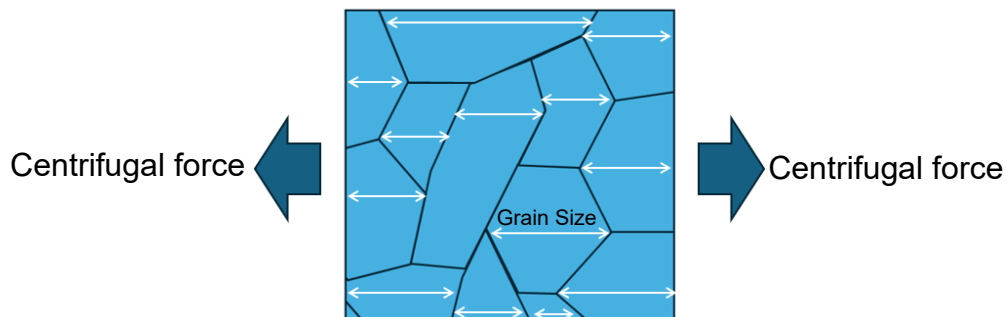


Fig. 13 Definition of grain size

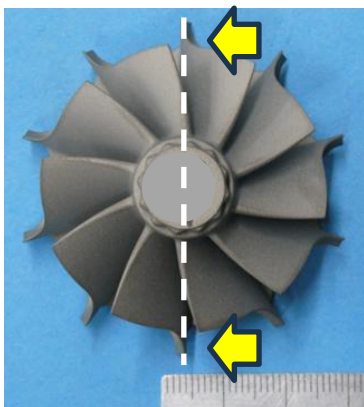


Fig. 14 Observation locations for grain size evaluation

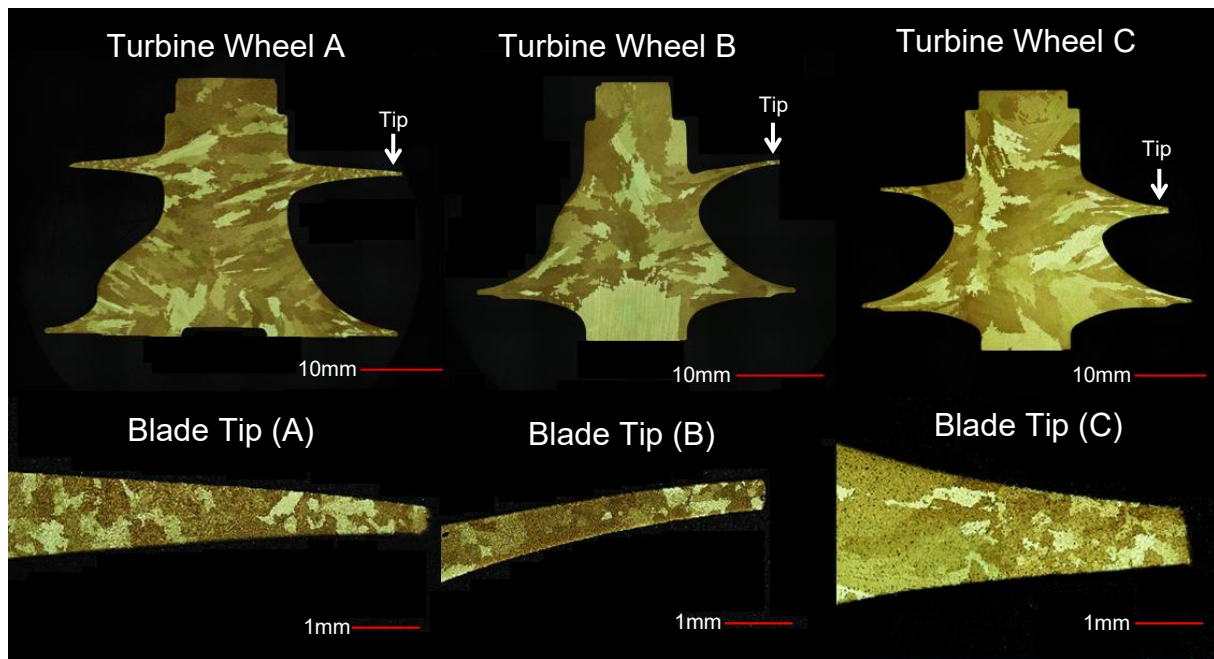


Fig. 15 Observation locations for grain size evaluation

Tab. 2 Mean grain size of each turbine wheel and overall mean [mm]

Location	Turbine Wheel A	Turbine Wheel B	Turbine Wheel C	Overall Mean
Center	4.26	3.99	4.56	4.27
Blade Tip	0.49	0.43	0.51	0.48

5.2 Effect of Grain Size on Creep Rate

In this study, creep test data for ALLOY 713C published in the NIMS Creep Data Sheet [13] were used to evaluate the effect of grain size on the minimum creep rate. Since the sole objective was to isolate the influence of grain size, specimens with high Si contents and specimens exhibiting coarse dendritic microstructures were excluded from the evaluation in order to minimize the effects of microstructural factors other than grain size. The grain size was determined from cross-sectional photographs of the specimens provided in the creep data sheet.

Although the minimum creep rate of each specimen is reported in the creep data sheet, direct comparison of the grain-size effect is difficult because the test temperatures and applied stress conditions are not identical. Therefore, to enable comparison of minimum creep rates obtained under different temperature and stress conditions, a regression equation, shown in Eq. 3, was developed using the minimum creep rate data contained in the creep data sheet.

$$\log \dot{\epsilon}_{min} = a_0 + \frac{a_1}{T} + a_2 \cdot \log \sigma + a_3 \cdot (\log \sigma)^2 + a_4 \cdot \frac{\log \sigma}{T} \quad \text{Eq. 3}$$

Here, $\dot{\epsilon}_{min}$ is the minimum creep rate, T is the absolute temperature, and σ is the applied stress. The coefficients a_0 , a_1 , a_2 , a_3 , and a_4 are regression coefficients determined for each specimen.

Furthermore, the minimum creep rate is generally known to be expressed by Eq. 4 [14].

$$\dot{\epsilon}_{min} = A \cdot d^m \cdot \sigma^n \cdot \exp\left(-\frac{Q_c}{R \cdot T}\right) \quad \text{Eq. 4}$$

Here, d is the grain size, m is the grain-size exponent, n is the stress exponent, Q_c is the apparent activation energy for creep, and R is the gas constant. It has been reported that the grain-size exponent is approximately $m = -1$ for many materials [14]. In this case, the minimum creep rate is inversely proportional to the grain size.

Furthermore, to compensate for temperature dependence and to systematically evaluate creep data obtained under different test conditions, the Zener-Hollomon parameter, Z , which is a high-temperature deformation parameter, was introduced as shown in Eq. 5 [15].

$$Z = \dot{\epsilon}_{min} \cdot \exp\left(\frac{Q_c}{R \cdot T}\right) \quad \text{Eq. 5}$$

By combining Eq. 4 and Eq. 5, Eq. 6 can be obtained.

$$Z = A \cdot d^m \sigma^n \quad \text{Eq. 6}$$

Furthermore, by rearranging the grain-size-dependent term and taking the natural logarithm of both sides, Eq. 7 can be obtained.

$$\ln(Z \cdot d^{-m}) = \ln A + n \cdot \ln \sigma \quad \text{Eq. 7}$$

In this study, the Zener-Hollomon parameter, Z , was calculated for each test condition using the grain-size exponent, m , and the apparent activation energy for creep, Q_c , determined later, and the relationship between Z and stress was analyzed.

The relationship between grain size and minimum creep rate normalized for temperature and stress using Eq. 3 is shown in Fig. 16. At temperatures of 875 °C and below, the minimum creep rate increased with decreasing grain size, which is consistent with the generally reported grain-size exponent of $m = -1$. In contrast, at temperatures of 925 °C and above, the influence of grain size became less significant, and nearly constant minimum creep rates were observed regardless of grain size. This suggests that the grain-size exponent approaches $m = 0$ at elevated temperatures. Around 900 °C, a value of $m = -0.5$ was applied to describe the transition between these two temperature regimes.

The grain-size exponent m , stress exponent n , and apparent activation energy for creep Q_c in Eq. 4 are summarized in Tab. 3. The stress exponent n ranged from 4.07 to 6.01, which is in good agreement with the values reported for power-law creep associated with dislocation motion ($n = 3 - 8$) [16]. The apparent activation energy for creep, Q_c , was 324 kJ/mol, which is close to the self-diffusion activation energy of Ni [4].

Furthermore, the creep data were reanalyzed using Eq. 5 - Eq. 7 to compensate for the effects of temperature and grain size. The resulting relationship is shown in Fig. 17. Data obtained under different temperature and grain-size conditions were successfully consolidated onto a single linear relationship, from which a stress exponent of $n=5.51$ was obtained. This value falls within the range of the stress exponents determined for each temperature condition and is consistent with the reported values for power-law creep controlled by dislocation motion [16]. In addition, the fact that the temperature- and grain-size-compensated data were represented by a single stress-dependent relationship confirms the validity of the grain-size exponent m and the apparent activation energy for creep Q_c determined in this study. These results also suggest that the dominant creep deformation mechanism does not significantly change within the investigated temperature range.

Based on these results, dislocation creep controlled by lattice diffusion associated with Ni self-diffusion is considered to be the dominant deformation mechanism within the temperature range investigated in this study. On the other hand, the difference in grain-size dependence is considered to result from changes in the contribution of grain-boundary sliding with temperature.

At temperatures of 875 °C and below, grain-boundary sliding contributes to deformation in addition to dislocation creep, resulting in higher minimum creep rates in fine-grained materials with larger grain-boundary areas [3]. Stress concentration at grain-boundary triple junctions caused by grain-boundary sliding promotes void formation, making creep deformation more likely to progress in fine-grained materials that are more strongly influenced by grain boundaries [17]. In contrast, at temperatures of 925 °C and above, power-law creep accompanied by dislocation climb and recovery becomes dominant, relaxing stress concentrations near grain boundaries. As a result, the contribution of grain-boundary sliding decreases, and the grain-size dependence of the minimum creep rate is considered to almost disappear.

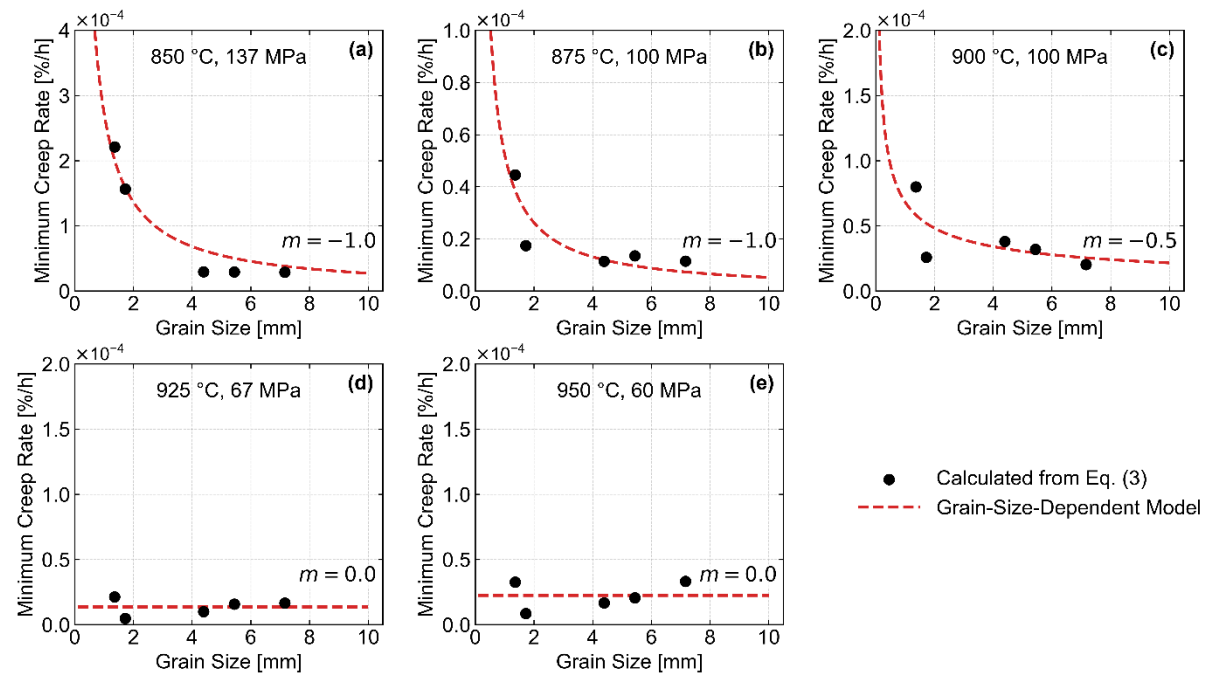
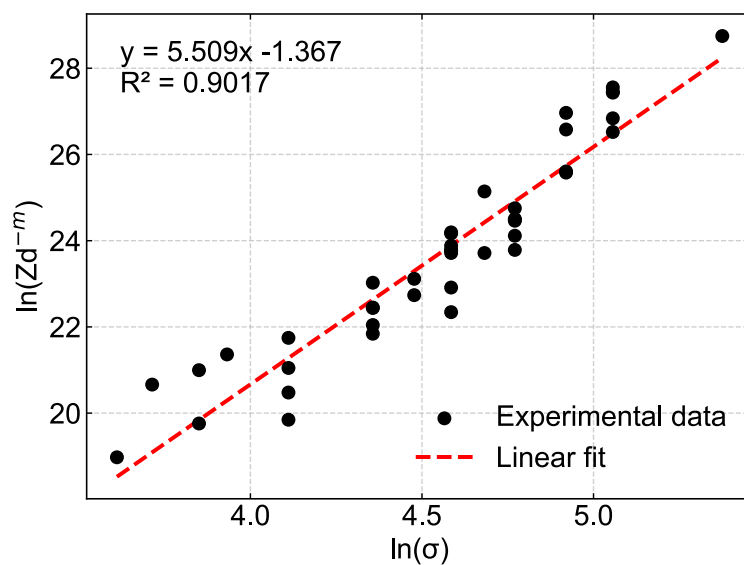


Fig. 16 Grain size dependence of minimum creep rate

Tab. 3 Grain size exponent, stress exponent, and activation energy used in Eq. 4

	850°C	875°C	900°C	925°C	950°C
m	-1	-1	-0.5	0	0
n	6.01	5.43	4.92	4.47	4.07
Q_c [MJ/kg]	11.19				
Q_c [kJ/mol]	324.17				

Fig. 17 Relationship between $\ln(\sigma)$ and $\ln(Zd^{-m})$.

5.3 Effect of Grain Size on Creep Life

In this study, the effect of grain size on creep rupture life was investigated using the approximation parameters of the Manson-Succop equation reported in the NIMS Creep Data Sheet for ALLOY 713C [13]. The Manson-Succop equation is expressed by Eq. 8.

$$\log t_r = b_0 + b_1 \cdot \log \sigma + b_2 \cdot (\log \sigma)^2 + B \cdot T \quad \text{Eq. 8}$$

Here, t_r is the creep rupture life, and b_0 , b_1 , b_2 , and B are regression coefficients determined for each specimen.

The relationship between grain size and creep rupture life calculated from the Manson-Succop equation at each temperature is shown in Fig. 18. At all temperatures, creep rupture life was found to increase with increasing grain size. In addition, the relationship between grain size and creep rupture life at each temperature could be approximated well by a linear equation. On the other hand, the slope of the regression line gradually decreased with increasing temperature. This result indicates that the influence of grain size on creep rupture life becomes less significant at elevated temperatures.

Next, using the linear approximations shown in Fig. 19, the creep rupture life ratio (CLR) between representative grain sizes of 4.3 mm and 0.5 mm was calculated, and its temperature dependence was approximated. Direct extrapolation of the regression equation beyond the evaluated temperature range may result in excessively low values at low temperatures and values exceeding unity at high temperatures. Therefore, the CLR was fixed at 0.40 below 850 °C and at 1.00 above the temperature at which the regression equation reached unity. The CLR is expressed by Eq. 9.

$$CLR = \begin{cases} 0.40, & T_c \leq 850 \\ C_1 \cdot T_c + C_2, & 850 < T_c < 1027 \\ 1.00, & T_c \geq 1027 \end{cases} \quad \text{Eq. 9}$$

Here, T_c is the temperature in degrees Celsius, and C_1 and C_2 are linear regression coefficients.

The relationship between CLR and temperature is shown in Fig. 19. It can be seen that the difference in creep rupture life caused by grain size decreases with increasing temperature. This trend is consistent with the reduction in the grain-size dependence of the minimum creep rate at elevated temperatures described in Section 5.2. In addition, it has been reported that changes in the γ' microstructure occur during prolonged exposure at high temperatures, and such microstructural evolution may also contribute to the reduction in grain-size dependence [18].

On the other hand, although the grain-size dependence of the minimum creep rate almost disappeared above 925 °C, as discussed in Section 5.2, the CLR remained approximately 0.74 even at 950 °C, indicating that the influence of grain size still remained. This may be because the minimum creep rate mainly reflects secondary

creep deformation, whereas creep rupture life also includes damage accumulation processes during tertiary creep, such as cavity formation and growth. Therefore, although the effect of grain size on the steady-state creep rate becomes less significant at elevated temperatures, its influence on the rupture process is considered to remain.

Based on the above results, a life correction factor was established to compensate for the effects of temperature and grain size on creep rupture life. By using this correction factor, creep life can be predicted while considering the local grain-size distribution within the turbine wheel.

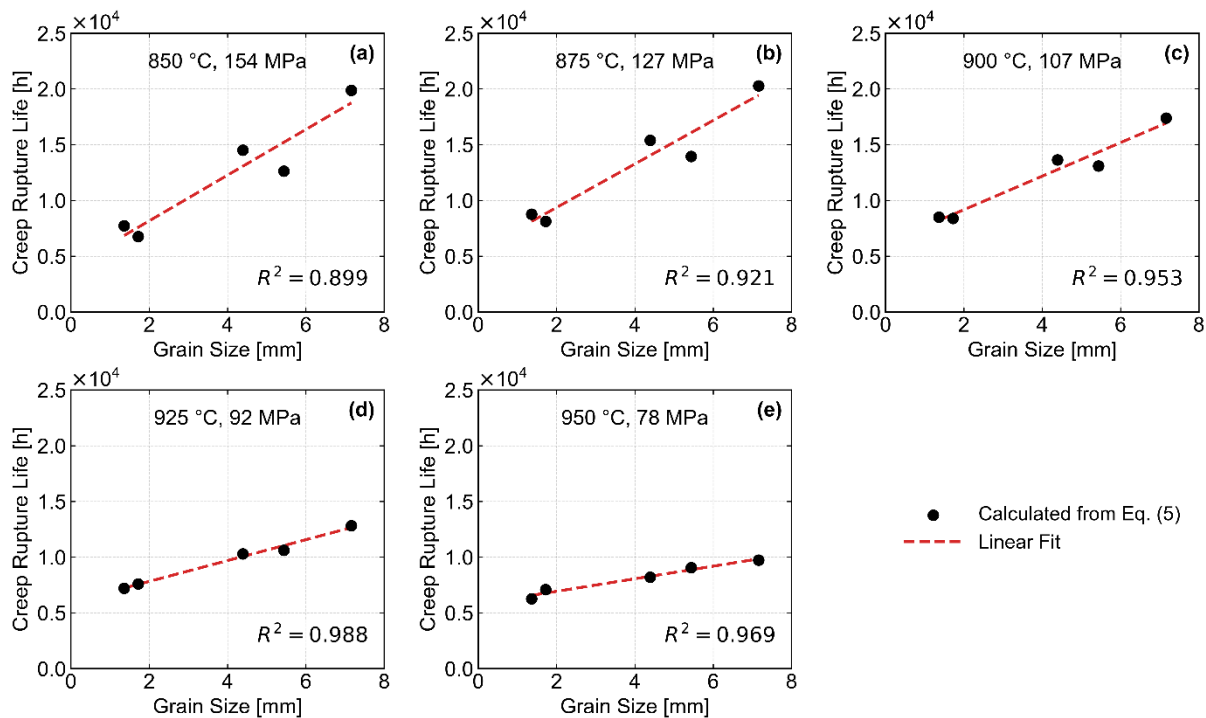


Fig. 18 Grain size dependence of creep rupture life

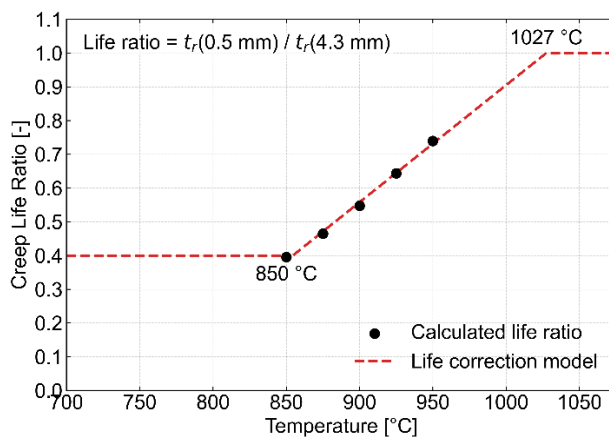


Fig. 19 Temperature dependence of creep life ratio

5.4 Development of Grain-Size-Dependent Creep Material Model

To incorporate the grain size dependence identified in Sections 5.2 and 5.3 into the CAE analysis, a grain-size-dependent creep material model was developed. In this study, the effect of grain size on the creep strain rate was incorporated into the creep constitutive equation, while its effect on creep rupture life was considered through a life correction factor. First, creep tests were conducted using ALLOY 713C, and the creep strain versus time relationships were obtained at 770°C, 870°C, and 970°C. The creep strain rate was described by Eq. 10, which represents both primary and secondary creep.

$$\dot{\varepsilon}_c = A_c \cdot \left(\frac{d}{d_{base}} \right)^m \sigma^n \cdot t^q \quad \text{Eq. 10}$$

Here, $\dot{\varepsilon}_c$ is the creep strain rate, A_c is a material constant, d is the grain size, d_{base} is the reference grain size, m is the grain-size exponent, σ is the applied stress, n is the stress exponent, t is time, and q is the time exponent. These material constants were determined independently at each temperature condition.

The material constants A_c , n , and q were determined by fitting Eq. 10 to the creep test results obtained at each temperature. The creep test results of specimens with a gauge diameter of 4 mm were used for parameter identification. Microstructural observations revealed that the gauge section was composed of an almost single grain. Therefore, the creep properties identified from these specimens were regarded as the reference properties corresponding to a grain size of 4.3 mm, which is the representative grain size at the center of the turbine wheel, and d_{base} was set to 4.3 mm.

The grain-size exponent m was determined based on the relationship between grain size and minimum creep rate obtained in Section 5.2. The grain-size exponent was set to $m = -1$ below 875°C and $m = 0$ above 925°C, with linear interpolation applied between these temperatures. Consequently, below 875°C, finer grains result in higher creep strain rates, whereas above 925°C, creep behavior becomes independent of grain size.

Fig. 20 compares the experimental creep curves and the model predictions at 770°C, 870°C, and 970°C. For the reference grain size of 4.3 mm, the model accurately reproduced the time-dependent creep strain at each temperature. In addition, accelerated creep deformation due to grain refinement was reproduced at 770°C and 870°C. At 970°C, because the grain-size exponent became $m = 0$, the creep curves for grain sizes of 0.5 mm and 4.3 mm coincided.

The developed grain-size-dependent creep material model was then implemented in the FEM analysis to calculate the creep strain distribution in each region of the turbine wheel. Furthermore, using the temperature and stress distributions obtained from the FEM analysis, creep rupture life was predicted based on the Larson-Miller Parameter (*LMP*) method. The *LMP* is expressed by Eq. 11.

$$LMP = T \cdot (C + \log t_r) \quad \text{Eq. 11}$$

Here, t_r denotes the creep rupture life, and C is the Larson-Miller constant. The relationship between the LMP and stress was established from the creep rupture data of ALLOY 713C and expressed by Eq. 12. Fig. 21 shows the resulting LMP -stress relationship using the 4.3 mm grain-size material as the reference condition.

$$LMP = \alpha \cdot \log \sigma + \beta \quad \text{Eq. 12}$$

Here, α and β are fitting coefficients describing the relationship between the LMP and stress. The LMP of each element was obtained by substituting the stress calculated from the FEM analysis into Eq. 12. Subsequently, the creep rupture life can be determined from Eq. 11 using the temperature obtained from the CFD analysis and the resulting LMP . For convenience, Eq. 11 and Eq. 12 were combined and rearranged to derive Eq. 13, which directly calculates the creep rupture life of the 4.3 mm grain-size material, $t_{r,4.3}$.

$$t_{r,4.3} = 10^{\left(\frac{\alpha \cdot \log \sigma + \beta}{T} - c\right)} \quad \text{Eq. 13}$$

Furthermore, the CLR between the 0.5 mm and 4.3 mm grain-size materials obtained from Eq. 9 in Section 5.3 was applied as a life correction factor. Using this correction factor, the creep rupture life of the 0.5 mm grain-size material, $t_{r,0.5}$, was calculated by Eq. 14.

$$t_{r,0.5} = CLR \cdot t_{r,4.3} \quad \text{Eq. 14}$$

As described above, the creep strain analysis incorporated the grain-size exponent m into the creep constitutive equation to represent the creep deformation rate as a function of temperature and grain size. In the creep rupture life analysis, the effect of grain size on creep rupture life was taken into account by applying the LMP relationship established from the estimated rupture life of the 4.3 mm grain-size material based on the NIMS creep rupture database, together with the temperature-dependent CLR .

Consequently, the proposed methodology enables turbine wheel life prediction while accounting for the effects of grain size on both the creep deformation rate and the creep rupture life.

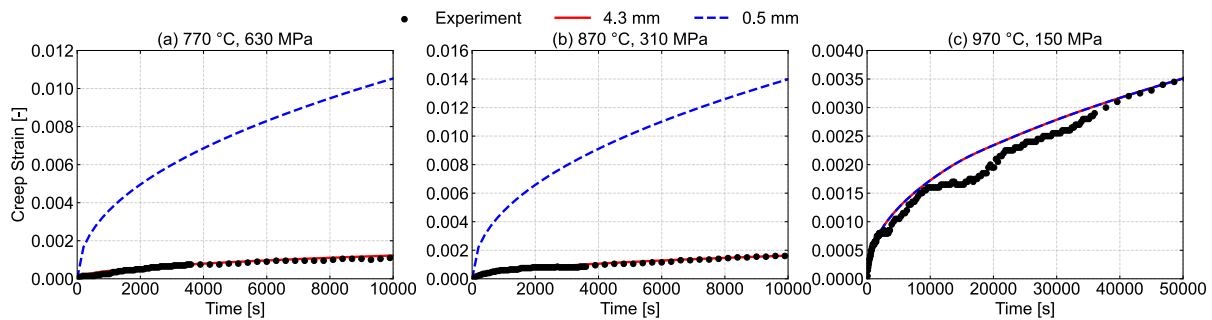


Fig. 20 Comparison of experimental creep curves and grain-size-dependent creep material model predictions: (a) 770 °C, 630 MPa, (b) 870 °C, 310 MPa, and (c) 970 °C, 150 MPa

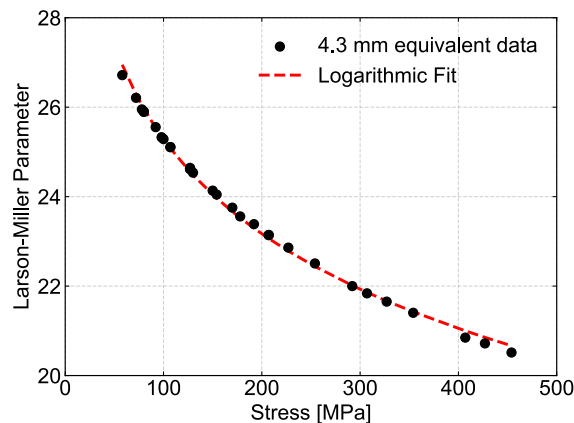


Fig. 21 Relationship between stress and Larson-Miller parameter for the reference grain size of 4.3 mm with logarithmic fit

6 Life Prediction, Failure Validation and Design Guideline Development

6.1 Analysis Model and Boundary Conditions

To validate the grain-size-dependent creep material model developed in Sections 4 and 5, a life prediction analysis was performed using a turbocharger that had experienced an actual creep failure. The housing design of this turbocharger differs from that of the turbocharger evaluated in Section 4. However, the geometry and material of the turbine wheel are identical.

Prior to the life prediction analysis, a CFD analysis was conducted using the results of a 1D analysis reproducing the actual operating conditions as boundary conditions, and the temperature distribution of the turbine wheel was calculated. Fig. 22 shows the CFD model and boundary conditions. The mass flow rate and temperature obtained from the 1D analysis were specified at the turbine inlet, while pressure and temperature boundary conditions were applied at the turbine outlet. Fig. 23 shows the circumferentially averaged temperature distribution obtained from the CFD analysis.

Next, the grain size distribution obtained in Section 5.1 was applied to the FEM model. Fig. 24(a) shows the grain size distribution assigned to the analysis model. To reproduce the grain size distribution observed in the actual turbine wheel, shown in Fig. 24(b), the representative 4.3 mm grain-size material was assigned to the central region, while the representative 0.5 mm grain-size material was assigned to the blade tip region. The grain-size-dependent creep material model and life correction factor developed in Section 5 were then applied.

Fig. 25 shows the boundary conditions used for the structural analysis. To prevent rigid-body motion, the bottom surface of the turbine wheel was constrained in the Z direction. In addition, the X- and Y-direction displacements of a single node located at the center of the turbine wheel hub were constrained. Furthermore, the centrifugal force calculated from the turbine wheel rotational speed was applied as a mechanical load, and the circumferentially averaged temperature distribution obtained from the CFD analysis was applied as the thermal boundary condition.

Based on these analysis conditions, the stress distribution and creep rupture life of the turbine wheel were calculated.

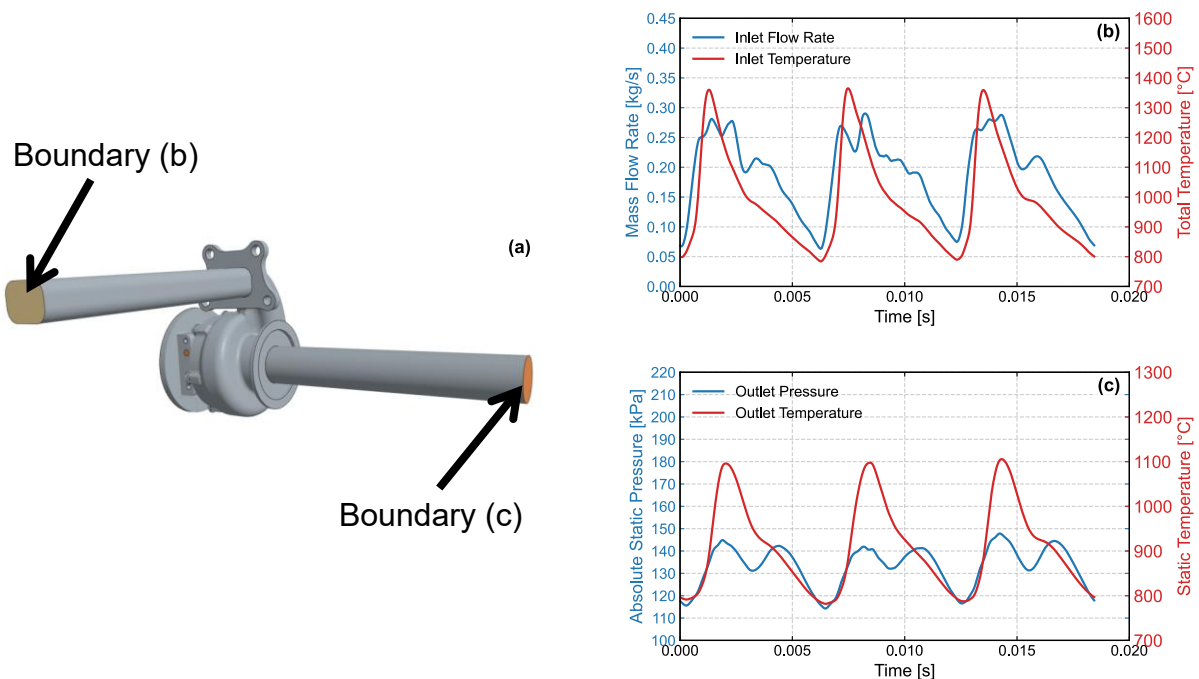


Fig. 22 CFD analysis model and transient boundary conditions: (a) CFD model, (b) turbine inlet boundary condition, and (c) turbine outlet boundary condition.

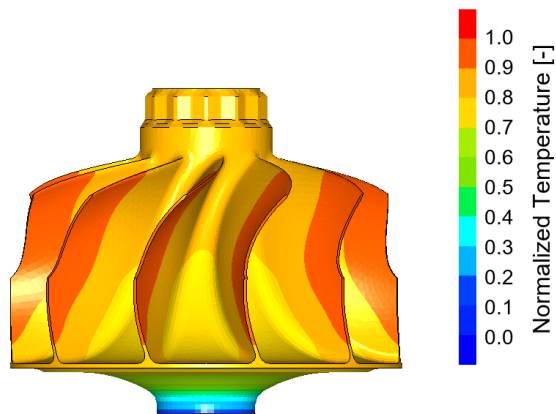


Fig. 23 Circumferentially averaged temperature distribution of the turbine wheel obtained by CFD analysis

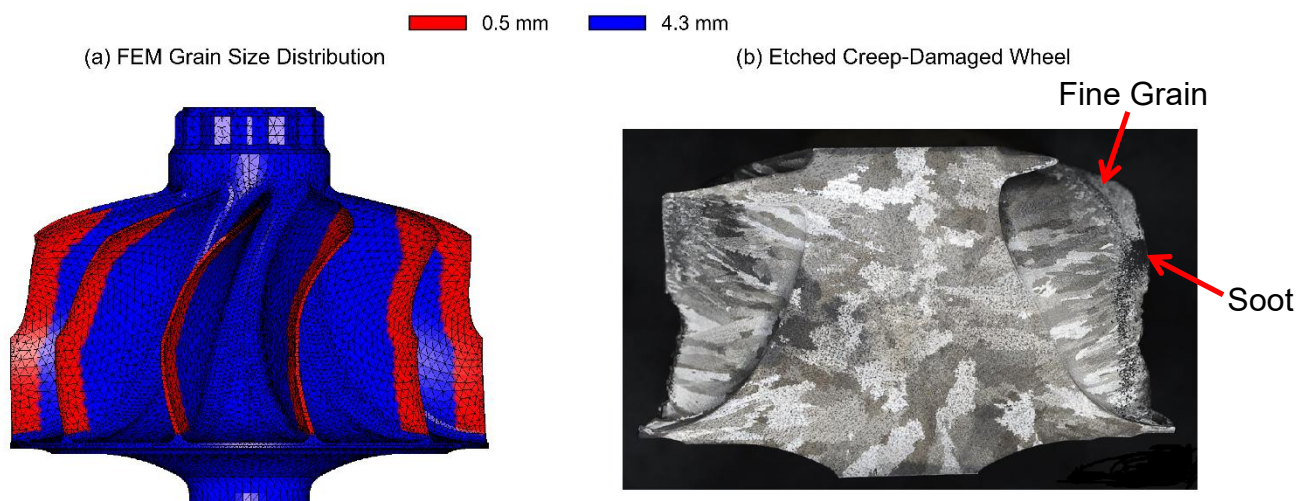


Fig. 24 Comparison between FEM grain size distribution and etched macrostructure of a creep-damaged turbine wheel: (a) FEM model and (b) etched turbine wheel.

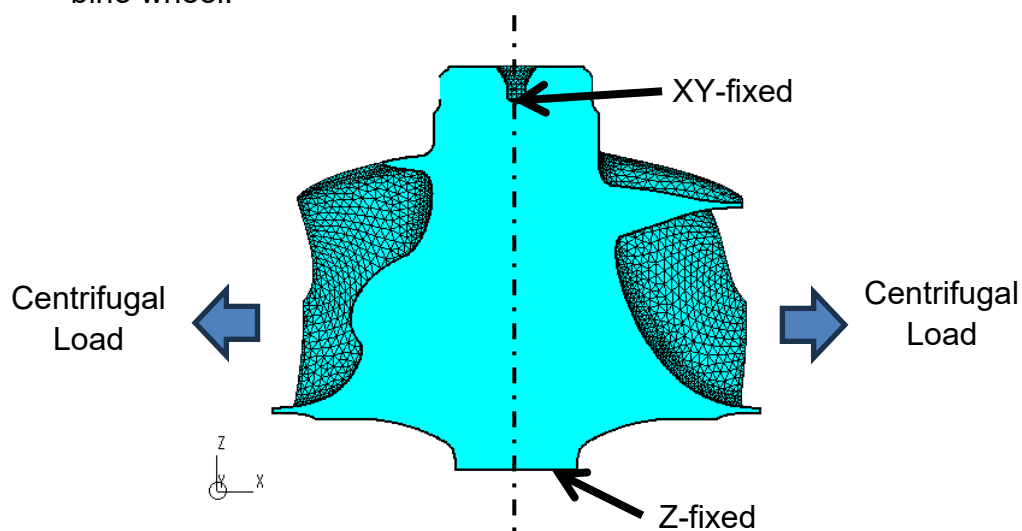


Fig. 25 Boundary conditions applied to the FEM model

6.2 Stress Redistribution and Residual Stress

Fig. 26(a) shows the stress distribution immediately after the start of the analysis. Relatively high stresses were observed around the region where the Z-direction displacement constraint was applied. However, these stresses are local stress concentrations caused by the boundary conditions. In addition, the temperature in this region is relatively low, and creep failure is not expected to occur. Therefore, these regions were excluded from the life assessment in this study.

Immediately after the application of the centrifugal load, high stresses were observed at the fillet region near the blade root of the turbine wheel. In contrast, the stress level at the blade tip was relatively low, and the high-stress region was primarily concentrated around the blade root.

Next, the time-dependent evolution of the stress distribution considering creep deformation was evaluated. Fig. 26(b) shows the stress distribution after the progression of creep deformation. As high-temperature operation continued, the high stress initially generated at the blade root fillet gradually relaxed. In contrast, the stress at the blade tip increased, resulting in a stress distribution different from that in the initial state. This behavior is considered to be caused by stress redistribution associated with the progression of creep deformation.

Furthermore, Fig. 27 shows the residual stress distribution after the transition from the high-temperature rotating condition to the room-temperature shutdown condition. Residual stresses were found to remain within the turbine wheel even after the centrifugal force and thermal load had been removed following shutdown. In particular, relatively large residual stresses were observed around the blade tip region, indicating that the effect of stress redistribution during high-temperature operation remained after shutdown.

Fig. 28 shows the stress histories at representative points located at the blade root fillet and the blade tip. In addition to the stress histories under the high-temperature rotating condition, the residual stresses after shutdown are also presented. At the blade root fillet, high stresses were generated during the early stage of operation; however, the stresses decreased rapidly as creep deformation progressed. In contrast, at the blade tip, the stress increased with time, and relatively large residual stresses were retained even after shutdown.

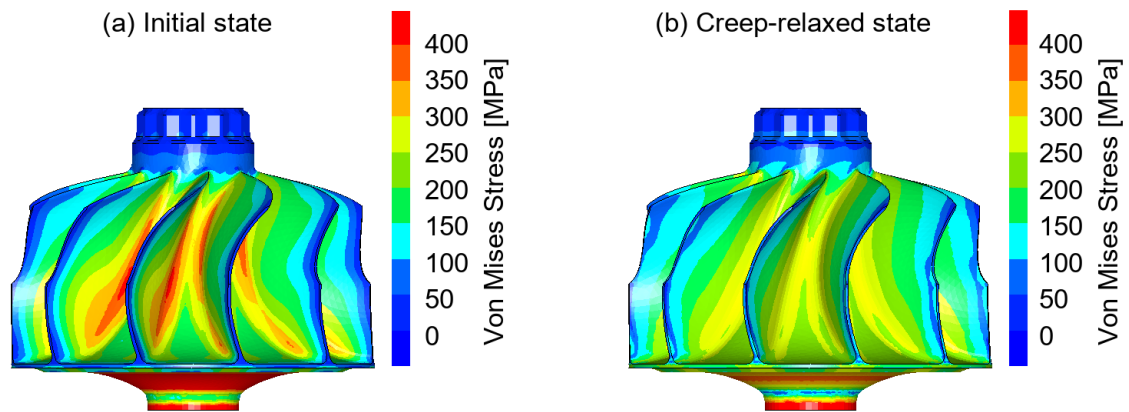


Fig. 26 Von Mises stress distributions at (a) the initial state and (b) the creep-relaxed state.

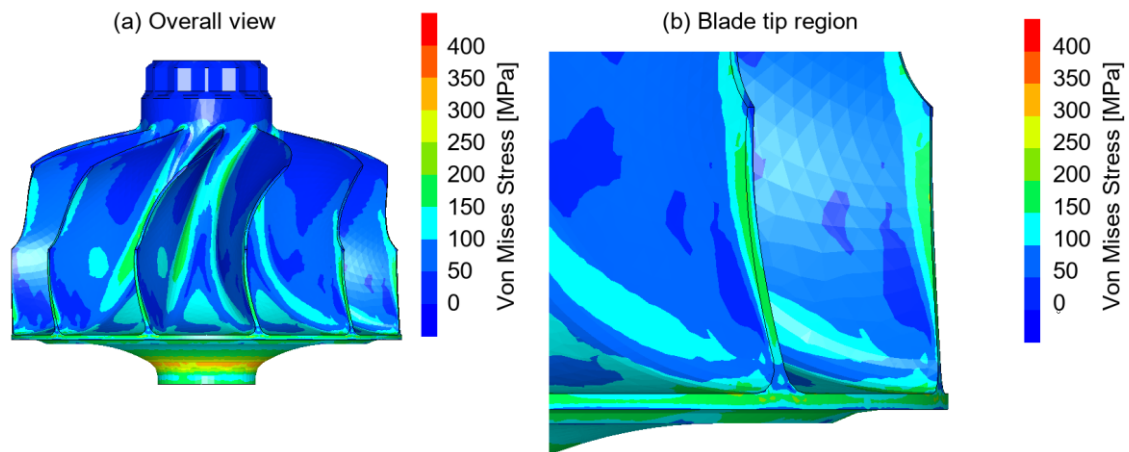


Fig. 27 Residual Von Mises stress distributions obtained after unloading thermal and centrifugal loads from the creep-relaxed state: (a) overall view and (b) blade tip region.

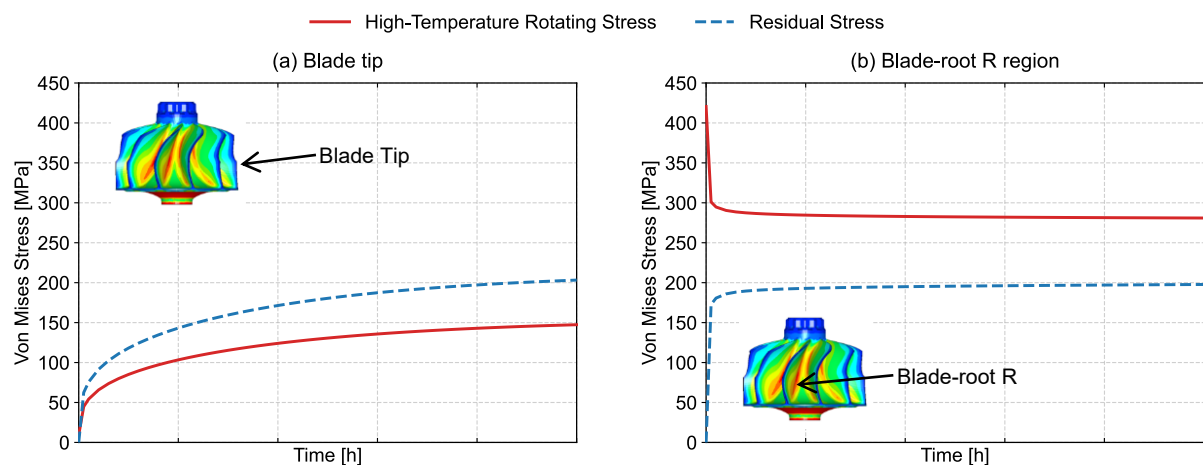


Fig. 28 Time histories of operational and residual Von Mises stresses at (a) the blade tip and (b) the blade-root R region.

6.3 Comparison between Predicted Creep Life and Actual Failure

In the conventional *LMP* method, creep rupture life is evaluated using the temperature and stress at the same instant. However, the objective of this study is not to calculate creep life based on rigorous creep theory, but rather to reproduce the failure location and rupture life observed in an actual turbine wheel. In an actual turbine wheel, stress redistribution occurs due to creep deformation during high-temperature operation, and residual stresses are subsequently generated by differential thermal contraction during the cooling process after shutdown. In this study, these residual stresses were assumed to play an important role in the formation of creep damage initiation sites. Therefore, life assessment was performed using the temperature during high-temperature operation in combination with the residual stress after shutdown.

Fig. 29(a) shows the actual fracture location, while Fig. 29(b) shows the predicted creep rupture life distribution. Because the difference between the short-life and long-life regions was large, the logarithm of the predicted life was used for visualization. As a result, short-life regions were identified at all blade tips. Enlarged views of the short-life region formed at the blade tip are shown in Fig. 29(c) and Fig. 29(d).

In the actual turbine wheel, fracture also initiated at the blade tip, and good agreement was obtained between the predicted and observed failure locations. Furthermore, the predicted rupture life was of the same order of magnitude as the creep rupture life of the actual turbine wheel, indicating that the proposed method can reasonably reproduce the creep damage behavior observed in service.

Based on these results, it was confirmed that the grain-size-dependent creep material model developed in Section 5, together with the life assessment methodology incorporating residual stress effects, can appropriately predict both the creep damage location and the creep rupture life of the turbine wheel.

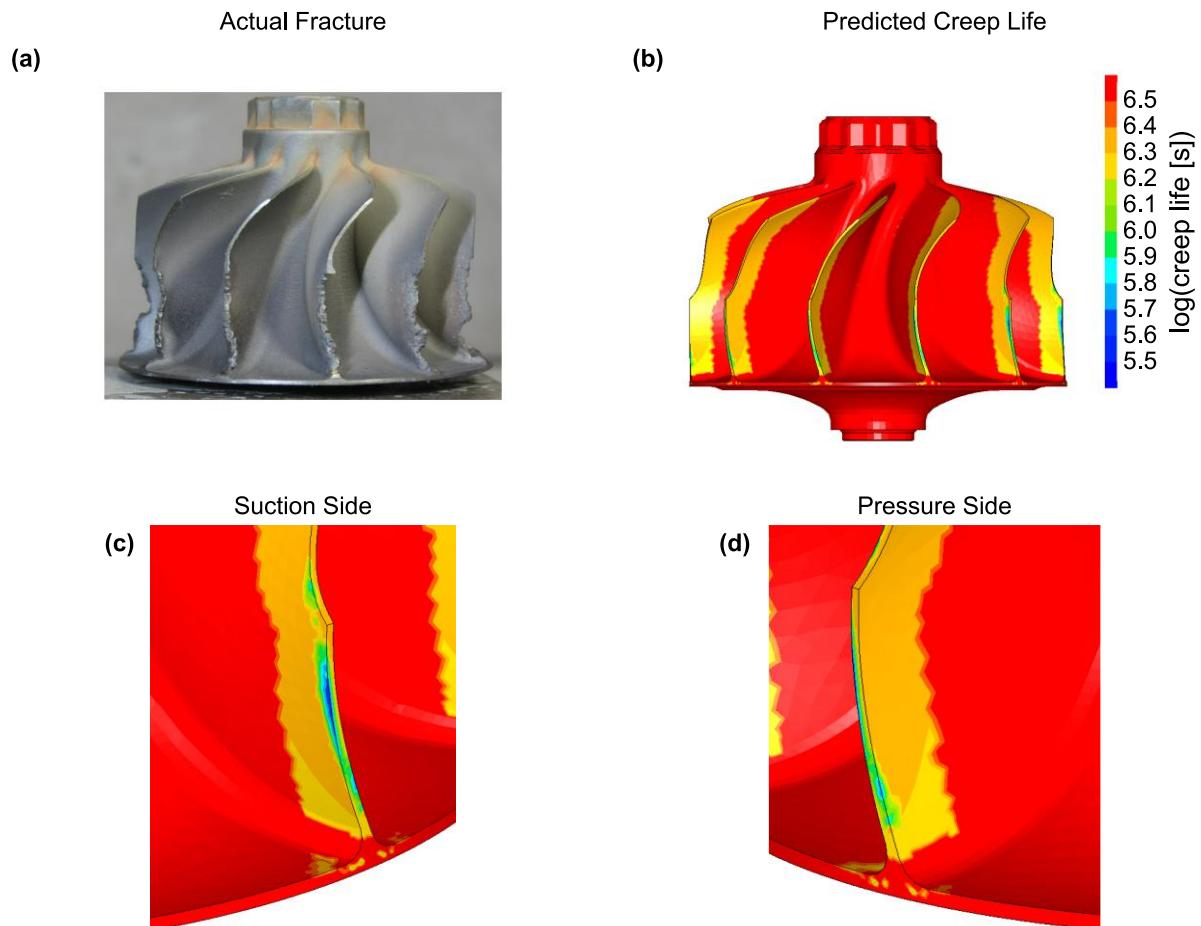


Fig. 29 Comparison between actual fracture location and predicted creep life distribution. (a) Actual fracture, (b) predicted creep life distribution, (c) detailed view of the suction side, and (d) detailed view of the pressure side.

6.4 Effect of Grain Size on Creep Life and Fatigue Strength

In Section 6, a creep life prediction methodology considering grain size was developed, and it was demonstrated that life prediction can be performed using CAE. On the other hand, for Ni-based superalloys, it is generally known that creep properties improve with grain coarsening, whereas fatigue strength improves with grain refinement. Therefore, both creep and fatigue properties must be considered simultaneously in order to determine the appropriate grain size for high-temperature turbine components.

For the evaluation of fatigue strength, fatigue strength data for materials with different grain sizes reported in Engineering Properties of ALLOY 713C published by the Nickel Institute were used [19]. The grain sizes investigated were Fine grain size (1/16 inch, 1.5875 mm) and Coarse grain size (3/16-1/4 inch, average 5.556 mm).

The relationship between fatigue strength and grain size was assumed to follow the Hall-Petch-type relationship and was expressed by Eq. 15 [20][21].

$$\sigma_f = \sigma_0 + kd^{-1/2} \quad \text{Eq. 15}$$

Here, σ_f is the fatigue strength at 10^8 cycles, σ_0 is the grain-size-independent component of fatigue strength, k is the Hall-Petch coefficient, and d is the grain size.

Furthermore, by rearranging Eq. 15, the fatigue strength for an arbitrary grain size can be expressed by Eq. 16.

$$\sigma_2 = \sigma_1 + k(d_2^{-1/2} - d_1^{-1/2}) \quad \text{Eq. 16}$$

Here, σ_1 and σ_2 are the fatigue strengths at 10^8 cycles for grain sizes d_1 and d_2 , respectively.

The Hall-Petch coefficient k was determined from the 10^8 -cycle fatigue strengths of the Fine grain and Coarse grain materials. Using the average value of k , the 10^8 -cycle fatigue strengths for the representative grain sizes of 0.5 mm and 4.3 mm, which were observed in the actual turbine wheel, were estimated using Eq. 16.

Fig. 30 shows the estimated 10^8 -cycle fatigue strength and the 100 h creep rupture stress calculated using the *CLR* developed in Section 5.3. These values were adopted as representative indicators for comparing the strength levels associated with fatigue and creep. At low temperatures, the 10^8 -cycle fatigue strength is lower than the 100 h creep rupture stress. However, as temperature increases, the 100 h creep rupture stress decreases rapidly and becomes lower than the fatigue strength at approximately 900°C. This result indicates that creep becomes the dominant failure factor in the high-temperature region.

In addition, when comparing the effect of grain size, the variation in 10^8 -cycle fatigue strength is relatively small, whereas the 100 h creep rupture stress changes significantly with grain size.

Fig. 31 shows examples of actual turbine wheels with different grain sizes. In Fig. 31(a), coarse columnar grains elongated in the centrifugal direction can be observed. In contrast, fine grains are observed in Fig. 31(b). These microstructures are considered to originate from differences in solidification conditions during casting. The coarse-grained structure shown in Fig. 31(a) is presumed to have formed under a relatively low solidification rate. For high-temperature applications, such a coarse-grained structure is considered advantageous from the viewpoint of creep life.

Based on the above results, it was clarified that the effect of grain size on fatigue strength is relatively small, whereas its effect on creep properties is significant. Therefore, for high-temperature applications of ALLOY 713C turbine wheels, grain size design focusing on creep performance rather than fatigue performance is considered important.

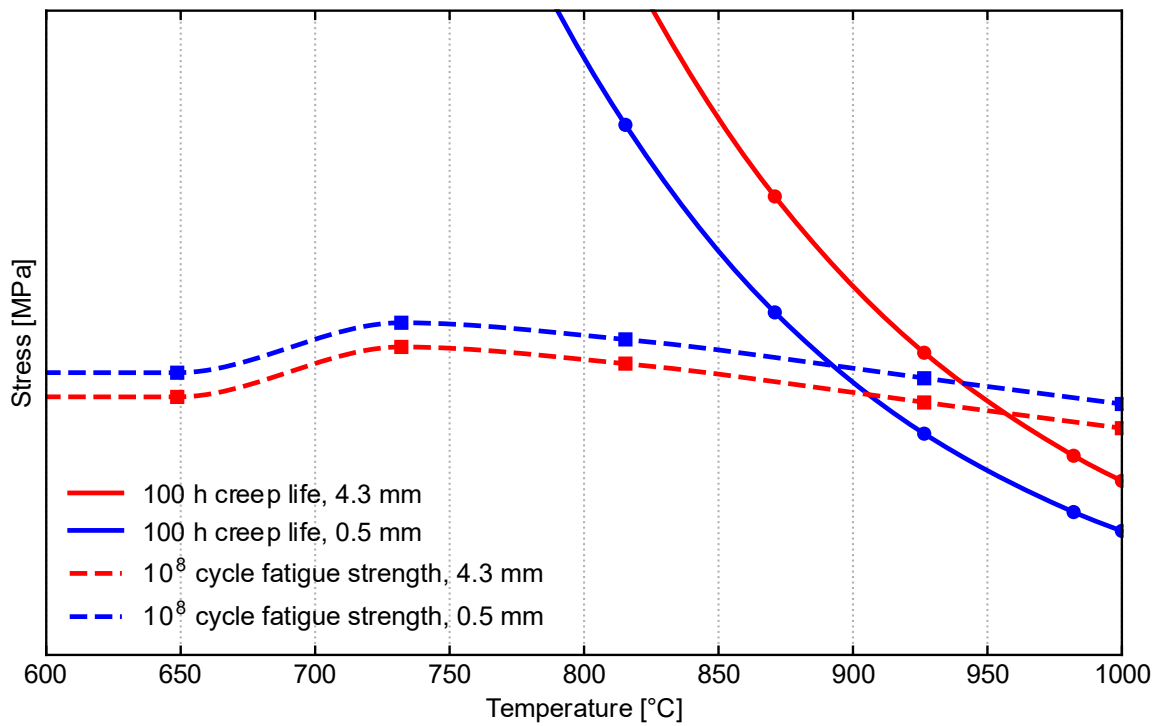


Fig. 30 Comparison of the stress corresponding to a 100 h creep life and the 10^8 -cycle fatigue strength for grain sizes of 0.5 and 4.3 mm.

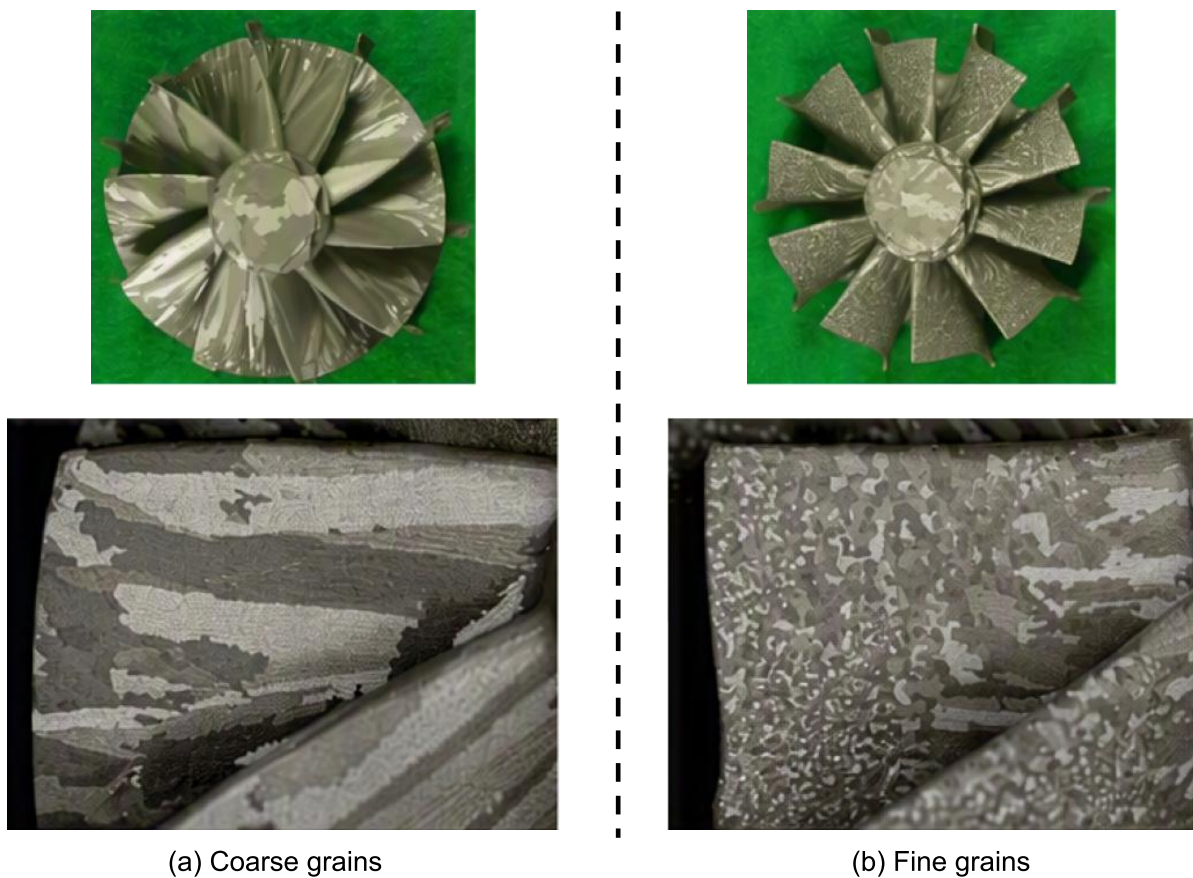


Fig. 31 Turbine wheels with different grain sizes. (a) Coarse grains and (b) fine grains.

7 Conclusions

In this study, a creep life prediction methodology considering the effects of measured temperature and grain size was developed for an ALLOY 713C turbocharger turbine wheel. The major findings are summarized as follows:

- A temperature prediction methodology for turbine wheels was established using CFD and heat transfer analyses calibrated with pulsating pressure and gas temperature measurements through a 1D analysis model. The predicted temperature distribution showed good agreement with the THC measurements, confirming that the temperature boundary conditions required for life prediction can be evaluated with sufficient accuracy.
- A significant grain size distribution was observed within the turbine wheel. The average grain size was approximately 4.3 mm in the center region and approximately 0.5 mm in the blade tip region. Furthermore, analysis of the NIMS creep database revealed that the creep strain rate increased and the creep rupture life decreased with decreasing grain size.
- A grain-size-dependent creep material model was developed to account for the effects of grain size on both the creep strain rate and creep rupture life. By applying this model, creep deformation analysis and life prediction considering the grain size distribution within the turbine wheel became possible.
- Life prediction analysis was conducted for a turbine wheel that experienced actual creep failure in service. The results predicted the formation of a short-life region at the blade tip. The predicted damage location showed good agreement with the actual fracture location, and the predicted rupture life was of the same order of magnitude as the actual rupture life.
- Comparison of the effects of grain size on fatigue strength and creep rupture life revealed that the grain-size dependence of fatigue strength was relatively small, whereas that of creep rupture life was significant. In addition, creep was found to be the dominant life-limiting factor at temperatures above approximately 900°C. Therefore, for high-temperature applications of ALLOY 713C turbine wheels, grain size design should prioritize creep performance rather than fatigue performance.

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