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An investigation of residual and thermomechanical stress and strain in the turbine housing of a turbocharger

Kathryn Elisabeth Gannon

A thesis submitted to the University of Huddersfield
in partial fulfilment of the requirements for
the degree of Doctor of Philosophy

The University of Huddersfield in collaboration with BorgWarner Turbo
systems
September 2019

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Abstract

This thesis contains the results of a novel combination of experimental measurements and simulation techniques to characterise the stress/strain behaviour throughout a typical lifecycle of a turbine housing. During operation the turbine housing is subjected to thermomechanical fatigue (TMF). The accurate validation of residual stress (both before and after operation) and stresses induced due to applied loading can be challenging and internal residual stresses in areas prone to fatigue crack initiation are difficult to measure accurately.

Experimental measurements of residual stress were obtained using two techniques. Neutron diffraction, a non-destructive technique based on the measurement of the shift in atomic lattice spacings induced by stress, was used to measure internally within the turbine housing both before and after the application of thermomechanical load cycles. The contour method, a destructive technique, based on the measurement of deformed surface contours after stress-relief induced by cutting was used to obtain measurements on a turbine housing before thermal cycling. High temperature strain gauges were used to measure strain due to applied thermomechanical load.

Simulations of the residual stresses present in the turbine housing at the end of the production process which included an annealing heat treatment showed very low $<\pm 10\text{MPa}$ levels of stress within the bulk of the turbine housing. Contour measurements showed a similar tendency toward low levels of residual stress. In contrast the neutron diffraction results revealed higher levels, tensile in nature and up to 150MPa in magnitude. Evidence of strain hardening was observed in the neutron diffraction measurement after thermal cycling with comparable behavior present in simulation models. The TMF simulation was validated against strain gauge measurements, observed trends were very similar. The magnitude of simulated and measured applied strain showed some variation and was within 10%-50%. However, the accuracy of the model was reasonable enough to predict the fatigue crack locations when assessed against a housing subjected to an extended durability test.

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Acknowledgements

I would like first and foremost to thank Professor Sue Kilcoyne and Dr Kiumars Shoghi (Q) for their invaluable guidance and encouragement over the course of this research, it has been very much appreciated. I would also like to thank Q for his other role as my manager at BorgWarner while I pursued this work, I have learnt so much from you, and I will be forever grateful.

This research would not have been possible without the sponsorship of BorgWarner Turbo Systems, I am very thankful to have had this opportunity.

The neutron diffraction measurements could not have been obtained without the valuable assistance of Thilo Pirling and the technicians at the Institut Laue-Langevin. Thank you!

A very special thank you to Russell Gray at Magmasoft for his unwavering support and guidance, it was great working with you.

I am also very grateful to the Stress Map facility especially Jefferson Oliveira for assistance with the contour measurements. The support and guidance were gratefully received.

I would also like to thank the test department staff at BorgWarner for their assistance with the gas stand testing.

And last, but by no means least thank you to my family and friends for providing much needed support and motivation along the way. To my late Grandad, thank you for being my greatest supporter and my biggest fan.

Publications

The following publications have arisen from my research detailed in this thesis:

Gannon, K., Shoghi, K., Kilcoyne, S. H., Pirling, T., & Zhang, S. Y. (2016). *An approach to turbine housing validation through the measurement of residual strains using neutron diffraction and operationally induced strains using high temperature strain gauges. 12th International Conference on Turbochargers and Turbocharging*. London: Institution of Mechanical Engineers.

Kilcoyne, S., Gannon, K., Kiumars, S., & Pirling, T. (2016). *A study of the effect of thermal cycling on residual stress in the turbine housing component of the turbocharger*. Institut Laue-Langevin (ILL). <https://doi.org/doi:10.5291/ILL-DATA.1-02-194>

Kilcoyne, S., Gannon, K., Shoghi, K., & Pirling, T. (2013). *Measurement of residual stress in the turbine housing of turbochargers. Institut Laue-Langevin (ILL)*. <https://doi.org/doi:10.5291/ILL-DATA.1-02-141>

I performed all the research with the support of T. Pirling the instrument scientist and my supervisor Professor S.H Kilcoyne in obtaining the neutron diffraction measurements and wrote the entire manuscripts with minor input and editorial changes from my co-authors. It is indicated where details, and results from these publications have been reproduced in this thesis.

Notation

A	= Area (m ²)
A_1	= Transformation temperature
BCC	= Body centred cubic
D	= Thermal diffusivity (m ² /s)
D_H	= Hydraulic diameter
E	= Elastic Modulus (GPa)
FCC	= Face centred cubic
FEA	= Finite Element Analysis
GF	= Gauge Factor
H	= Enthalpy (J/kg)
HBW	= Hardness Brinell
ILL	= Institut Laue-Langevin
L	= Latent heat of fusion (KJ/KG)
L_n	= Neutron path length (m)
LCF	= Low Cycle Fatigue
MBN	= Magnetic Barkhausen Noise
Nu	= Nusselt number
N	= Integer
P	= Perimeter (m)
PEEQ	= Equivalent plastic strain (MPa)
Pr	= Prandlt number
R	= Resistance (Ohms)
R_0	= Resistance of underformed gauge (Ohms)
Re	= Reynolds number
SG	= Strain gauge
T	= Temperature (°C)
T_a	= Annealing temperature (°C)
T_m	= Absolute melting temperature (°C)

TC	= Thermocouple
TMF	= Thermomechanical fatigue
TOF	= Time of Flight (s)
U	= Velocity m/s
V_{ex}	= Excitation voltage (V)
V_g	= Gauge volume
V_0^{II}	= Characteristic volume type II
V_0^{III}	= Characteristic volume type III
V_{out}	= Output voltage (V)
WEDM	= Wire Electrical Discharge Machining
Wt.%	= Weighted percent (%)
Q	= Scattering vector
a	= Lattice parameter (m)
α_{ij}	= Back stress (MPa)
c_p	= Specific Heat (J/kgK)
d	= Interplanar spacing (m)
d_0	= Strain free interplanar spacing
f_s	= Solid Fraction
$\hbar$	= Planck's Constant
hkl	= Miller indices
K	= Thermal conductivity
l	= length (m)
$\dot{m}$	= mass flow (kg/s)
m_n	= Neutron mass (kg)
t	= time (s)
S_{ij}	= deviatoric stress (MPa)
q	= Heat flux (w/m ²)
ε_x	= Direct strain in x direction
ε_y	= Direct strain in y direction
ε_z	= Direct strain in z direction

σ	= Stefan Boltzmann constant ($\text{W/m}^2\text{K}^4$)
σ_x	= Direct stress in x direction (MPa)
σ_y	= Direct stress in y direction (MPa)
σ_z	= Direct stress in z direction (MPa)
$\sigma_1, \sigma_2, \sigma_3$	= Principal stresses (MPa)
σ_a	= Axial stress (MPa)
σ_r	= Radial stress (MPa)
σ_h	= Hoop stress (MPa)
τ_{xy}	= Shear stress x-y plane (MPa)
τ_{xz}	= Shear stress x-z plane (MPa)
T_x	= Translation in X direction (mm)
T_y	= Translation in Y direction (mm)
T_z	= Translation in Z direction (mm)
ω	= Angular rotation ($^\circ$)
χ	= Angular tilt ($^\circ$)

1. Introduction

The turbine housing is one of the main components of a turbocharger. Turbochargers are commonly used alongside combustion engines to improve performance and fuel economy. To understand the purpose of a turbine housing, it is useful to first outline the basic principles of turbocharging. An example of a typical turbocharger assembly is shown in Figure 1.1.

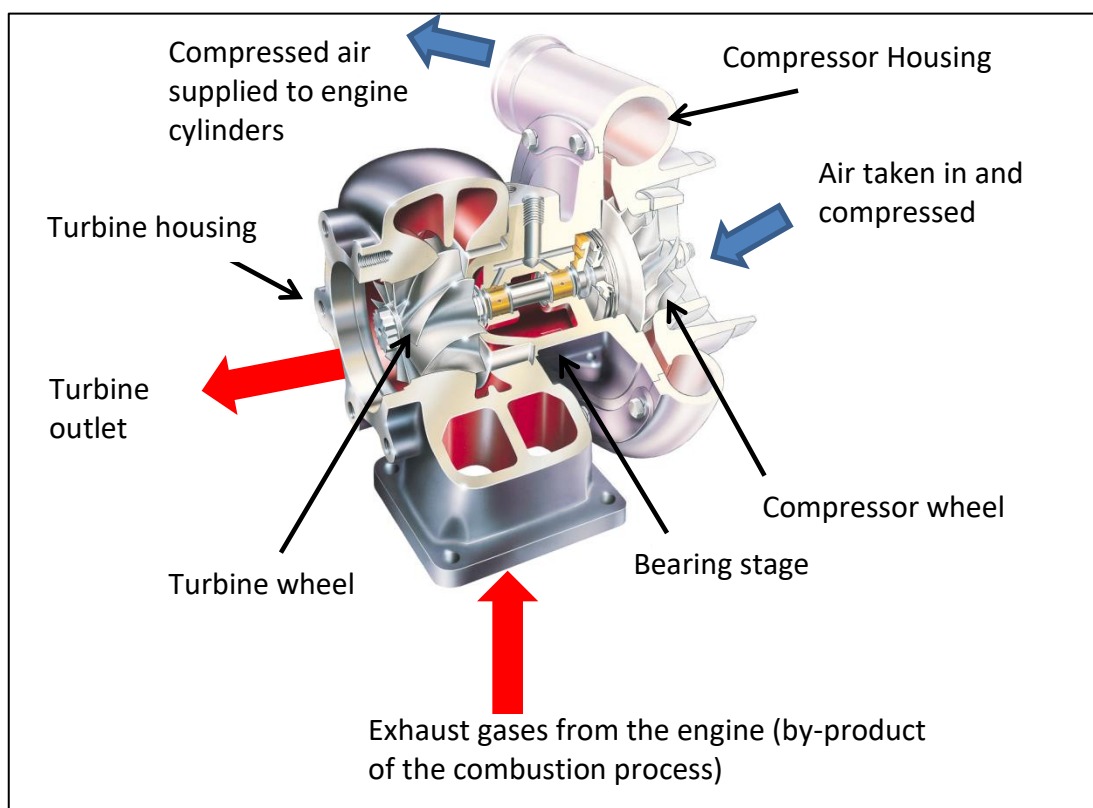


Figure 1.1 Basic turbocharger assembly adapted from (BorgWarner, 2016)

The function of the turbine housing is to direct what would otherwise be wasted hot exhaust gases coming from the engine cylinders, towards a turbine wheel causing it to rotate. This subsequently drives a compressor wheel which is connected to the turbine wheel via a shaft. As a result, the compressor wheel draws in and compresses air before supplying it to the engine cylinders. This additional supply of oxygen helps to burn fuel at a faster rate resulting in increased power output of the engine. Using turbochargers, 'engine downsizing' is possible i.e. smaller engines with turbochargers

can offer the power output of larger engines. This is advantageous as compared to a naturally aspirated engine of the same power rating, the fuel consumption of a turbocharged engine is lower, and an improvement in fuel economy and a reduction in harmful emissions such as CO₂ and NO_x is possible. According to a report by the European Commission (2017), heavy duty vehicles such as trucks and buses contribute as much as 25% of the total CO₂ emissions from road transport in the EU. In May 2018 the European Commission (2019) set out to do more to tackle this issue and outlined a proposal that by 2030, CO₂ levels should be reduced by 30% compared to 2019 levels. A review of the turbocharger market by Transparency Market Research (2017), summarises that the continued requirement for reduced emissions is driving the growth in the use of turbochargers, as well as the development of turbocharger technology. Some of the ways this is being tackled is by reducing weight in the turbine housing, such as through a reduction in wall thickness', or using integrated exhaust manifolds. In addition, engine exhaust temperatures are expected to rise, due to the need for increased combustion pressures and minimal heat loss when using engine after treatments systems to reduce NO_x. These developments may serve to improve performance and lower harmful emissions, but they will occur at the expense of the durability of the turbine housing, and as the environment in which it is expected to survive is already harsh, durability is a very important concern for turbocharger manufacturers.

Turbine housings are cast components and designed like a coiled shell, guiding the exhaust gases towards the turbine wheel. The thermal behaviour of the exhaust gas is transient and cycles between hot and cold conditions based on the mode of engine operation. This can lead to a differential in temperature forming. It is not difficult to imagine that during this process non- uniform sections, varying in both thickness, (due to design requirements) and microstructure/material properties, (due to differing rates of cooling during the manufacturing process) will expand and contract at dissimilar rates. Certain areas will be constrained from expanding while others will expand only to be prevented from contracting during the cool down period. This introduces high levels of strain which can be magnified over repeated load cycles and eventually lead

to fatigue crack initiation. These cracks could result in a loss of function of the turbine housing if they propagate through to the exterior surface and cause a release of exhaust gases.

In addition to the applied stress that the turbine housing is subjected to during operation, there is another type of stress that can be present in all cast components, known as residual stress. Residual stresses differ to those generated during service in that they can be introduced during the manufacturing process prior to any kind of service load. Withers (2007) explains that they can play a beneficial or a detrimental role in the failure of a component, as they can combine with or be deducted from those stresses applied during service. The tendency of residual stresses to behave in this manner is dependent on their magnitude, direction and stability, and this behaviour should be examined in relation to the turbine housing. To ensure high levels of reliability and durability of the turbine housing, the behaviour of the stresses and strains induced during service, and throughout the manufacturing process should be characterised and understood; which is the aim of this research. This will be achieved through a combination of both experimental test measurements and simulations to understand how stress is introduced and changed throughout a typical life cycle of the turbine housing.

Work to accurately validate residual stress and thermomechanical fatigue (TMF) simulation models can be challenging. Residual stresses internally in areas prone to fatigue crack initiation are difficult to measure accurately, therefore measurement techniques capable of achieving this will be explored. By gaining this knowledge, the prospect of using residual stresses to improve the fatigue life of the turbine housing could be assessed, as well as any potentially detrimental effects they may have on durability. To achieve this it is necessary to determine their magnitude and distribution. This research will also aim to explore the effect of the heat treatment on residual stress levels, as well as the influence of other manufacturing processes, to understand what role they play in the generation and re-distribution of stress within the turbine housing over its lifetime.

The objectives of this research can therefore be summarised as follows:

- To determine the appropriate measurement techniques to characterise the stress/strain throughout a turbine housing lifecycle.
- To obtain experimental measurements of applied strain during thermal cycling of the turbine housing.
- To obtain residual stress measurements both before (at the end of the manufacturing process) and after the application of thermomechanical loading.
- To simulate both residual and applied stress and validate the models against obtained measurements.

2. Background – The turbine housing

This chapter provides information relating to the turbine housing used in this research. It will outline the typical material requirements and detail the full life cycle of the turbine housing from manufacture into service, as well as providing details of key features of its design and potential modes of failure due to thermomechanical fatigue (TMF).

Turbocharger usage is widespread, especially within both on and off-highway industry segments, from light to heavy duty diesel applications, including commercial transportation and construction as well as gasoline engines in passenger cars. The main difference between gasoline and diesel turbocharger applications is the temperature of the exhaust gas. For diesel applications, exhaust gas temperatures can reach up to 760°C and ductile cast irons are the most common material choice for the turbine housing. In gasoline applications temperatures can reach up to 1050°C and stainless steels are required. The focus of this research is turbine housings used in diesel driven engines, typically those used in both on and off highway applications.

2.1. Ductile cast iron

The material of the turbine housings considered in this thesis, is a type of ductile cast iron referred to as SiMo 5.1 which, as the name suggests, is an alloy containing both silicon and molybdenum. Ductile cast iron is ideal when strength and ductility, as well as oxidation resistance at high temperatures is required at a much lower cost compared to cast stainless steels. In comparison to steels, ductile cast irons (as with all types of cast irons), contain a higher carbon content, typically 2-4 wt.%. They have a lower melting point, and a high fluidity, making them a good choice for the casting of complex shapes (Davis, 1996). Ductile cast irons are so called as they possess improved ductility compared to as-cast grey iron. This ductile behaviour is due to the presence of a carbon rich phase of graphite, which forms into a spheroidal or nodular shape during

solidification. Such a graphite morphology has been found to provide greater resistance against cracking and thermal fatigue (Tong et al., 2009) when compared to grey cast iron which consist of graphite in a flake or lamellar shape, and as a result is much more brittle in behaviour. An illustration of the different graphite morphologies is shown in Figure 2.1 and includes grey and ductile cast iron as well as compacted graphite type which has mechanical properties and a graphite morphology halfway between that of grey and ductile iron.

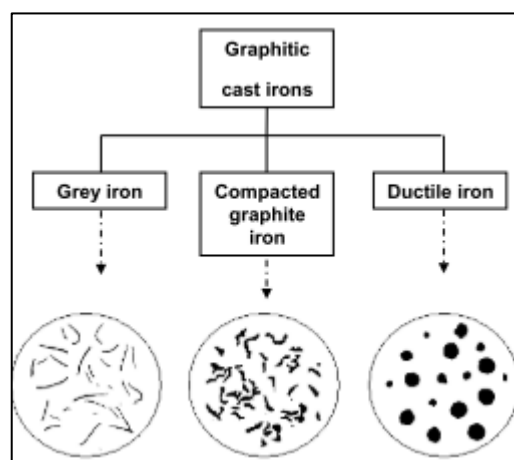


Figure 2.1 Cast iron graphite morphologies. Reproduced from (Ekstrom, 2013).

SiMo 5.1 is a ferritic cast iron, which means that in addition to the spheroidal graphite, the overall matrix microstructure should consist of a minimum of 90% ferrite (α -Fe). Compared to austenite (γ -Fe) ferrite has a higher thermal conductivity and a lower coefficient of thermal expansion. This is beneficial for high temperature fatigue strength as it reduces thermal gradients and lowers strain during thermal cycling.

The remainder of the matrix (<10%) is made up of carbides of pearlite, which is a mixture of ferrite and cementite (Fe_3C) as well as a molybdenum rich M_6C -type phase (Black, Burger, Logan, Perrin, & Gundlach, 2002). Cementite is a metastable phase and can continue to break down at elevated temperatures, resulting in a dimensional change, so its content is minimised. Whereas M_6C -type Carbides are dimensionally stable to temperatures above 925°C (Black et al., 2002). Carbides precipitate at the grain boundaries and can significantly impact ductility and machinability. To minimise

Fe_3C carbides, castings can be slowly cooled and allowed to self-anneal within the mould or subjected to a ferritising annealing heat treatment. Molybdenum-iron M_6C -type carbides are minimised by keeping the molybdenum content below 1% (Milot, 2012). The compositional requirements for SiMo 5.1 are shown below in Table 2.1 and Figure 2.2 shows a typical microstructure for this material.

Element	Fe	C	Si	Mo	P	Mg
Wt.%	Balance	2.8-3.6	4-5	0.8-1.2	Max 0.01	0.03 to 0.06

Table 2.1 Typical compositional requirements for SiMo5.1 ductile iron (BorgWarner, 2012).

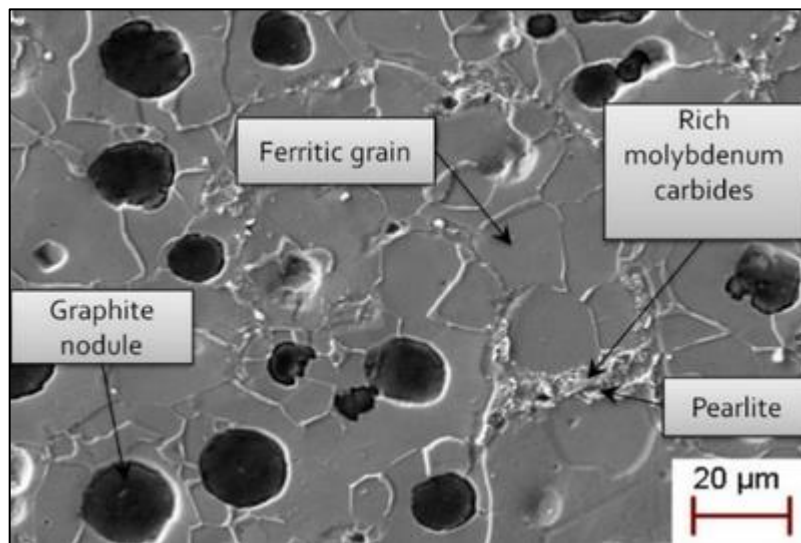


Figure 2.2 A typical microstructure of a ferritic ductile cast iron. Reproduced from (Herves, Thuault & Hug, 2015).

Silicon is used as a ferritising element and helps to stabilise the ferritic matrix of the microstructure. It also helps to increase the allotropic (A_1) transformation temperature. This is the temperature at which ferrite changes from its body-centred cubic structure (BCC) into austenite which has a face-centred cubic structure (FCC). When this occurs a change in volume takes places. Cycling through this temperature should be avoided during the operation of the turbine housing and the transformation

temperature is increased to ensure that it is well above the operating temperature that the turbine housing would be subjected to during service. The transformation temperature for a cast iron made of only iron-carbon will have an allotropic temperature of 723°C, below which austenite does not exist. The addition of each percent of silicon increases the transformation temperature by roughly 25 °C (Milot, 2012), therefore increasing the transformation temperature to approximately 850°C, allowing for a margin of safety above the maximum exhaust gas temperature of 760°C. Too much silicon (>5wt%) has been shown to reduce ductility and increase brittleness (Davis, 1996). The increase in the A_1 temperature is shown in Figure 2.3.

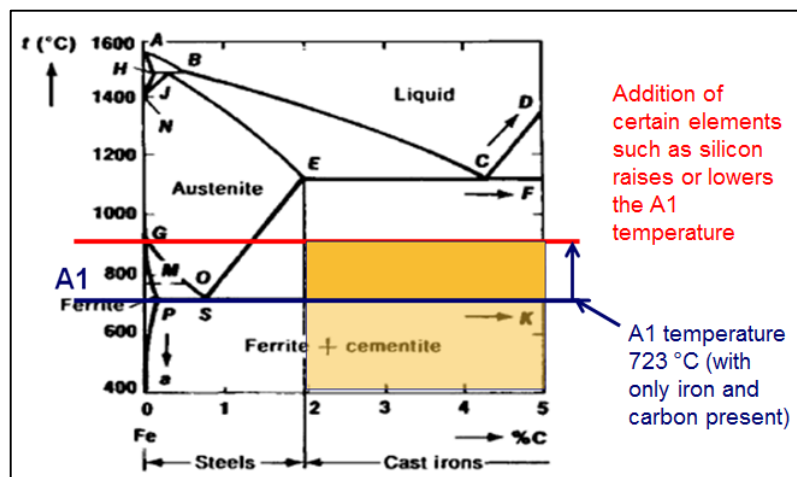


Figure 2.3 Iron-Carbon Phase diagram and the increase in the A_1 transformation temperature.

During heat treatment the A_1 temperature is purposefully exceeded. This occurs because the austenitic FCC structure can dissolve substantially more carbon than the ferritic BCC structure and is therefore an effective way to dissolve excess carbides. The heat treatment process to which the turbine housing is subjected to is defined in Section 2.2.

In addition to silicon, molybdenum is added to improve mechanical properties especially creep and elevated temperature strength. However, as already discussed, molybdenum should be carefully controlled as it results in stable carbides that cannot

be removed with heat treatment (Davis, 1996). The mechanical requirements of SiMo5.1 are given in Table 2.2.

Property	Tensile strength (MPa)	Yield strength 0.2% (MPa)	Elongation (%)	Hardness (HBW 10/3000)
At 22°C	Min. 550	Min. 480	Min 5	210 - 250

Table 2.2 Typical mechanical property requirements of a SiMo5.1 ductile cast iron (BorgWarner, 2012).

2.2. Turbine housing life cycle

The turbine housings are manufactured following a typical foundry production process for ductile cast iron. The main stages of the manufacturing process are outlined in sequence below:

- i. **Melting stage:** Raw pig iron and scrap ductile cast iron containing a composition as close to SiMo 5.1 chemistry as possible is used to produce a base of grey cast with some small additions as necessary to correct the final melt composition. The melting point is approximately 1400°C (Basha, 2015).
- ii. **Magnesium treatment and inoculation stage:** The molten grey cast iron is then treated with magnesium, converting the melt from grey iron into ductile iron by altering the growth directions of the flake graphite, causing spheroidal graphite to form. After the magnesium treatment the liquid is inoculated with a ferrosilicon compound. This assists with producing a uniform distribution of graphite nodules.
- iii. **Pouring stage:** The liquid cast iron must then be poured within a specific time frame, typically 7 minutes, into the mould (Basha, 2015). This is to prevent the

effects of the inoculation fading. The mould medium is referred to as green sand and inside the mould are sand cores which are used to create the internal geometry such as the volute.

- iv. Figure 2.4 shows an example of the molten cast iron being poured into green sand moulds while Figure 2.5 shows an illustration of a casting within a green sand mould and an example of the feeding and runner system that could be used to fill the mould with molten metal.

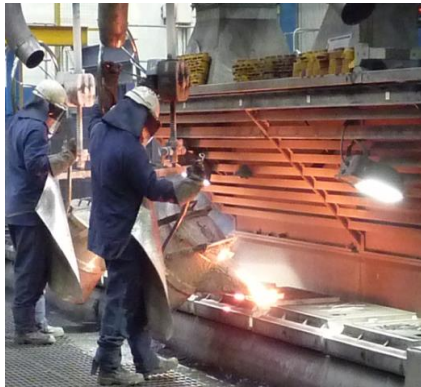


Figure 2.4 Molten cast iron being poured in sand moulds

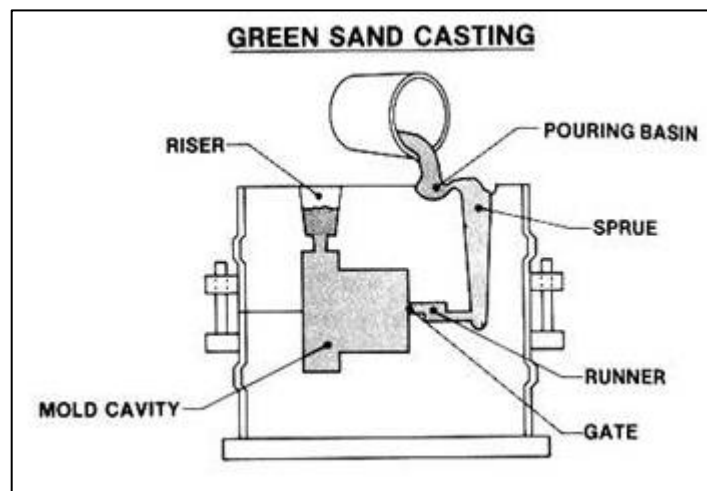


Figure 2.5 An illustration of a green sand casting within a mould. Reproduced from (The Steel Yard, 2010)

- v. **In-mould cooling and shakeout stage:** The liquid cast iron will then solidify and cool within the mould before being ejected, after which any feeding and gating systems which were used to assist in filling the mould are removed. The turbine housings are left to cool in the mould for ~ 75 minutes (Basha, 2015).

- vi. **Heat treatment:** The housing is then subjected to a ferritising annealing heat treatment to breakdown excess carbides, improving machinability and mechanical properties such as increasing ductility, which helps to improve the fatigue strength, which is the primary purpose of the heat treatment. However, literature such as Callister (2003) suggests such a heat treatment could also offer stress-relief to some degree. This is discussed in further detail in Section 3.1.1. Two different heat treatment processes are stipulated in the BorgWarner SiMo 5.1 specification (BorgWarner, 2012). These are outlined as follows:

Procedure A:

- a. Annealing to 900°C with a dwell time of 2 hours.
- b. Cooling down to 780°C with a dwell time of 0.5 hours.
- c. Annealing oven cools to 650°C.
- d. Cooling to room temperature, with no control requirement in still air.

Procedure B is then:

- a. Heating rate of 80°C per hour to 900°C.
- b. Annealing at 900°C with a dwell time of 2 hours.
- c. Annealing oven cools with a rate of 60°C per hour down to 500°C.
- d. Cooling to room temperature, with no control requirement in still air.

This turbine housings considered in this study have been subjected to Procedure A.

- vii. **Cleaning and machining:** Shot blasting is used to clean any residual sand from the castings, after which the castings are machined to their final design, this includes machining flange features and bolt holes.

After manufacture, a turbine housing will be built into a turbocharger assembly and then into an engine before entering operational usage. Over the course of its lifetime it is subjected to cyclic high temperatures and pressures due to the flow of engine exhaust gases, which enter and pass through it. A turbocharger used in commercial diesel applications is expected to last more than 10,000 hours for off-road vehicles or 1,000,000 km for on-road applications, over this time it will be subjected to significant stress. It must also be able to contain the turbine wheel if it were to shatter during operation. The thermal cycle that the turbine housing is subjected to during operation varies and is based on the specific application i.e. tractor, bus, excavator etc. An example of how the exhaust gas temperature changes over time for the sea level usage of a tractor is shown in Figure 2.6.

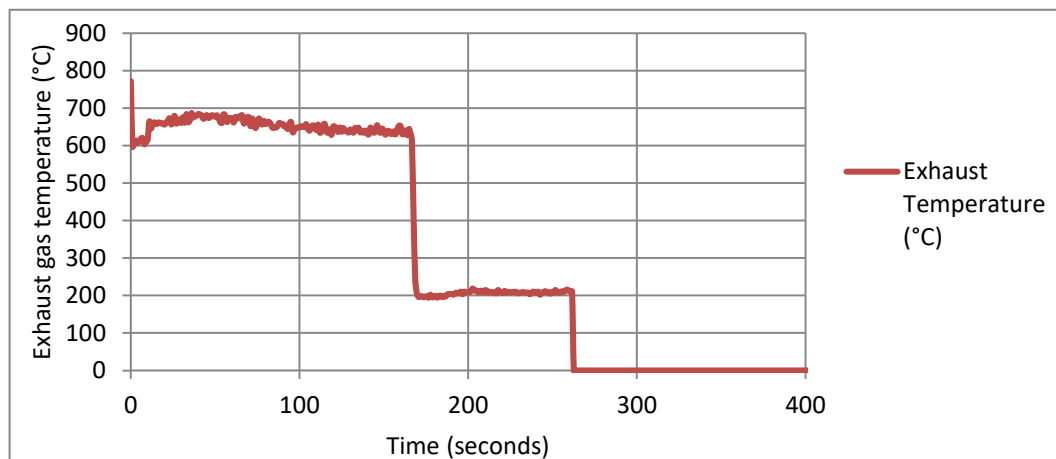


Figure 2.6 Exhaust gas temperature change over time from a tractor application recorded at sea level.

2.3. Turbine housing design and fatigue behavior

The design of a turbine housing varies, and is dictated by both performance and durability requirements, as well as physical space limitations of the engine. This

research focuses on a specific design referred to as a twin volute turbine housing, i.e. a housing with two passages through which exhaust gases pass, separated by a partition referred to as a divider wall. This specific design was chosen due to its high production volume and common usage in the turbochargers of heavy-duty diesel engines. A model of the selected housing is shown in Figure 2.7 and specific areas of the design are labelled.

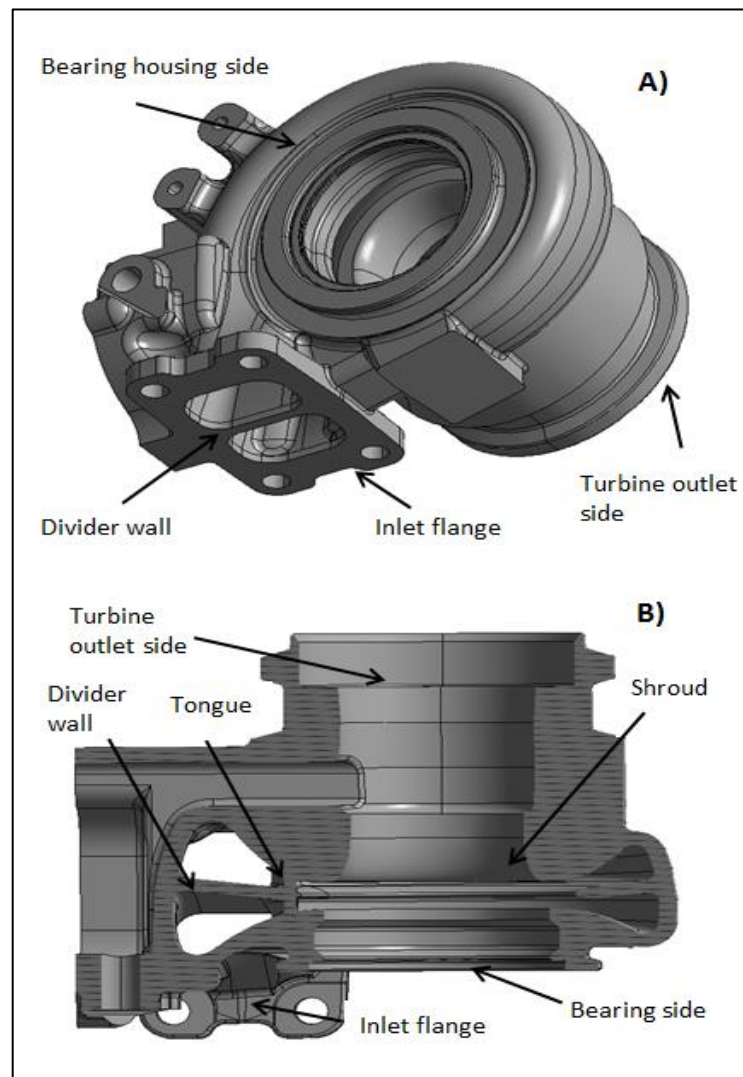


Figure 2.7 A) Twin volute turbine housing design exterior view and
B) A cross-section showing the interior, key design features are
labelled.

The interior design of the turbine housing is complex, with sections of varying thickness. The fluctuation of the exhaust gas temperature during operation results in a

temperature gradient across the housing which causes non-uniform expansion and contraction. At the same time, the turbine housing is constrained by the other parts of the turbocharger assembly, this results in expansion in areas within the housing subjected to high temperature which are then constrained by the surrounding cooler material, introducing mechanical strain. It is this alternating temperature, combined with the constraint of the turbocharger assembly that induces inelastic strain and stress within the turbine housing. The simultaneous behaviour of both thermal and mechanical loads varying over time is what defines thermomechanical fatigue (TMF) and can lead to low cycle fatigue (LCF). This can be very damaging to the turbine housing, resulting in significant plastic strain and eventually fatigue crack initiation. Areas that are very susceptible to fractures due to TMF are the divider wall and tongue regions of the turbine housing. The tongue is particularly susceptible to fatigue cracking as it is a thin section that is subjected to high temperature almost immediately during operation and as a result tries to expand but is prevented from doing so by the constraint of the surrounding thicker material. Cracking is generally always expected at the tongue location and is acceptable so long as fractures do not propagate to the outside surface of the turbine housing, an example of such a fracture is shown in Figure 2.8A. The divider wall is subjected to differential pressure due to the pulsation of the exhaust gases entering from the engine. It is also an area with changing section thickness which gives rise to differential temperatures and differing expansion behaviour compared to surrounding thicker sections. Divider wall cracks are also somewhat common but should be carefully monitored as they can potentially propagate to the exterior surface of the housing. An example of divider wall fractures is shown in Figure 2.8B. Fatigue cracking is not limited to the tongue and divider wall locations and can occur elsewhere, such as on the exterior surface of the housing close to bosses (see Figure 2.8C) and on the exterior of the volute (see Figure 2.8D). Ultimately, any location that is subjected to substantial strain is at risk of fatigue crack initiation. During operation the turbine housing is also subjected to an oxidising environment and harsh engine vibration, however this research will only consider the thermomechanical fatigue behaviour during operation.

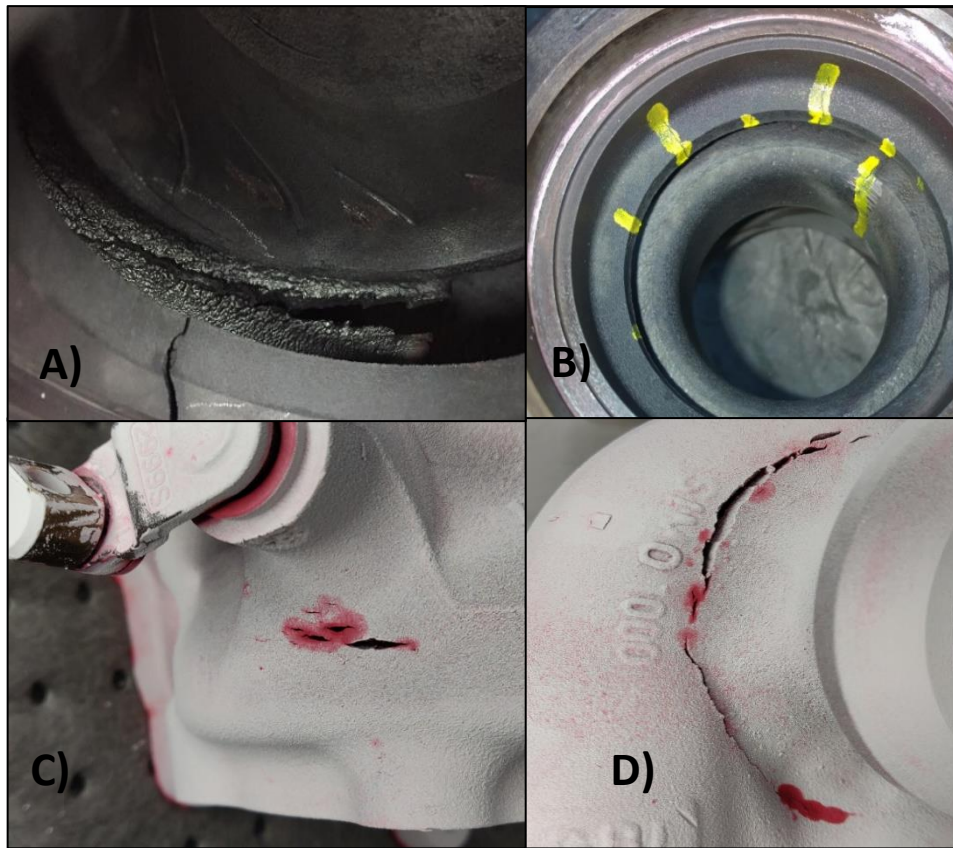


Figure 2.8 Examples of fractures occurring in various turbine housing designs due to thermomechanical fatigue in various locations. A) tongue, B) divider wall, C) the external surface next to a boss and D) the external surface of the volute.

3. A review of the behaviour and measurement of stress

This chapter will review existing literature covering two key areas.

3.1. Residual stress

3.2. Applied stress due to thermomechanical fatigue (TMF)

The aim of section (3.1) is to detail the origins and behaviour of residual stress and relate this to the life stages of the turbine housing. A review of measurement techniques is also given, so that suitable techniques can be identified. Section (3.2) provides an overview of the concept of thermomechanical fatigue and reviews the measurements of strain at high temperatures.

3.1. Residual stress

Residual stresses as summarised by Laszlis (2002), are stresses which remain within a component once all external constraints and loads have been removed and the body is at equilibrium with its surroundings. Withers & Bhadeshia (2001) explain that they are caused by mismatches between regions or phases within a material. Such stresses can be introduced in a variety of ways, for example due to non-uniform dimensional changes because of in-homogenous plastic deformations, or because of thermal gradients and variations in phase transformations.

There are three categories of residual stresses, (type I-III) based on the length scales over which the mismatches self-equilibrate (Macherauch & Kloss, 1986). Type I residual stresses are called macro stresses, and vary over large distances within the material, affecting many grains. Type II are intergranular micro stresses and vary over the grain scale, which contrasts with Type III micro stresses which vary over the atomic scale and relate to crystalline defects i.e. dislocations. Micro stresses are often present in materials of different phases, as a result of differing expansion coefficients (Lohe & Vohringer, 2002). The three categories of residual stress are shown in Figure 3.1. Type I stresses are the focus of this thesis, as they are introduced and altered by numerous

manufacturing processes (Lasmiss, 2002), for example welding (Braga et al., 2013), casting (E. M. Johnson, Watkins, Schmidlin, & Dutler, 2011), shot peening (Dalaei, Karlsson, & Svensson, 2011), and machining (Lohe & Vohringer, 2002).

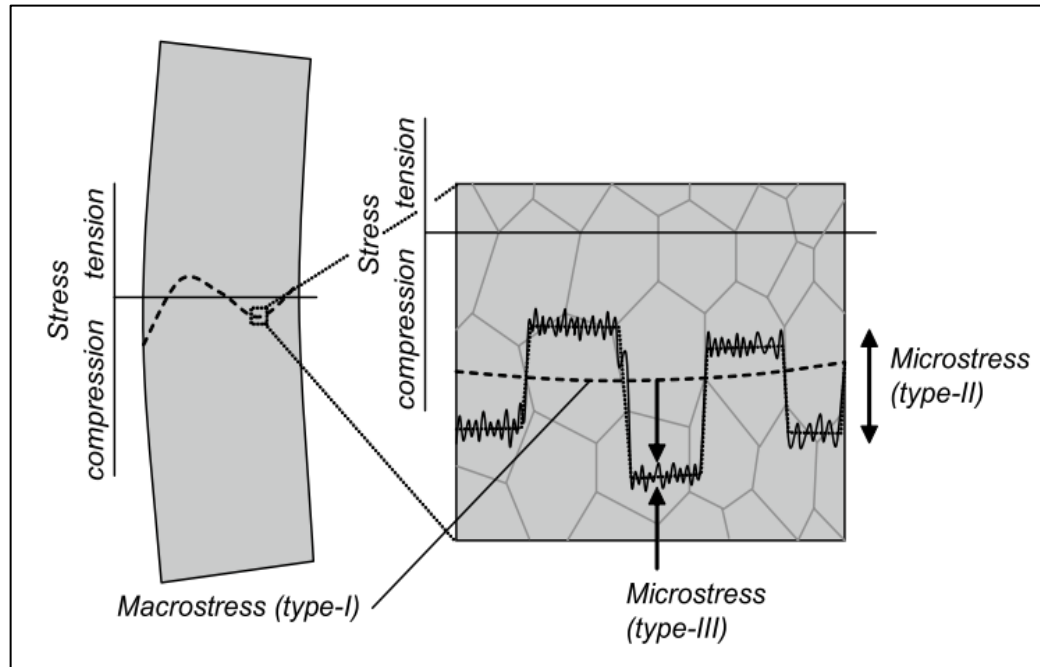


Figure 3.1 Type I, II and III residual stresses which balance over varying length scales.

Reproduced from (Johnson, 2008)

It is evident from literature sources such as the work of Lewis, Seetharamu, Zainal, and Hassan (2002) that residual stresses can be introduced during the casting process due to the non-uniform cooling of different sections within a casting. This is important, as the turbine housing is complex in design, with internal sections of varying thickness and as a result will be susceptible to temperature gradients during solidification and cooling and therefore the potential generation of residual stress. Lewis et al (2002) also identify another factor that should be considered as potentially influencing the magnitude of residual stress, that of the constraint of the mould wall and the change in volume that occurs during solidification. Previous studies have shown that the constraint of the mould can restrict the contraction of the casting as it cools, inducing residual stress (Baghani, Davami, Varahram, & Shabani, 2014).

A 'stress lattice' geometry consisting of different section thickness' can be used to help describe the behaviour of residual stresses during the casting process from solidification to cooling at ambient temperature. Such a geometry has been well studied and is often used as a simplified case to assess residual stress behaviour. Marberg & Ahlström (2017) considered such a geometry in their study of stresses in ductile iron castings and Figure 3.2 shows an example of the stress lattice geometry used in their work. A review of the sequence that takes place during the solidification and cooling stage is outlined by Johnson, Watkins, Schmidlin and Dutler (2011). To summarise, initially as the lattice begins to solidify the thin outer sections cool faster than the thicker central section, they try to contract but are prevented from reaching their desired length fully because of the constraint of the central thick section and are therefore subjected to tension, as a result the hotter thicker section is compressed slightly. The tensile stress in the thin sections will become large enough to cause yielding and plastic deformation this increases the length of the thin sections. Finally, as the lattice continues to cool to ambient temperature the thin sections are placed in compression due to the contraction of the thick section which then develops tensile stresses.

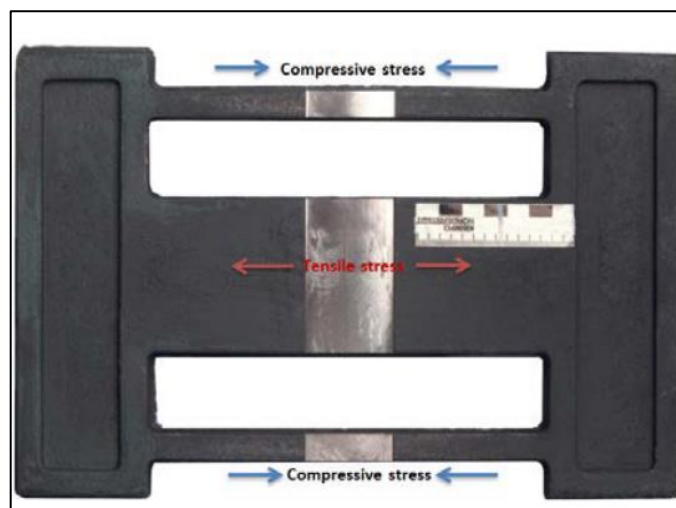


Figure 3.2 Stress lattice geometry. Reproduced from (Marberg & Ahlström, 2017).

3.1.1. Influence of residual stress on fatigue life

Residual stresses can be beneficial or detrimental to the durability of a component over its lifetime. This is dependent on the magnitude and direction of the residual stress i.e. compressive (negative) or tensile (positive), the type of loading the component is subjected to, as well as the stability of the residual stress (Lohe & Vohringer, 2002). The state in which the residual stress exists prior to service operation should be carefully considered, as it could potentially impact both the dimensional stability and durability of a component. When a tensile load is applied to a component fatigue cracks can initiate on the surface and propagate normal to the applied load. In order to improve the fatigue life, residual stresses that are in a direction such that they reduce the most significant applied stress will provide the biggest advantage, assisting with inhibiting crack growth and propagation (Withers & Bhadeshia, 2001). Residual stresses could hasten or delay the plastic deformation of the turbine housing if they combine with operationally induced stresses. However, Withers & Bhadeshia (2001) state in their paper that the addition (or subtraction) of residual stresses from applied stress only takes place up until the yield strength of the material is reached.

It is generally regarded that compressive residual stresses are most likely to have a beneficial effect on improving fatigue life. An example of this is shown by Ochi, Masaki, Matsumura, and Sekino (2001). Their work outlines a common method of introducing beneficial compressive residual stresses, using shot peening. This process causes plastic deformation of the surface layer resulting in a combination of surface work hardening and the introduction of compressive stresses in the shot peened surface layer. Their study found that by shot peening the surface of a ductile cast iron, the fatigue life was improved by 50% compared to as-cast material.

While compressive stresses may help to improve fatigue life times, tensile stresses can have the opposite effect, potentially accelerating fatigue. To assess the influence of residual stress on fatigue behaviour it's important to consider the stability and any relaxation effects which may occur.

It cannot be assumed that the initial level of residual stress, potentially introduced into a turbine housing during the casting stages will remain stable over subsequent processing and during operation. The magnitude and direction of a residual stress could be significantly altered, and any potentially beneficial residual stresses can only improve the fatigue life durability providing they remain stable. According to Lohe & Vohringer (2002) relaxation and redistribution of the residual stress can occur as a result of the movement of dislocations and changes in the elastic stress state and it is evident that residual stress relaxation is a complex mechanism and depends on many factors such as material composition, loading condition, part geometry and the environmental factors including elevated temperatures. There are numerous stages within a typical turbine housing life cycle that could cause residual stresses to relax after the initial casting process, such as during the annealing heat treatment due to thermal relaxation (Lohe & Vohringer, 2002), during machining due to mechanical relaxation (Li, Maijer, Lindley, & Lee, 2007) and finally during operation due to a combination of high temperatures and low cycle fatigue (Kim, Cheong, & Noguchi, 2013). Any relaxation of stresses can result in distortion of the component.

The heat treatment of the turbine housing, which is performed primarily to improve ductility and hence the fatigue strength of the material as detailed in Section 2.2 could also impact the residual stress magnitudes due to thermal softening. This is because of thermal stress relaxation, perhaps inadvertently removing beneficial compressive stresses. Complete relaxation of residual stresses can occur in a pure metal when it is annealed at a temperature, T_a of

$$T_a \approx 0.3T_m \quad (3.1)$$

Where T_m is the absolute melting temperature of the metal (Callister, 2003). This temperature corresponds to the point that recrystallisation takes place. During the recrystallisation process, new grains are formed which are approximately uniform in size and shape and strain-free. The recrystallisation temperature is defined as the temperature at which recrystallisation is complete within one hour. The onset of recrystallisation occurs more rapidly in pure metals, as the addition of alloying

elements increases the temperature at which the mechanism takes place and can be as high as $0.7T_m$ for some alloys (Callister, 2003). During the heat treatment process for the turbine housing as defined in Section 2.2 there is a period in which the temperature is held at 780°C. The aim of this is to homogenise the grain structure and in doing so, relieve stress.

A study by Spooner, Payzant, Edward, Vyletel, and Hubbard (1996) in which they measured the reduction in residual strain before and after heat treatment on grey iron brake discs, showed a reduction in strain of up to $400\mu\epsilon$ in a comparison between neutron diffraction strain measurement on grey iron break discs both before and after a stress relieving heat treatment.

Relaxation due to cyclic loading is mainly affected by the fatigue stress amplitude, mean stress ratio and the number of cycles. The initial magnitude and gradient of the residual stress should also be considered as well as the material behaviour, such as its stress/strain response and the degree to which cyclic work hardening and softening occurs (Zhuang & Halford, 2001). As previously mentioned, the turbine housing experiences LCF during operation, with stress amplitudes large enough to cause yielding, the fact that the stability of residual stresses may not be kept at higher stresses is obviously an issue for LCF. In ductile materials, which allow a degree of plastic deformation, residual stresses will only combine with applied stresses up until the yield strength of the material and as such could possibly hasten or delay the onset of plastic deformation. (Withers & Bhadeshia, 2001).

The relaxation of residual stress is exponential in rate and depends on the amplitude of the cycle and the degree of hardening or softening that takes places which each consecutive cycle. It is the first cycle in which most of the relaxation occurs with successive cycles resulting in further relaxation. Any relaxation occurring during the first load cycle is quasi static loading and depends on the yield strength of the material in tension and compression (Dalaei et al., 2011).

When a component is machined, any residual stresses close to the machined surface will be affected. If residual stresses are large, castings have been known to warp during

machining as residual stresses relax, resulting in a component that is permanently deformed.

3.1.2. The measurement of residual stress

To measure residual stress in the turbine housing the following is a list of requirements that the ideal technique would possess:

- i. It would be capable of non-destructive measurements so that residual stress can be measured on a turbine housing both before and after operation.
- ii. It would be able to measure a large area or deeply into polycrystalline (metallic) materials
- iii. Preferably it would be able to measure tri-axial stress (i.e. σ_{xx} , σ_{yy} , σ_{zz}) to allow full characterisation of the stress tensor.
- iv. It should be possible to apply to complex shapes
- v. It would be capable of measuring both surface (or near surface) and internal stresses.

Considering these requirements, several measurement techniques, commonly categorised based on whether they are non-destructive or destructive can now be assessed. Although the primary focus is the selection of a non-destructive measurement method, destructive techniques are still reviewed, as there is the potential for additional test specimens.

3.1.2.1. Non-destructive methods of measuring residual stress

Non-destructive methods rely on the measurement of some observable property influenced by strain (Schajer, 2013). For example this could be through measurements of magnetic Barkhausen noise (MBN) which is based on the principle first discovered by Barkhausen (1919). The technique is outlined in full in a best

practise guideline by Buttle, Moorthy, Shaw and Lord (2006). The MBN method relies upon the notion that materials with positive magnetic anisotropy (such as most steels and iron), will cause a variation in the intensity of the MBN noise depending on the direction of the residual stress (Rossini, Dassisti, Benyounic, & Olabi, 2012). Tensile stresses will increase the intensity of MBN noise while the opposite occurs for compressive stresses.

This technique is however sensitive to microstructure, composition and texture (Vashista & Moorthy, 2015), with previous studies showing that the signal intensity is influenced by both grain size (Anglada-Rivera, Padovese, & Capo-Sanchez, 2001) and carbon content (Vaidyanathan, Moorthy, Jayalumar, & Raj, 1998). This can cause problems with obtaining accurate stress measurements. The intensity of the MBN is also damped with increasing depth through the test piece and is therefore limited to surface or near surface measurements. An example of such measurements is shown in the study by Rosipal, Neslušan, Ochodek, and Šípek (2010) in which they used the MBN technique to evaluate surface stresses due to machining.

Another observable property influenced by strain is the velocity of ultrasonic waves, which when passing through a material are influenced by the magnitude and direction of any stresses present, due to the acoustic-elasticity effect (Rossini et al., 2012). This method is detailed in the guidelines by Noronha, Chapman and Wert (1973) and is focussed on recording the time-of-flight of the ultrasonic wave as it passes through different sections of a component, which will vary depending on the stress state. This technique can be used to measure both applied and residual stress. It is however sensitive to microstructure and obtained measurements are taken as an average over a reasonably large gauge volume. Therefore, in order to determine accurate stress values, a stress-free calibration sample should be used to correct for uncertainty due to texture induced anisotropy. The work of Bevan, Zhang and Kabra (2014) explored the use of the ultrasonic technique to measure residual stress in railway wheels. In their study measurements were averaged over the full width of the railway wheel rim at a depth of 20mm. These measurements were also undertaken alongside another technique called neutron diffraction which allowed for the use of a much smaller

gauge volume, in this case 4mm. Smaller gauge volumes are advantageous as they make it possible to evaluate any stress gradients that may exist.

Neutron diffraction is one of three non-destructive measurement techniques that rely on the use of the lattice spacing in crystalline structures as an atomic strain gauge, the others being x-ray diffraction and synchrotron diffraction. The techniques work on the principle that when a crystalline material is subjected to stress the interplanar spacing in the atomic structure will change in dimension. This can be recorded as a shift in the peak position of a diffraction pattern. If the peak position is then compared to that of a stress-free lattice structure, the strain can be determined, and stress calculated. X-ray diffraction is the most accessible of the three diffraction techniques, as it does not require particularly expensive or specialised equipment, with measurements possible in standard laboratory settings. However, laboratory produced x-rays are low in intensity and cannot penetrate more than a few microns into metallic alloys therefore their use is limited to surface or near surface measurements (Kim et al., 2013). It can also be challenging to obtain measurements on engineering components due to potential size and weight limitations.

Compared to x-ray diffraction, the synchrotron diffraction technique makes use of synchrotron sources enabling the use of very intense high energy x-rays, which are a lot more intense than lab produced x-rays. This allows for faster measurements and greater penetration depths of up to 20mm in steels (Veqter, 2018b). The main advantage of using neutrons rather than x-rays is due to their lack of charge which gives them the ability to penetrate much deeper into engineering materials, allowing neutrons to travel tens of mm through some metals. This unique penetrative ability is particularly beneficial for studying engineering components like the turbine housing, since it makes it possible to measure stress fields within the interior. This is advantageous as surface stress fields will often differ from those in the bulk of the material. In addition, neutron diffraction can be used to obtain a full stress tensor, as the measurement of 3D strain is possible with the use of the appropriate instrumentation.

Although neutrons can penetrate further than x-rays, data acquisition takes longer due to the lower flux of neutrons available which can be 1000-10,000 times less than those produced via synchrotron x-ray fluxes (Withers, 2004). This limits the use of neutrons to 2D line measurements rather than 2D/3D strain maps (Withers, 2007). The relatively low intensity of neutron flux means that the volume of the studied component from which diffraction data is collected is required to be larger ($> mm^3$), than that possible with synchrotron sources (typically $< 1mm^3$) this makes it possible to study a large number of grains within the material providing an average value of stress that is statistically representative i.e. macro stresses. Diffraction measurement techniques are however affected by texture (grain orientation) and microstructure variations and this can result in an uncertainty in the measurement. Additional sources of uncertainty can arise when the measurement gauge volume is not completely within the sample resulting in pseudo-strain (Holden, Traore, James, Kelleher, & Bouchard, 2015) which can be large ($1000\mu\epsilon$) compared to instrument resolutions $50-100\mu\epsilon$.

The Neutron and synchrotron x-ray sources require large scale facilities and are used for an array of experiments not just the measurement of stress. Access is generally granted in one of two ways. Either free of charge via the submission of a proposal which is then reviewed by a panel. This is generally the route academic research follows. The other way to access such facilities is via a commercial access route which can be very expensive, but access is much quicker.

An example of a neutron facility is the Institut Laue-Langevin (ILL) in France which is one of the leading centres in the for experimental studies using neutrons, the SALSA instrument at the ILL is used for residual stress experiments (Pirling, Bruno, & Withers, 2006). The Diamond Light Source in the UK is an example of a facility used for synchrotron x-ray experiments (Diamond, 2018).

3.1.2.2. Destructive methods of measuring residual stress

Destructive methods work by disturbing the state of equilibrium in the mechanical component, i.e. they rely on initiating the relaxation of residual stress, so that the

subsequent change in strain can be monitored and used to back calculate initial stress levels. The way in which the relaxation is initiated varies depending on the technique used and could be achieved in numerous ways, for example by the cutting of a slit in a surface, as with the crack compliance method, to measure surface strains (Veqter, 2018b), or as a result of the well-established technique of hole drilling (ASTM, 2013). This method involves drilling a hole through the centre of a rosette strain gauge consisting of three strain gauges positioned radially at a 45° spacing. When the hole is drilled, the local surface strains relax causing distortion which is detected as a change in strain by the strain gauges and used to determine bi-axial residual stresses.

Assessing the hole drilling technique as a method to use on the turbine housing, it is clear such a technique is better suited to less complex shapes due to the need for strain gauges. Positioning strain gauges internally within the turbine housing, in locations prone to fatigue crack initiation is not possible. The hole drilling method is also limited to measuring at a maximum depth of 2mm for standard strain gauges (Veqter, 2018b), therefore ignoring the bulk of the material which limits the amount of comparisons that can be made against simulations.

Another destructive method, the contour method is appealing, since it enables a 2D cross-sectional map of residual stress to be produced. The contour method of measuring residual stress was first developed (Prime, 2001) and involves the cutting of the component under investigation in two along a flat plane, resulting in the relaxation of residual stress. If the contours of the deformed surfaces of each of the opposing cut faces are then measured, any deviation of the surfaces from planarity is assumed to be due to elastic relaxation of residual stresses and can be used to determine the original residual stress distribution present in the component. Other assumptions that are made using the contour method, are that the original cut plane was flat and that the cutting method itself does not introduce stress. Despite its destructive nature, the contour method is attractive as it allows large stress maps of the cut surfaces to be obtained and, when compared to neutron diffraction, is not limited by microstructure or section thickness. It can also be applied in a laboratory setting with the necessary

cutting and measurement equipment. The method does not require a stress-free sample for calibration which is also advantageous (Veqter, 2018b).

3.1.2.3. Summary and selection of methods

To summarise the many residual stress measurement techniques available and compare some of their capabilities, they are presented in Table 3.1.

Method	Penetration	Spatial resolution	Accuracy	Comments
Hole Drilling (deformation due to stress relaxation)	~1.2 x hole diameter	50µm depth	± 50MPa	Measures in plane type I stresses; semi-destructive
Contour method (deformation due to stress relaxation)	Specimen cross-section (section thickness from 2 to 600mm)	1mm (based on FEA mesh size)	5-10%	Produces a 2D map Requires very accurate cutting; Destructive Not sensitive to microstructure
Synchrotron x-ray diffraction (atomic strain gauge)	>5mm	20 µm lateral to incident beam; 1mm parallel to beam	~± 50MPa	Requires synchrotron access
Lab produced x-rays (atomic strain gauge)	<50µm (Al); <5µ (Ti)	1mm laterally; 20µm depth	± 20MPa	Limited to near surface measurements, sensitive to microstructure
Neutron diffraction (atomic strain gauge)	~200mm Al, ~60mm Fe	500µm	~± 10-50MPa	Measurement of 3D strain tensor, non-destructive. Limited facilities. Sensitive to microstructure; measures types I, II and III
Magnetic methods (variations in magnetic domains with stress)	10mm	1mm	10%	Sensitive to microstructure; magnetic materials only; types I, II, III
Ultrasonic methods (changes in elastic wave velocity due to stress)	>10cm	5mm	10%	Sensitive to microstructure; types I, II, III
Thermal methods	Varies		Qualitative	Results are not quantitative

Table 3.1 Comparison of various residual stress measurement techniques information

adapted from Withers & Bhadeshia (2001) and Prime (2001).

Of all the methods available neutron diffraction, which uses the lattice spacing in crystalline structures as an atomic strain gauge, is the most appealing for use on the turbine housing. This is because:

- Neutrons can penetrate deeply into iron alloys potentially up to 60mm enabling internal measurements to be obtained.
- It is a non-destructive technique, allowing for measurement both before and after service operation of the housing.
- Tri-axial stresses can be measured.
- Macroscopic stress can be measured.
- Facilities, for example at the ILL in France, have instruments large enough to enable measurements to be taken on the turbine housing.

A technique based on mechanical relaxation would be a good pairing to neutron diffraction. The contour method is a good candidate as it:

- uses a different set of assumptions to neutron diffraction.
- is not as sensitive to texture/ varying microstructure within a material as neutron diffraction.
- can produce a 2D map of the stress normal to the plane of a cut, therefore broadening the area of measurement.
- does not require a stress-free calibration sample.

The use of neutrons as a probe to obtain measurements of stress in real components is well documented, with existing literature providing numerous examples of such studies. For example, neutron diffraction is often applied to study weld induced residual stresses in the heat affected zone. In these cases, the geometry is often simple. An example of such a study is given by Brown et al., (2011) in which neutron diffraction was used to obtain measurements of the weld zone in electron beam welded cylinders. Another study by Ahn et al., (2018) focussed on the measurements in fillet welded AA2024-T3, across a simple plate geometry.

A review of existing literature with a focus on neutron diffraction measurements in castings, revealed a study by Johnson et al., (2011) which considered the measurement of a 'stress lattice' geometry. This geometry as detailed in section 3.1 is a simplified cast design to ensure the generation of residual stress due to different section thicknesses, and is used as a simple test piece rather than considering a real component which could be larger and more complex. Another study given by Spooner et al., (1996) investigated residual stresses before and after a stress relieving heat treatment in cast grey iron brake discs, while Li et al., (2007) compared neutron diffraction measurements of an engine compressor disc subjected to a quenching heat treatment with simulation models.

The measurement of residual strains using neutron diffraction can be challenging when applied to a complex geometry. Additional consideration may be required to ensure correct orientation and alignment of the neutron beam in the location of interest, while minimising beam attenuation. The available beam path length is also dependent on the material composition, while the potential for inhomogeneous microstructure i.e. large grains and preferred growth direction (texture), could present a challenge due to the sensitivity of neutron diffraction to microstructure variation (Withers & Bhadeshia, 2001). A literature review of existing studies, which obtained neutron diffraction measurements in complex components, revealed a limited number of examples. However, the work of Lombardi, Sediako, Machin, Ravindran and Mackay, (2017) applied the technique to an aluminium cast cylinder head which contained a grey cast iron liner. In this study measurements were obtained both in-situ during a heat treatment process and ex-situ along the cylinder bridge. Aluminium allows for a greater neutron penetration depth compared to the cast iron geometry of the turbine housing (Withers & Bhadeshia, 2001). The measurement of residual stresses in a fully cast iron structure could add difficulty to the experiment, as the neutron beam is limited to approximately 60mm while up to 200mm is possible in Aluminium (Pirling, 2017b).

Measurements of residual stress have not been obtained on a turbine housing before and a key location of interest within the housing is the tongue and divider wall region,

which as detailed in Section 2.3 is a thin section (~2-3 mm at the narrowest point) and completely hidden by the surrounding geometry of the housing. This makes the measurement, and alignment of the neutron beam in the correct location challenging. In addition the potential inhomogeneous microstructure of the ductile cast iron could present an additional challenge.

The contour method is still very much under development and is relatively new compared to other techniques. Its application to new, challenging structures which push the boundaries of the technique is important. A review of existing literature suggests the contour method is regularly applied to what can be considered simple components such as welded plates. In these studies, similar to previous neutron diffraction studies, the focus is frequently on the heat affected weld zone rather than the surrounding geometry which is often simplified. An example of such a study is given by Turski and Edwards, (2009), in which they measured residual stresses in a single weld bead on a 316L stainless steel plate. Another study by Shi, Price, and Hung (2018) used the contour method to investigate the weld region of arc weld blocks with comparisons made to FEA simulations of the welding process. However, with an increase in the complexity of a component the sectioning, constraint and data analysis requirements increase in difficulty. One such study which attempts to obtain measurements of a complex component is given by Tanner and Conroy (2016), in which they applied the contour method to a cast, Co-Cr-Mo femoral knee implant. The study highlighted difficulties in the measurements due to a possible variation of stiffness during the sectioning process.

The turbine housing consists of a large variation in section thickness which could prove demanding for the contour method. Toparli and Fitzpatrick (2015) detail the difficulties in applying the contour method to a thin component, in this case a 2 mm thick aluminium plate. The study presents the use of sacrificial layers, which were attached to both surfaces of the thin sample to minimise cutting induced errors. It was concluded that although the contour method can be applied to thin geometries, there is the potential of a greater uncertainty in the obtained results compared to thicker geometries. In addition, the study showed that the data analysis of the measured

contour faces is much more sensitive, and it is recommended to compare the results against another measurement technique. The varying thickness of the turbine housing with a range of both thick and thin sections will present an additional challenge. It is clear to the author, after reviewing existing literature and through conversations with specialists at one of the UK's leading contour measurement facilities, Stress Map, part of the Open University (Stress Map, 2018), that the measurement of residual stress in such a complex geometry is pushing the boundaries of the technique and will add to the field of research in this area (Araujo De Oliveira, 2018).

Further information exploring the theoretical background of both neutron diffraction and the contour method will be presented in Section 4.1.1 and Section 4.1.2 respectively and will be used to inform the experimental methodology.

3.2. Applied stress due to thermomechanical fatigue (TMF)

Thermomechanical fatigue is influenced by temperature, phasing, and strain rate. The relationship between the mechanical and thermal loading can be described as either in-phase (IP) or out of phase (OP). When the two load cycles are OP the material will experience maximum compression at the highest temperature point. For IP loading, the maximum temperature and strain occur at the same time (Wu et al., 2015). Fatigue can eventually lead to crack initiation and failure, and occurs due to some type of cyclic loading, in the case of thermomechanical fatigue, temperature and mechanical constraint are the primary factors.

A component when subjected to cyclic load, can fail at much lower load than if subjected to a static load, if applications require fatigue strength, ductile material behaviour is beneficial, as the onset of crack initiation is more gradual than in brittle materials, which will fail rapidly. Fatigue can be further categorised into high cycle fatigue (HCF) and low cycle fatigue (LCF). HCF involves many cycles and stresses that are too low to cause plastic strain; for example, due to vibration. LCF involves a lower

number of cycles but a larger stress, large enough to yield the material each cycle, eventually after several cycles fatigue cracks initiate. The turbine housing is subjected to LCF during operation.

The measurement of residual stress/strain both before and after thermomechanical loading on a complex component, appears to be novel. Existing literature provides numerous examples of measurements of TMF induced strain behaviour in single crystals, mechanical test pieces or simple bars. For example Hong, Li, Li, Sun, and Wang (2018), obtained neutron diffraction strain measurements in an austenitic stainless-steel bar subjected to tensile loading at various elevated temperatures. Erdong et al.(2008) obtained measurements from a Ni-based single crystal which had been subject to thermomechanical loading. While Beese, Wang, Stoica and Ma (2018), focussed on in situ neutron diffraction strain measurements of an additive manufactured, nickel-based superalloy in the form of a mechanical test specimen. This study was undertaken to investigate work hardening behaviour as a result of thermomechanical loading, due to uniaxial compression tests at temperatures up to 700°C. However, the measurement of residual stress in an actual component, both before and after operationally induced changes in residual strain, appears to be unique. Discussions with Instrument scientists at the ILL neutron facility also suggest such measurements using neutron diffraction have not been obtained before using the SALSA instrument (Pirling, 2017).

3.2.1.Measurement of strain using high temperature strain gauges

The measurement of applied strain due to elevated temperature, can be achieved with conventional resistive strain gauges which are composed of thin electrically conductive wires. If the conductive wire is stretched due to an applied tensile strain the thickness of the wire will reduce, likewise the thickness of the wire will increase with the application of a compressive load. This change in the dimensions of the wire affect its

electrical resistance, which can then be recorded and related back to strain, this is the basic principle of resistive strain gauges (Window, 1992).

An example of the measurement of strain within a turbocharger turbine housing during thermal cycling is given by (Hosny, 2001) with micro strains in the region of 2500-4500 $\mu\epsilon$ observed, the largest strain magnitudes were in a region subjected to high plasticity with crack initiation taking place. This was attributed to the housing deforming due to the constraint of the v-band connection to the bearing housing, no simulation model was presented for comparison to the observed results.

4. Theoretical Background

The purpose of this chapter is to provide an overview of the theoretical background relating to the experimental measurement techniques and numerical simulations. This will include a review of the supporting theory behind the neutron diffraction and the contour method residual stress measurement methods.

4.1. Residual stress measurement

4.1.1. Neutron diffraction

Diffraction occurs when waves scatter from an array of scattering objects, such as atoms in a crystalline material providing that the distance between the atoms and the wavelengths (λ) are in the order of the same magnitude (Bragg, 1913). Engineering materials are polycrystalline in nature, this means that some grains within the material will always be orientated for diffraction to take place. Scattered waves may be out of phase, in which case they will interfere with one another destructively. However, if they are in-phase they will interfere constructively, and an outgoing wave will be produced. From equation 4.1 the extra path length travelled by the waves scattered from the lower scattering plane is $2d\sin\theta$ where θ is the angle of incidence between the incident waves and the scattering plane. If this distance is equal to $n\lambda$ (where n is an integer), the result is Bragg's Law (Bragg, 1913) which outlines the condition for constructive interference to occur.

$$n\lambda = 2d_{hkl}\sin\theta \quad (4.1)$$

If the conditions for constructive interference are satisfied as shown in Figure 4.1, a diffraction pattern with sharp peaks of intensity will be produced with each peak of intensity corresponding to diffraction from atomic planes with interplanar spacing d_{hkl}

where hkl are miller indices and used to label reflections (diffraction peaks) corresponding to specific planes. Higher order ($n=2, 3$, and 4 etc.) scattering from crystal planes d_{hkl} is indistinguishable from first order ($n=1$) scattering from planes $d_{hkl}/2$, $d_{hkl}/3$, $d_{hkl}/4$ and so forth, and therefore for neutron scattering the Bragg equation can be reduced to equation 4.2.

$$\lambda = 2d_{hkl}\sin\theta \quad (4.2)$$

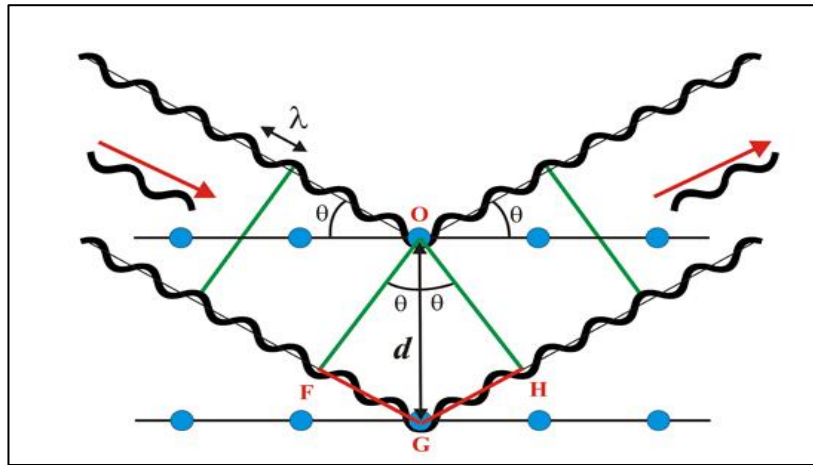


Figure 4.1 Diagram demonstrating the Bragg equation and the conditions for constructive interference.

By observing the intensity of diffracted neutrons from different angles θ , which is half the scattering angle 2θ , or by varying the incident wavelength of the radiation it is possible to determine the d_{hkl} spacing of the lattice plane. When a lattice plane deforms elastically, the interplanar d_{hkl} spacing will vary, increasing if a tensile stress is present and decreasing in the event of a compressive stress. This results in a shift $\Delta\theta$ in the diffraction peak positions, by observing this shift in peak position the elastic lattice strain ε can be determined and is derived from the change of the stressed

lattice spacing Δd_{hkl} with respect to a stress-free reference lattice spacing d_{0hkl} as shown by equation 4.3 and illustrated in Figures 4.2 and 4.3.

$$\varepsilon = \left(\frac{d-d_0}{d_0} \right)_{hkl} = \left(\frac{\Delta d}{d_0} \right)_{hkl} = -\cot\theta \Delta\theta \quad (4.3)$$

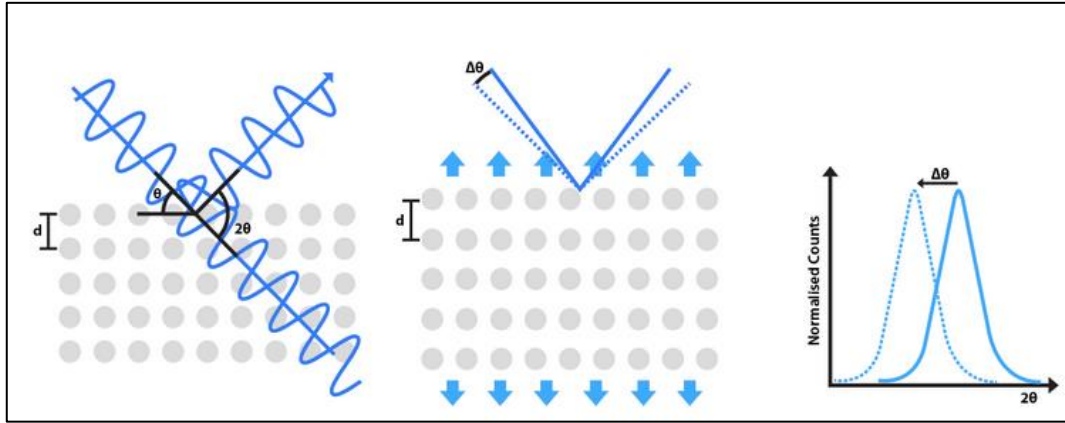


Figure 4.2 The incident and diffracted neutron beam with a change in 2θ angular position reproduced from (Veqter, 2018a)

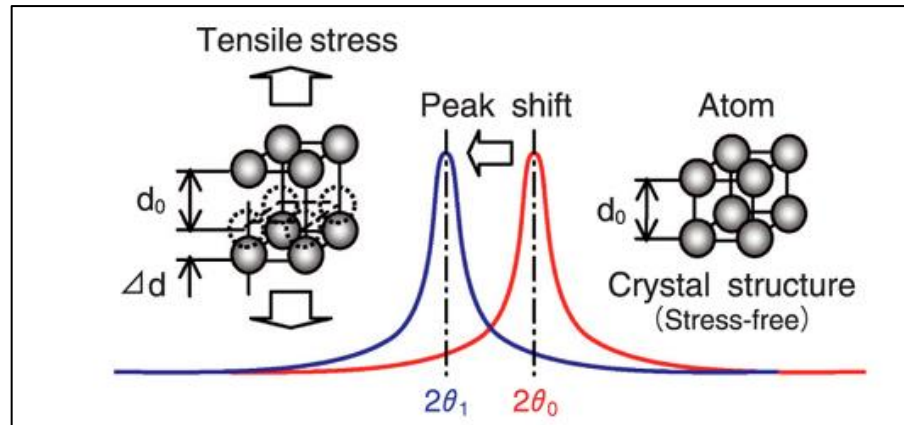


Figure 4.3 The shift in peak position as a result of a change in the interplanar spacing reproduced from (Kanematsu, Noguchi, Yasuda, & Suzuki, 2008).

The measured interplanar spacing d_{hkl} can be related to the lattice parameter of the unit cell to describe its size. There are three parameters a , b , c which describe the

length of the unit cells edges along with three angles as shown in Figure 4.4. For a cubic material such as SiMo 5.1 $a=b=c$ and the lattice parameter can be determined using equation 4.4.

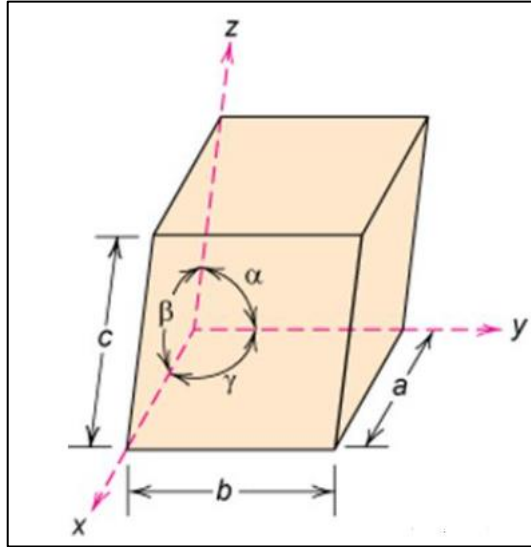


Figure 4.4 Lattice geometry for a cubic structure $a=b=c$

$$d_{hkl} = \frac{a}{\sqrt{h^2 + k^2 + l^2}} \quad (4.4)$$

The neutrons used in diffraction experiments are produced using either a nuclear reactor which results in a constant wavelength neutron beam, or via a spallation source producing a pulsed neutron beam. In reactor-based neutron diffraction, a nuclear reactor generates high energy neutrons by fission of U-235 nuclei after neutron capture. This method is well suited to conventional $\theta/2\theta$ scanning in which the incident wavelength is selected by a monochromator and kept constant while the scattering angle is measured in a single hkl direction i.e. a single peak is analysed. The SALSA strain scanner at the ILL facility utilises a constant wavelength neutron source (Institut Laue-Langevin, 2019). A schematic of the SALSA instrument detailing the positioning of the sample as well as the direction of the neutron beam, and the positioning of the detectors is shown in Figure 4.5.

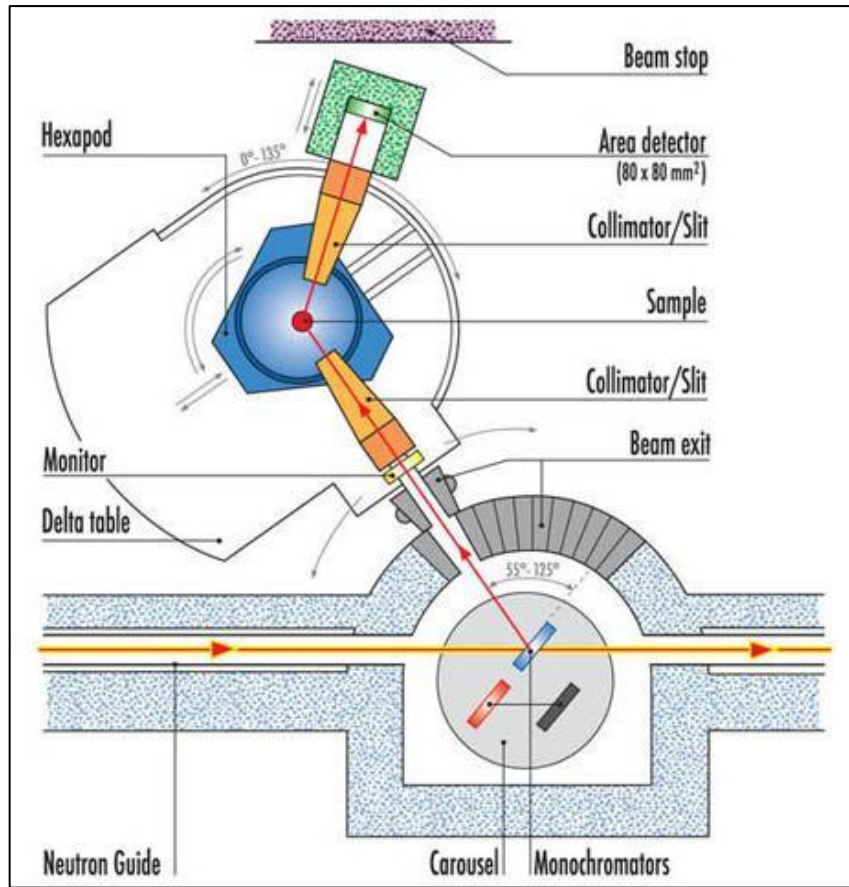


Figure 4.5 A Schematic diagram of the SALSA strain scanning instrument, which utilises a constant wavelength neutron beam. Reproduced from (Institut Laue-Langevin, 2019).

Pulsed spallation sources produce neutrons because of the collision of a proton beam with a heavy metal target and have a fixed Bragg angle (usually $2\theta = 90^\circ$). Therefore, the independent variable becomes the neutrons wavelength rather than the scattering angle. For each pulse of neutrons, there is a range of neutron energies produced in a short pulse, which are slowed down with the use of a moderator. The wavelength of each detected neutron can then be determined from its time-of-flight (TOF) (Oliver, 2008). DeBroglie's relation gives momentum p and therefore neutron speed v_n ;

$$p = m_n v_n = \frac{h}{\lambda} \quad (4.5)$$

Where m_n is the mass of the neutron and $\hbar$ Planck's constant. The neutrons will then travel down the path length, L_n from the moderator to the detector with the time-of-flight (TOF), the differing neutron energies will result in neutrons arriving at the detector at different times.

$$t = \frac{L_n}{v_n} \quad (4.6)$$

Therefore, the wavelength of each neutron can be determined as follows;

$$\lambda = \frac{\hbar t}{m_n L_n} \quad (4.7)$$

For a polycrystalline material which has several lattice planes of different orientations, the TOF method will result in a spectrum of diffraction peaks recorded as a function of TOF at a fixed scattering angle (usually 90°), with each peak corresponding to a (hkl) family of lattice planes. In the case of the TOF method, elastic strain can therefore be determined by:

$$\varepsilon = \left(\frac{d - d_0}{d_0} \right)_{hkl} = \frac{\Delta \lambda}{\lambda} \quad (4.8)$$

An example of a strain scanning instrument which utilises a pulsed neutron source is the ENGIN-X instrument at the ISIS neutron and muon source in the UK (Santisteban, Daymond, James & Edwards, 2006). Figure 4.6 details a schematic of the ENGIN-X layout.

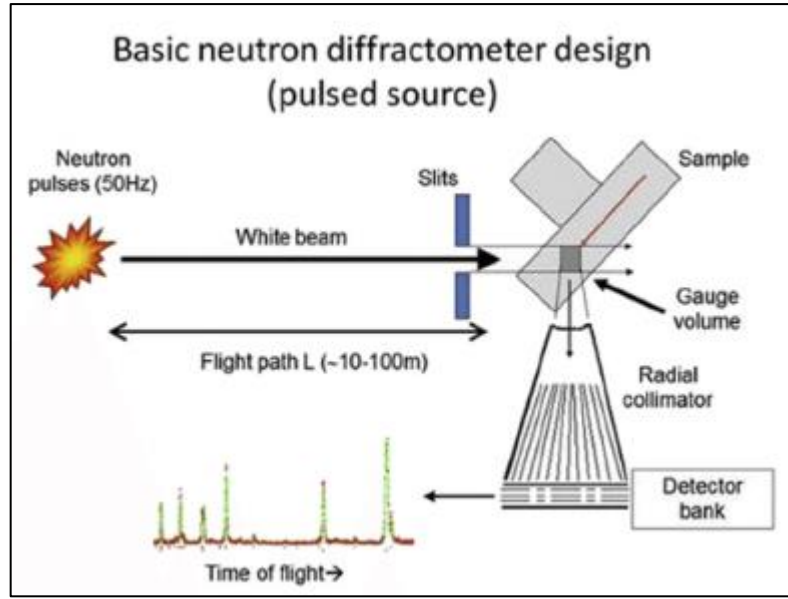


Figure 4.6 Schematic of experimental set up of the ENGIN-X strain scanner which utilises a pulsed neutron source. Reproduced from (Fitzner et al., 2016). The neutron path length is given as L.

The diffraction technique permits the determination of elastic strain for an interplanar spacing in the direction of the bisector of the incident and diffracted beam, therefore, to determine the full strain tensor would require measurements of the elastic strain in at least six independent directions. However, assumptions regarding the strain directions can generally be made from the geometry of the sample, allowing measurements to be performed along three mutually orthogonal directions. The mean stress in the sampled gauge volume can then be determined for each of the stress components ($\sigma_x, \sigma_y, \sigma_z$) using the relevant elastic constants with equations 4.9 to 4.11, where E is the Young's Modulus and ν Poisson's ratio.

$$\sigma_x = \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\epsilon_x + \nu(\epsilon_y + \epsilon_z)] \quad (4.9)$$

$$\sigma_y = \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\epsilon_y + \nu(\epsilon_x + \epsilon_z)] \quad (4.10)$$

$$\sigma_z = \frac{E}{(1 + \nu)(1 - 2\nu)} [(1 - \nu)\varepsilon_z + \nu(\varepsilon_x + \varepsilon_y)] \quad (4.11)$$

It is important to take care when selecting the appropriate elastic constants for the direction normal to the $\{hkl\}$ plane considered, as most metals are not truly isotropic and using the isotropic bulk response elastic constant may not be appropriate. Before plastic yielding, lattice planes can differ in their response to a macroscopically applied stress due to elastic anisotropy (Withers, 2007) resulting in a variation in the lattice spacing in proportion to any applied stress. For TOF measurements the macroscopic elastic strain can be determined using a multi peak Rietveld refinement analysis which is a full fit of the diffraction profile using the lattice parameter a and bulk elastic constants can be used to determine the macroscopic residual stress. In comparison neutron diffraction from a reactor source with a single wavelength produces a single peak and the values appropriate to the specific $\{hkl\}$ crystallographic plane on which the strain is measured should be used. However, the sampled gauge volume, V_g used in neutron diffraction is typically much larger (in excess of two orders of magnitude) than that of the grain size of the material (IAEA, 2005), and therefore larger than the characteristic volumes for type II, V_0^{II} and type III, V_0^{III} microstresses over which a given stress will average to zero. As a result micro stresses, for which the characteristic volume of V_0^{II} and $V_0^{III} < V_g$, will normally give rise only to peak broadening (Withers, 2007) and will not result in a shift in peak position which occurs due to a change in strain, therefore the measured strains are often suitable for the determination of macro stress. The measured strain is an average taken of the number of diffracting grains in the gauge volume.

The neutron diffraction technique is sensitive to the measurement of elastic strains only, as macroscopic plastic strain as a result of inelastic strain occurs by slip processes and the passage of dislocations through the crystalline lattice and does not change the interplanar lattice spacing (Zhang et al., 2009). Each grain plastically deforms by different amounts due to plastic anisotropy and intergranular misfits can be introduced which can cause type II intergranular stresses. The potential error due to

plasticity occurring can be minimised by either using the Reitveld refinement which considers many diffraction peaks and therefore averages out any deviations in plasticity or by using reflections that are linear through the elastic/plastic regions with a monochromatic constant wavelength source (Withers, 2007).

4.1.2. The contour method

The supporting theory behind the contour method is a variation of Bueckner's superposition principle (Bueckner, 1958). Considering Figure 4.7, an explanation of the principle given by Prime (2001) can be summarised through steps A-C. First, the component is shown in (A) in its initial state containing the original residual stress distribution. The part is then cut in two equal parts (B), which relieves the normal (σ_x) and shear (τ_{xy}, τ_{xz}) stresses of the cut surface resulting in displacement of the surface. The stress relieved cut surface is then forced back to its original shape in (C) this is achieved using a 3D elastic finite element model. Prime, (2001) then explains that the original residual stress state can be determined through the addition of those at (B) and (C).

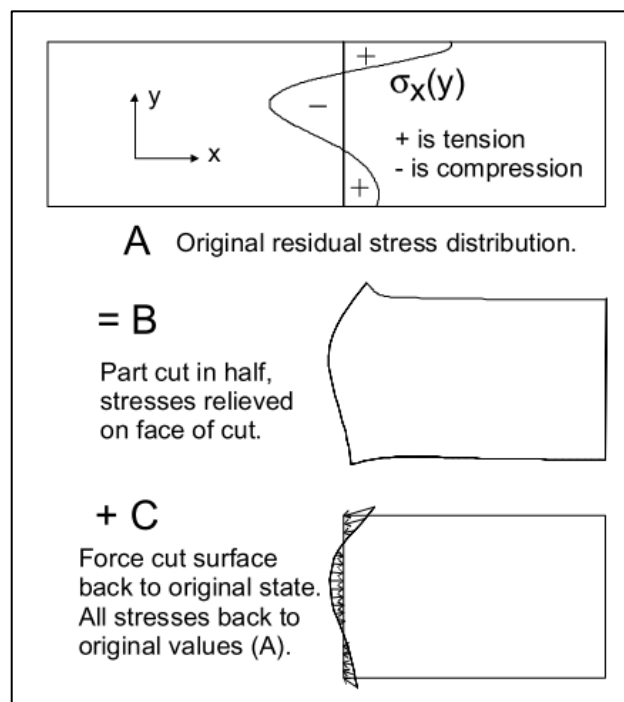


Figure 4.7 Demonstration of the contour method. Reproduced from (Prime, 2001).

Relaxation of both the normal stress and any shear stresses present in the cut face will occur, however, the contour method is limited to the measurement of the normal stresses to the cut plane only. This is because measurement of the contour of the displaced surface will only provide information in the normal (x) direction, not those in the transverse (y) direction. Although the deformation due to shear stress relaxation is not measured, they will not cause an error in any result. This is proven by measuring the displacement of both cut surfaces and determining the average, as detailed by Prime (2001) the shear stress which is anti-symmetric cancels out and only the normal stress relaxation which is symmetric remains. In the analytical finite element analysis, the displacements of the surface in the transverse direction are constrained and set to zero. This is the case even if residual shear stresses were non-zero.

4.2. Measurement of applied strain at high temperatures

Parts of this section were previously published in Gannon, Shoghi, Kilcoyne, Pirling, & Zhang (2016). The change in resistance is typically measured using a Wheatstone bridge arrangement from which strain can then be determined. The sensitivity of a strain gauge to strain can be expressed as a gauge factor (GF) which is the ratio of relative change in electrical resistance $\Delta R / R$ to the mechanical strain, ϵ .

$$GF = \frac{\Delta R / R}{\Delta L / L} = \frac{\Delta R / R}{\epsilon} \quad (4.12)$$

The use of strain gauges at high temperature is more complex than strain measurements under ambient temperature conditions, due to the fact that the electrical resistance of the strain gauge is now not only affected by strain but by temperature (Hannah & Reed, 1992). This can make high temperature strain measurement challenging.

To correct for temperature, half bridges with two active gauges can be used, with each half bridge wired to a bridge completion unit. This set up consists of two active gauges, one gauge aligned in the direction of the strain component (i.e. axial or hoop) to be measured. The other gauge is aligned in a direction at 90° to the first gauge and therefore subjected to Poisson's strain. The second gauge then corrects for the change in gauge resistance due to temperature. The strain field that exists within the turbine housing during operation is complex. There is the potential that the use of a dummy gauge to account for the change in Poisson's strain, as well as to compensate for temperature, could be a source of uncertainty in the measurement, since the strain behavior may not be uni-axial, and mechanical strain could affect the dummy gauge.

The voltage output, V_{out} from a Wheatstone bridge is given in (Hoffmann, 2001) and shown in equation 4.13.

$$V_{out} = \left(\frac{R_2}{R_1 + R_2} - \frac{R_4}{R_3 + R_4} \right) V_{ex} \quad (4.13)$$

Where V_{ex} is the supply (excitation) voltage to the bridge, R_1 and R_2 are the active gauges and R_3 and R_4 are the fixed resistances. The arrangement is also shown in Figure 4.8.

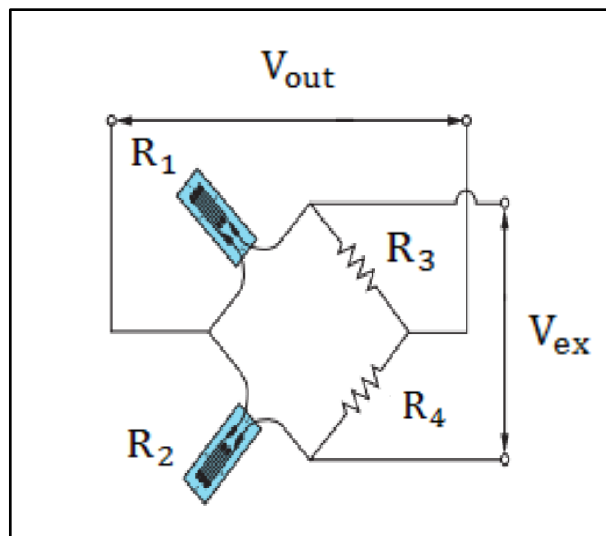


Figure 4.8 Half-bridge Strain gauge circuit diagram

The Wheatstone bridge arrangement is considered balanced when:

$$\frac{V_{\text{out}}}{V_{\text{ex}}} = 0 \quad (4.14)$$

This occurs when:

$$R_1 = R_2 = R_3 = R_4 \text{ or } R_1:R_2 = R_3:R_4 \quad (4.15)$$

If the resistors vary, the bridge is unbalanced and an output voltage V_{out} will appear. It can be assumed that the variation in resistance ΔR_i is much smaller than the resistance R_i therefore:

$$\frac{V_{\text{out}}}{V_{\text{ex}}} = \frac{1}{4} \left(\frac{\Delta R_1}{R} - \frac{\Delta R_2}{R} + \frac{\Delta R_3}{R} - \frac{\Delta R_4}{R} \right) \quad (4.16)$$

With R_1 and R_2 being the only active gauges:

$$\frac{V_{\text{out}}}{V_{\text{ex}}} = \frac{1}{4} \left(\frac{\Delta R_1}{R} - \frac{\Delta R_2}{R} \right) \quad (4.17)$$

The relative change of the resistance of a strain gauge is described by equation 4.18.

$$\frac{\Delta R_0}{R_0} = GF \cdot \varepsilon \quad (4.18)$$

Where ΔR_0 is the change in resistance caused by strain, R_0 is the resistance of the undeformed gauge and GF is the gauge factor of the strain gauge.

Combining equation 4.17 and 4.18 gives;

$$\frac{V_{out}}{V_{ex}} = \frac{GF}{4}(\varepsilon_1 - \varepsilon_2) \quad (4.19)$$

In this case ε_1 is the strain applied to strain gauge 1, normal to this a contraction will occur in the second strain gauge ε_2 which is subjected to Poisson's ratio ν hence:

$$\varepsilon_1 = \varepsilon \quad (4.20)$$

$$\varepsilon_2 = -\nu\varepsilon \quad (4.21)$$

Therefore, the output voltage can be defined as:

$$V_{out} = \frac{V_{ex}}{4} (1 + \nu)GF \cdot \varepsilon \quad (4.22)$$

4.3. Numerical simulation

4.3.1. Heat transfer

During the life cycle of the turbine housing, the transfer of heat is continually taking place, whether it's during the casting process as the molten iron solidifies and cools, during the annealing heat treatment cycle, or finally during operation.

Heat can be transferred by three mechanisms; conduction, convection and radiation and all three types of heat transfer occur throughout the turbine housings life cycle.

4.3.1.1. Conduction

Heat transfer occurs via conduction when a temperature gradient exists across a body with heat energy moving from a hot region to a cool body. This process for a one-dimensional case is given by Fourier's Law:

$$q = -kA \frac{dT}{dx} \quad (4.23)$$

Where q is the conductive heat transfer rate, k the thermal conductivity of the material, A the area normal to the direction of the heat transfer and $\frac{dT}{dx}$ the temperature gradient in the direction (x) of the heat flow.

The governing partial differential equation for conductive un-steady heat transfer in a three dimensional Cartesian coordinate system is represented by equation (4.24) and can be used to describe the solidification with phase change and the temperature inside a casting (Holman, 1997). It considers the change in temperature that occurs within a body over time and the rate of heat energy generated.

$$\rho(T)c_p(T) \left(\frac{\partial T}{\partial t} \right) = \frac{\partial}{\partial x} \left(k_x(T) \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left(k_y(T) \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial z} \left(k_z(T) \frac{\partial T}{\partial z} \right) + \dot{q} \quad (4.24)$$

Where ρ is the density, c_p the specific heat, T is the temperature, k_x , k_y and k_z are the thermal conductivities in the x , y and z directions respectively and $\dot{q}$ is the latent heat.

When a change in phase occurs due to solidification, a change in energy takes place and latent heat is generated during the transition from the liquidus to solidus temperature. The heat generated due to the latent heat is given by (Janik & Dyja, 2004):

$$\dot{q} = \rho(T)_s L \left(\frac{\partial f_s}{\partial t} \right) \quad (4.25)$$

Where L is the latent heat of fusion, $\rho(T)_s$ the density of the solid and f_s the solid fraction.

A change in phase relates to a rapid change in enthalpy with respect to temperature, hence the non-linear nature of a phase change analysis. The enthalpy relationship is often used to model the process of phase change as it incorporates latent heat and is given by:

$$\frac{\partial H(\partial T)}{\partial t} = \rho(T)c_p(T)\frac{(\partial T)}{\partial t} - \rho(T)_sL\left(\frac{\partial f_s}{\partial t}\right) \quad (4.26)$$

Hence

$$\frac{\partial H(\partial T)}{\partial t} = \frac{\partial}{\partial x}\left(k_x(T)\frac{\partial T}{\partial x}\right) + \frac{\partial}{\partial y}\left(k_y(T)\frac{\partial T}{\partial y}\right) + \frac{\partial}{\partial z}\left(k_z(T)\frac{\partial T}{\partial z}\right) \quad (4.27)$$

Or simplified to;

$$\frac{\partial H(\partial T)}{\partial t} = \nabla(k(T)\nabla T) \quad (4.28)$$

Where H is enthalpy. To solve the heat conduction equation, the thermal boundary conditions for the surfaces regions must be considered. The boundary conditions for the thermal simulation of a casting are convection and radiation, and the heat conducted through the body must be equal to the sum of the convection and radiation heat losses.

4.3.1.2. Convection

Heated particles within a fluid (liquid or gas) will always attempt to move from a region of high temperature to a cooler location. This mass movement of fluid is the mechanism that defines convection and is given by Newton's Law of cooling, which can be used to describe the surface boundary condition between a casting and a mould wall and is given by (Holman, 1997):

$$q_{12} = h_{12}A(T_1 - T_2) \quad (4.29)$$

Where q is the heat flux due to convection, h is the convective heat transfer coefficient, A the total surface area for heat transfer, T_1 the surface temperature of the casting and T_2 the surface temperature of the inner wall of the mould.

While the boundary condition for the heat transfer between the exterior wall of the mould and the surrounding environment is given by:

$$q_{3\infty} = h_{3\infty}A(T_3 - T_{\infty}) \quad (4.30)$$

Where T_3 is the exterior surface area of the mould and T_{∞} the ambient temperature of the environment.

Depending on the source that drives the convective heat flow, convection can be defined as either natural or forced convection. Natural convection occurs due to buoyancy effects, because the heating up of the fluid changes its density. For example, as the air surrounding the turbine housing is heated during operation, it becomes less dense and rises, with cooler air moving to replace it. This behaviour is continually repeated, drawing heat away from the turbine housing.

During operation, the inside surfaces of the turbine housing are subjected to forced convection because of the mass flow of exhaust gas travelling through the housing. The convective heat transfer coefficient, h can be determined to describe the transfer of heat between the solid/ gas boundary layer, and is given by equation 4.31 and is related to the dimensionless Nusselt number Nu , the thermal conductivity k and the hydraulic diameter D_H as defined in equation 4.32

$$h = \frac{Nu k}{D_H} \quad (4.31)$$

A Nusselt number of 1 indicates that the velocity of the fluid flow is zero and consequently heat transfer occurs via pure conduction at the boundary layer. A high Nusselt number indicates convection is taking place.

As the channel through which the exhaust gas flows through the turbine housing is not circular in cross section the hydraulic diameter, D_H is used where:

$$D_H = \frac{4A}{P} \quad (4.32)$$

Where P is the perimeter of the cross section and A the cross-sectional area of the flow.

For forced convection, the determination of heat transfer coefficients for turbulent flow in smooth tubes was first introduced by Dittus and Boelter (1930) with further work by McAdams (1942), resulting in equation 4.33. This equation relates the Nusselt number as a function of the Prandtl, Pr and Reynolds numbers, Re .

$$Nu = 0.023Re^{0.8}Pr^n \quad (4.33)$$

Where n is 0.4 for the heating of the fluid and 0.3 for the cooling of the fluid (Holman, 1997).

The Prandtl number is dependent on the material properties of the exhaust gas and is given as the ratio of the viscous diffusion rate to the thermal diffusion rate by:

$$Pr = \frac{\nu_k}{D} = \frac{c_p \mu}{k} \quad (4.34)$$

Where ν_k is the kinematic viscosity, D the thermal diffusivity and μ the dynamic viscosity of the fluid. The Prandtl number for the majority of gases is constant over a large range of temperatures and pressures, with its value falling within the range of 0.7^n and 1.0^n (Probstein, 1989).

The Reynold's Number (Holman, 1997) is given by equation 4.35 and describes the condition of the fluid flow, i.e. whether it is laminar or turbulent.

$$Re = \frac{\rho u D_H}{\mu} \quad (4.35)$$

Where ρ is the fluid density.

The mass flow rate of the fluid, $\dot{m}$ is related to the velocity, u of the fluid flow by

$$\dot{m} = \rho u A \quad (4.36)$$

Therefore, the Reynolds number can then be related to the mass flow of the fluid flow by:

$$Re = \frac{4\dot{m}}{\pi D_H \mu} \quad (4.37)$$

4.3.1.3. Radiation

Heat transfer via radiation, occurs due to the movement of electromagnetic waves or particles and can occur through a medium or in regions where a vacuum exists.

Electromagnetic radiation because of temperature change is the most important consideration to describe the heat transfer behaviour affecting the turbine housing.

The Stefan-Boltzmann law of thermal radiation describes the heat energy emitted by a blackbody i.e. a perfect emitter of radiation by:

$$q = \sigma A T^4 \quad (4.38)$$

Where σ_{SB} is the Stefan-Boltzmann constant, A the surface area of the body and T the absolute temperature of the body.

While equation 4.38 is valid for a blackbody, in reality most bodies will not radiate energy to such a high degree, instead an emissivity value is introduced, with values ranging from 0 for a perfect reflector up to 1 for a perfect blackbody. Radiation

calculations are also complicated by the fact that electromagnetic radiation when leaving the surface of a body, will vary in direction depending on the surface orientation of the emitting body, this is known as the view factor and related to the geometry of the body (Holman, 1997).

A simpler radiation problem is that of a body completely encased in an enclosure which is comparable to a turbine housing casting within a sand mould. The radiated heat transferred between the surface area A_1 of the casting at temperature T_1 enclosed by the larger surface of the mould at temperature T_2 is given by:

$$q = \epsilon_1 \sigma A_1 (T_1^4 - T_2^4) \quad (4.39)$$

Where ϵ_1 is the emissivity value of the casting.

A similar equation can be given for the radiated heat from the exterior of the sand mould to the surroundings by

$$q = \epsilon_3 \sigma A_3 (T_3^4 - T_\infty^4) \quad (4.40)$$

Where ϵ_3 is the emissivity value of the sand mould, T_3 is the temperature of the exterior surface of the mould and T_∞ the ambient temperature of the environment.

4.3.2. Mechanical behaviour

4.3.2.1. Elasticity

A material is said to behave elastically, if when subjected to a load the induced stresses are below the yield strength or elastic limit of the material. During loading up to the elastic limit, the induced stress is proportional to strain according to Hooke's law, and deformations are completely recoverable once the load is removed. Hooke's law is given by:

$$\sigma = E\varepsilon \quad (4.41)$$

Where σ is stress, ε , strain and E the Young's modulus of the material.

4.3.2.2. Temperature effects

Thermal stresses arise due to different rates of thermal expansion and contraction.

The stress, σ due to a change in temperature is given by

$$\sigma = E\alpha\Delta T \quad (4.42)$$

Where E is the Young's modulus of the material, α the coefficient of linear expansion of the material and T the temperature.

At elevated temperatures the yield and tensile stress of a material reduce as a result a lower level of stress is required to cause plasticity and permanent deformation.

4.3.2.3. Plasticity theory

If a material is subjected to a load which exceeds its yield strength, yielding will occur, resulting in permanent plastic deformation. The material no longer behaves elastically according to Hooke's law and the relationship between stress and strain is lost.

The von Mises yield criterion is given by equation 4.43 and is commonly used in numerical simulation software to model the behaviour of ductile materials to define the limit of elasticity and is expressed in terms of a yield function $f(\sigma_{ij}, \varepsilon_p)$ (Bower, 2008a).

$$f(\sigma_{ij}, \varepsilon_p) = \sqrt{\frac{1}{2}[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]} - \sigma_y(\varepsilon_p) = 0 \quad (4.43)$$

Where σ_{ij} is the stress tensor, $\sigma_y(\varepsilon_p)$ is the yield stress as a function of the total plastic strain under uniaxial tension and σ_1 , σ_2 and σ_3 are the principal stresses. A

material will deform elastically if $f(\sigma_{ij}) < 0$ and plastically if $f(\sigma_{ij}) = 0$. The von Mises criterion therefore enables the conversion of a three-dimensional stress state into an equivalent uniaxial stress state and for ductile materials is given by:

$$\sigma_v = \sqrt{\frac{1}{2}[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]} \quad (4.44)$$

Where σ_v is the von Mises stress or equivalent tensile stress.

To fully model metal plasticity is a complex process and as well as the initial yield criteria which determines when plasticity will first occur other concepts as outlined in detail by Bower (2008b) such as hardening and plastic flow rules are required. A hardening rule is used to describe how the yield surface changes because of plastic deformation and will determine when a material will yield again if the loading is removed and re-applied. An elastic-perfectly plastic material subjected to a load will not work harden and the stress within the material will increase linearly until the yield stress is reached, after which plastic deformation will take place at a constant stress. If strain hardening is considered, repeated loading of the material would require progressively higher loads to induce further plastic deformation.

The behaviour of metals undergoing plasticity can be separated into two categories:

- i. Rate independent plasticity.
- ii. Rate dependent plasticity.

Each category has its own set of rules which dictate material behaviour after yielding. In the following section the discussion will be limited to the change in the hardening behaviour.

4.3.2.3.1. Rate independent plasticity

Rate independent plasticity refers to the instantaneous (i.e. independent of time) onset of plastic strain when the yield point of the material is reached (Ansys Inc., 2010). Rate independent plasticity is used to model strain rates of the order of 0.01-10/s and materials which are deformed at temperatures less than half their melting point (Bower, 2008b). There are two basic hardening rules that are rate-independent, these

are isotropic hardening and kinematic hardening. There is also a non-linear kinematic hardening material behaviour, an example of which is the Chaboche model.

4.3.2.3.2. Isotropic hardening

Isotropic hardening is the simplest way of modelling strain hardening behaviour as it allows the yielded surface to remain the same shape while expanding uniformly in all directions around the same central axis under increasing stress as shown in Figure 4.9. If the load is then reversed the compressive yield point is equal to the highest tensile stress as is shown in the stress-strain diagram in Figure 4.10. The compressive and tensile yield strengths therefore increase at the same rate.

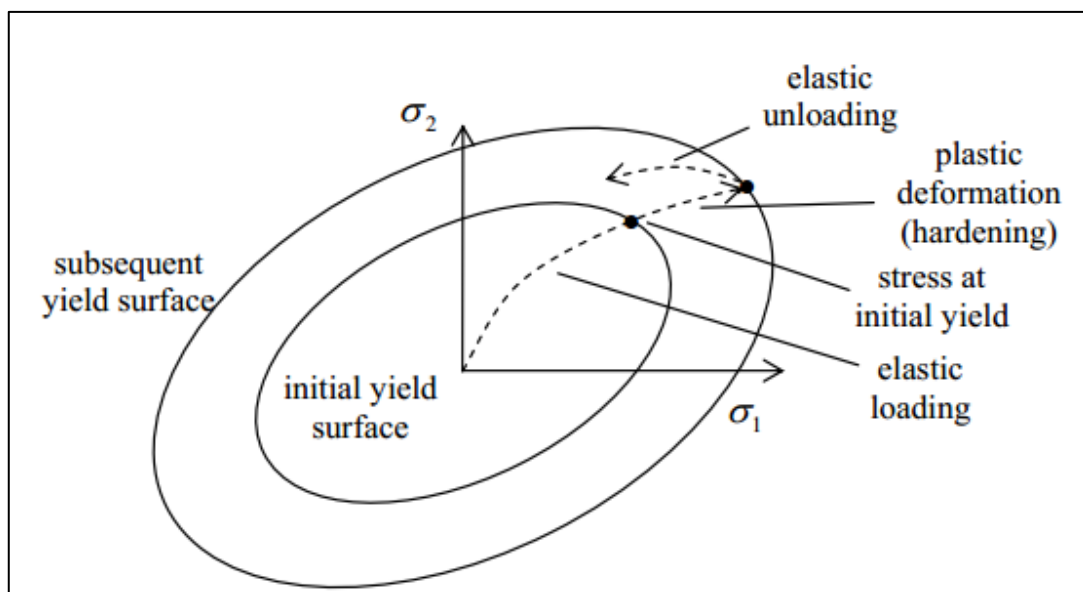


Figure 4.9 The change in the yield surface because of isotropic work hardening.

Reproduced from (Ansys Inc., 2010).

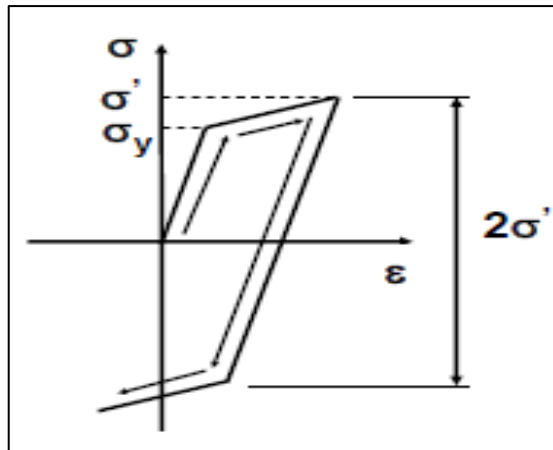


Figure 4.10 Stress-strain diagram of isotropic hardening. Reproduced from (Ansys Inc., 2010).

An isotropic material model can be used to determine the location of crack initiation sites in the turbine housing through the assessment of the accumulated equivalent plastic strain (PEEQ) at the end of a simulated thermal load cycle. For such a simulation, the material model uses isothermal tensile test data. The accumulated PEEQ limit is characteristic of the material and can be determined using LCF/TMF tests and validated with experimental thermal cycle (gas stand/engine tests) typically over 1800 cycles as shown in Figure 4.11. For a ferritic ductile cast iron such as SiMo 5.1 the limit is defined as 0.2%, an accumulated PEEQ value equal to or greater than 0.2% suggests a risk of fatigue crack initiation. The PEEQ method is suitable in cases where failure is predominately due to fatigue damage, and neglects time dependant plastic behaviour.

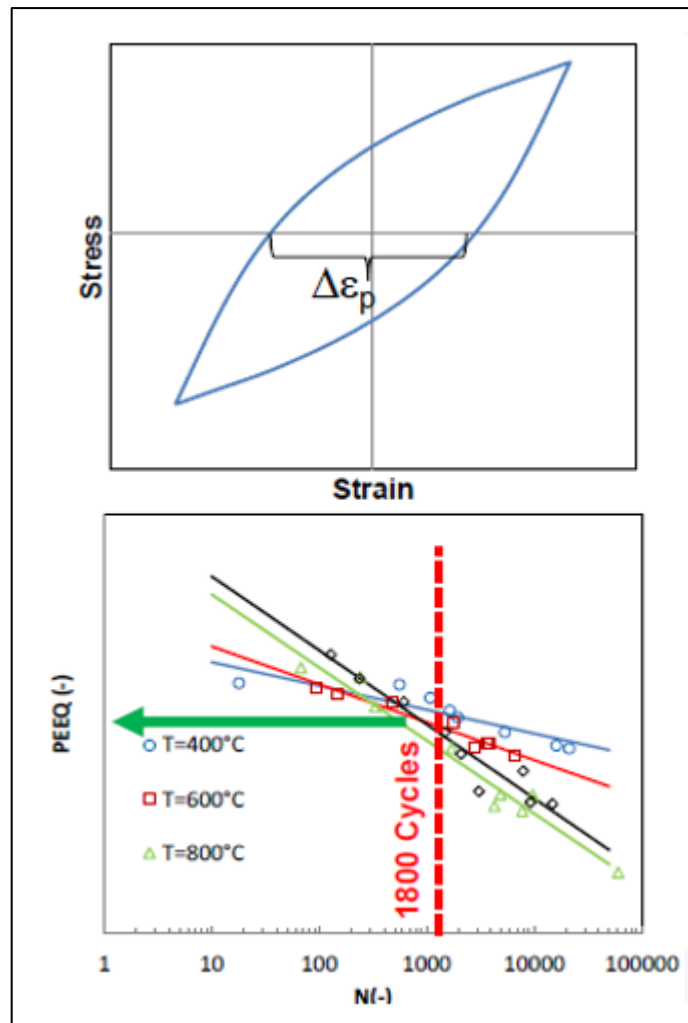


Figure 4.11 (top) typical stress strain hysteresis loop for a LCF-cycle, (bottom) the accumulated PEEQ plotted for several LCF tests against number of cycles (N)

Reproduced from (BorgWarner, 2018).

4.3.2.3.3. *Linear-Kinematic hardening*

Kinematic hardening accounts for the Bauschinger effect (Ansys Inc., 2010) as such when a material is subjected to a tensile load then unloaded and subjected to a compressive load, it will yield in compression at a much lower stress than the initial tensile yield strength. Therefore, the compressive yield strength is no longer the same as the tensile yield strength as was the case for isotropic materials. For linear kinematic hardening the yield surface does not change in either size or shape, instead the yield surface is translated as shown in Figure 4.12a.

The elastic region of the stress-strain curve is now twice the initial tensile yield stress as shown in Figure 4.12b due to the compressive yield strength decreasing at the same rate as the tensile yield strength increases. The yield criterion can be expressed as:

$$f(\sigma_{ij}, \alpha_{ij}) = \sqrt{\frac{3}{2} [(s_{ij} - \alpha_{ij})(s_{ij} - \alpha_{ij})]} - \sigma_y = 0 \quad (4.45)$$

Where s_{ij} is the deviatoric stress, α_{ij} the back stress which relates to the translation of the centre of the yield surface and σ_y is the yield stress which now remains constant. Instead the hardening behaviour is modelled because of the movement of the yield surface which moves linearly with plastic strain. The yield surface is translated by the back stress which is linearly related to plastic strain and given by equation 4.46.

(Bower, 2008b)

$$\Delta\alpha_{ij} = \frac{2}{3} C \Delta\varepsilon_p \quad (4.46)$$

Linear kinematic hardening predicts that the plastic stress-strain curve is a straight line with slope C.

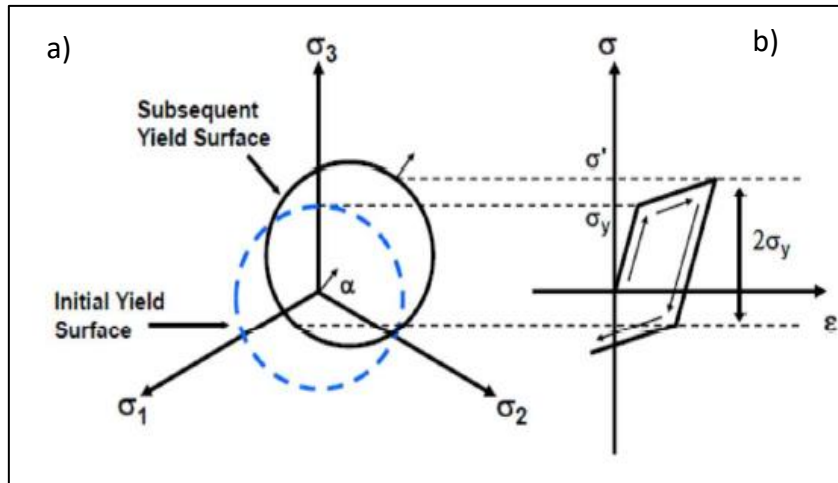


Figure 4.12 a) The change in the yield surface because of linear kinematic work hardening and b) a stress-strain diagram of linear kinematic hardening. Reproduced from (ANSYS, 2012)

4.3.2.3.4. *Non-linear kinematic hardening model*

An example of a non-linear kinematic hardening model is given by Chaboche (Chaboche, 1991). The basic concept of non-linear kinematic hardening is that there is no longer a linear relationship between hardening and plastic strain. Unlike linear kinematic hardening there is a limiting yield surface. This means that the stress-strain curve can behave perfectly plastic, which linear kinematic hardening models do not account for as the slope remains linear (ANSYS, 2012). A non-linear kinematic material model can be used to model the effects of ratchetting and shakedown.

4.3.2.3.5. *Rate dependant plasticity (viscoplasticity)*

Rate-dependant plasticity or viscoplasticity is a theory used to describe the deformation of a material under load, over time. The theory can be applied to describe the mechanisms of high temperature creep, and stress relaxation. Unlike rate-independent plasticity, rate-dependant plasticity allows for continued deformation over time after the application of a load. During the casting process and subsequent heat treatment stage, residual stresses will relax. Therefore, a time dependant creep model is required to determine the magnitude of the final stress condition.

The time dependency of the mechanical behaviour of the material can be described by a 'unified' creep model, which combines multiple inelastic deformation mechanisms.

5. Experimental Methodology

This chapter details the experimental methods that were followed to obtain measurements of:

- Residual stress using the contour method and neutron diffraction.
- Applied stress using strain gauges to measure strain throughout a thermal cycle.

An overview is provided, to explain the combinations of experiments and simulations undertaken.

5.1. Experimental measurements and simulation overview

Experimental measurements were not possible at every stage of the turbine housing life cycle, therefore simulations using MAGMA (Magmasoft, 2018) and ANSY (ANSYS, 2016) have been undertaken and validated against obtained measurements where possible. In total five turbine housings (A to E) were used for all the test work this was necessary due to timing constraints, and since some of the testing was destructive.

All housings were obtained from the same production batch of castings to minimise the chance of variations in the production process, chemical content and mechanical properties. To ensure that the residual stress levels were as similar as possible each of the housings had been through an identical annealing heat treatment process and machined following the standard production process before any experimental work was carried out. The usage of each of the five housings over the course of the experimental test work is given in Table 5.1. Figure 5.1 shows the work flow of both the simulation and experimental measurements that have been completed and highlights points at which comparisons between simulations, and experimental measurement techniques have taken place.

All simulations considered a CAD model of the same design as the tested parts which was based on middle design tolerances.

Turbine Housing	Experimental measurement
A	Residual stress measurements with neutron diffraction (non-destructive), both before and after thermal cycling on a gas stand
B	Residual stress measurements using the contour method (destructive) before thermal cycling on the gas stand.
C	Applied strain and temperature measurements during thermal cycling with the instrumentation of high temperature strain gauges and thermocouples. Dimensional check before and after thermal cycle testing.
D	Used to obtain stress free reference (d_0) piece for neutron diffraction measurements and to obtain test pieces for mechanical and chemical testing
E	Housing was subjected to an extended 1200 thermal cycle test and checked at 300 cycle intervals for crack initiation and propagation.

Table 5.1 Turbine housings used in experimental measurements

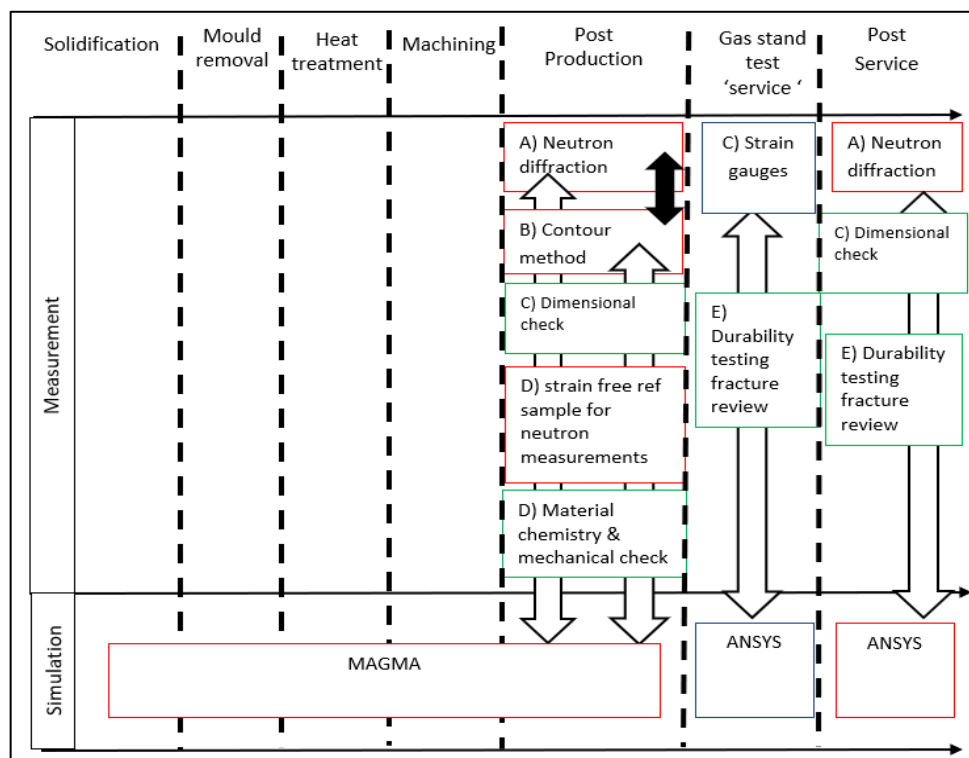


Figure 5.1 Experimental measurements and simulations carried out on turbine housings A-E (white arrow denotes validation point; black arrow denotes comparison between techniques).

A summary of the experimental measurements of stress/strain and the numerical simulations used in this research, as well as their advantages and disadvantages are given in Table 5.2.

Technique	Advantages	Disadvantages
Neutron diffraction	• Non-destructive • Internal measurements • Measurement of tri-axial stresses • Macroscopic stresses can be measured • Measurements can be obtained on large engineering components at select facilities.	• Limited facilities • Expensive/controlled access • Can be sensitive to microstructure • Line scans only • Stress-free reference required for calibration
Contour Method	• Lab based technology • Does not require a stress-free reference sample • Not sensitive to microstructure • Large measurement area, full surface of contour cut • Does not require a stress-free reference sample.	• Destructive measurement • Cutting process needs to be very accurate • Uni-axial stress measurement
Strain gauges	• Can be used to measure applied strain	• High temperature strain gages are expensive • Specialist installation required.
Numerical simulations (MAGMA, ANSYS)	• Cheap • Quick • Variety of materials, boundary conditions • Complex geometries	• Accuracy of boundary conditions • Accuracy dependant on mesh size • Large computation hardware requirements • Large amounts of material data required depending on material model.

Table 5.2 Summary of experimental and simulation techniques used in this thesis

5.2. Test specimen

The turbine housing design used for all the experimental test work and simulation CAD models is shown in Figure 5.2

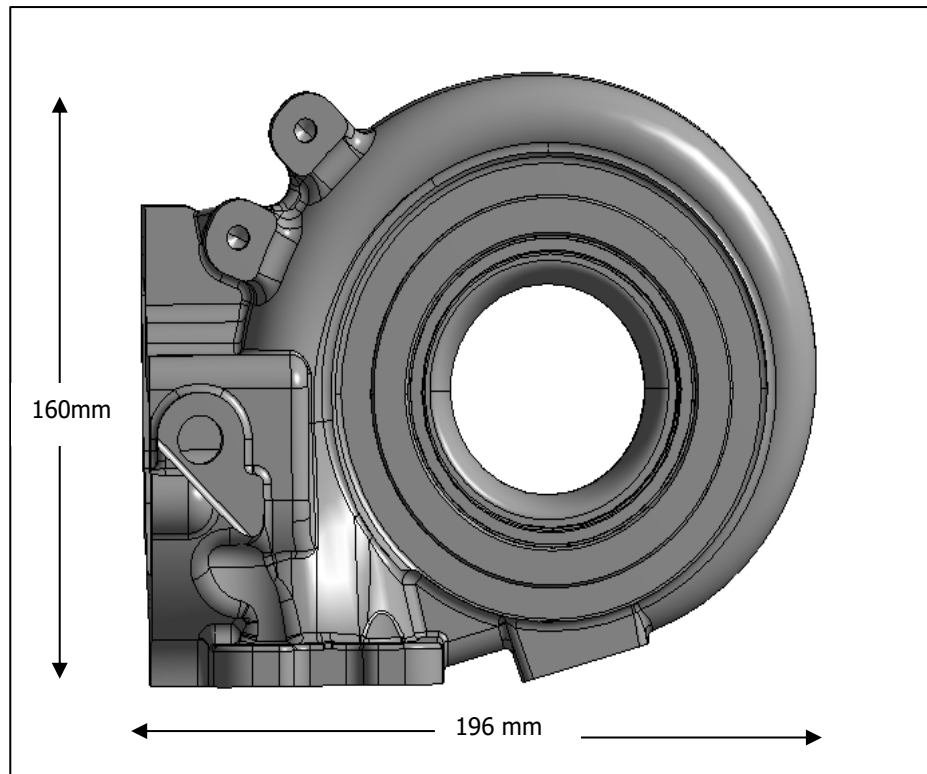


Figure 5.2 Twin volute turbine housing geometry considered for all experimental and simulation models

During the casting process a housing from the batch of castings was tested both mechanically and chemically to ensure it met the requirements for the SiMo 5.1 specification. In addition, one of the turbine housings was dimensionally checked, against drawing requirements both before and after it was subjected to thermal cycling.

5.3. Residual stress measurements

5.3.1. Neutron diffraction procedure

Neutron diffraction measurements of residual stress were performed at the Institut Laue Langevin (ILL) reactor source in Grenoble, France using the SALSA strain scanning instrument (Kilcoyne, Gannon, Kiumars, & Pirling, 2016). SALSA is a monochromatic strain diffractometer which is adapted for the measurement of strain in large engineering components. The instrument benefits from the continuous high flux of the ILL reactor as well as a 'hexapod' sample stage, to allow the movement of large engineering components. A comprehensive review of the instrument is given by Pirling, Bruno, and Withers (2006).

The neutron diffraction technique permits the determination of elastic strain for an interplanar spacing in the direction of the bisector (scattering vector, Q) of the incident and diffracted beam. Therefore, the turbine housing must be positioned so that the required stress component (axial σ_a , radial σ_r , or hoop σ_h) is orientated correctly for measurement. The hexapod sample stage was used to obtain measurements of each strain component, by allowing translations in T_x , T_y and T_z as well as angular rotation through omega (ω). Additionally a eulerian cradle for rotation (tilt) in chi (χ) through a range of 0° to 90° was used for certain measurement positions. The translation directions are shown in Figure 5.3 and the rotation and tilts necessary for the neutron measurement are detailed in Figure 5.4.

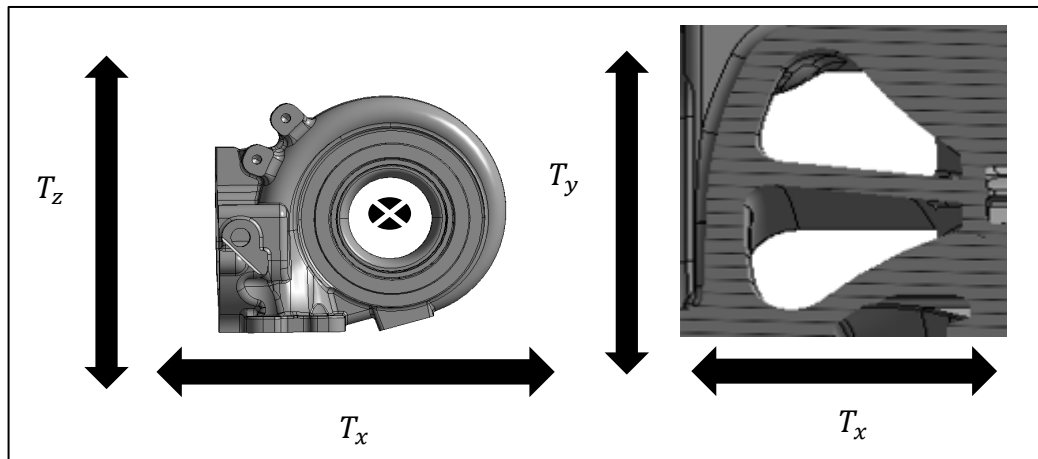


Figure 5.3 The translation directions in relation to the turbine housing.

Calibration of the diffractometer and the detector position were first determined using a steel reference sample, this also allowed for the determination of the wavelength of the neutron source. The turbine housing was then secured to an aluminium plate which was attached to the eulerian cradle and positioned on top of the hexapod sample stage. The turbine housing is shown in position in the SALSA instrument in Figure 5.5.

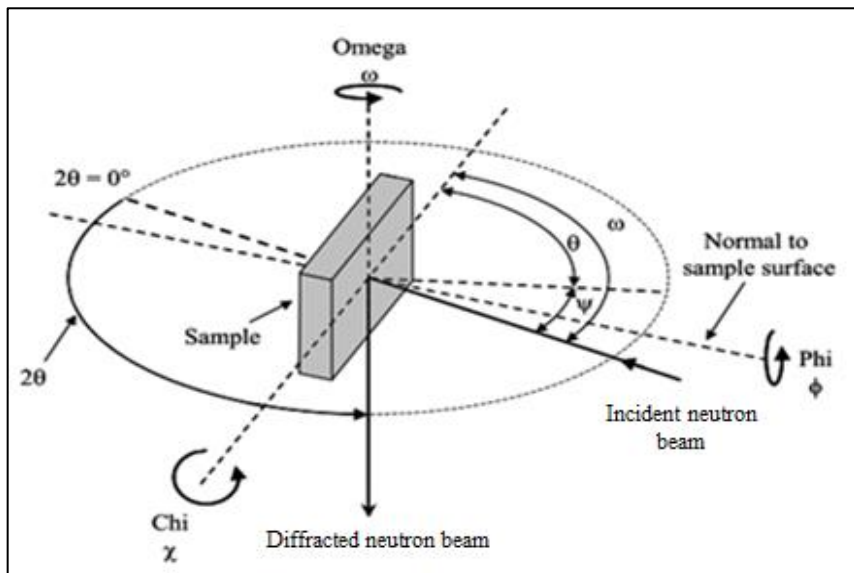


Figure 5.4 The angles and rotations necessary for residual stress measurements.

Reproduced from Fitzpatrick et al. (2005)

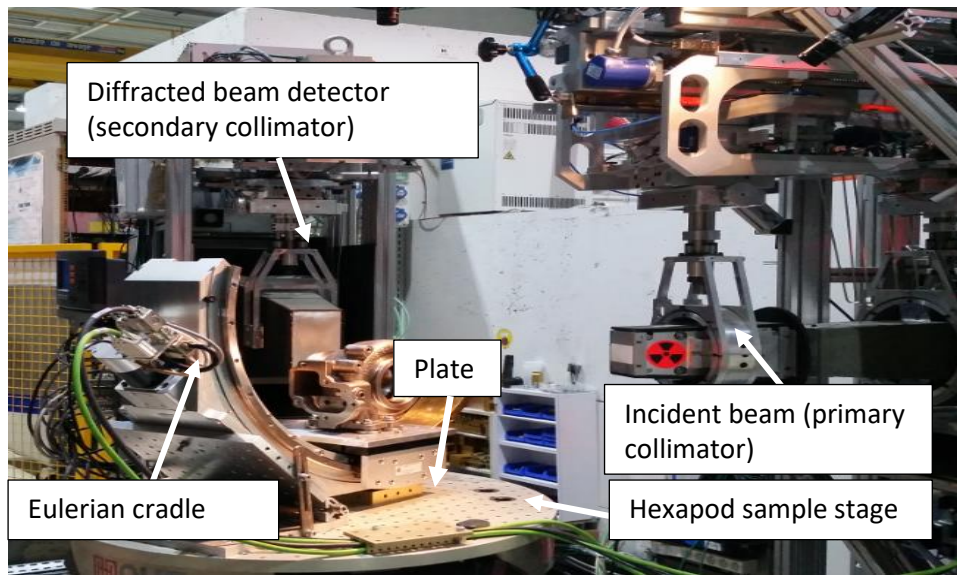


Figure 5.5 The experimental set up on the SALSA instrument

The scattering lattice plane chosen for the body centred cubic cast iron alloy material of the turbine housing was the $\{211\}$ plane. This was chosen as it is a strongly diffracting plane. It is also considered to be the most isotropically elastic and therefore most representative of the bulk strain response when compared to other reflections (Pirling et al., 2006).

Residual stress measurements were obtained in two stages in identical locations on:

- i. A housing prior to thermal cycling.
- ii. The same housing after thermal cycling on a gas stand test.

The wavelength of the neutron flux considered for the experiment prior to thermal cycling was $\sim 1.726 \text{ \AA}$ and corresponded to a peak position of $\sim 95.93^\circ 2\theta$. The wavelength considered for the measurements on the housing after being subjected to the gas stand testing was $\sim 1.61 \text{ \AA}$ corresponding $86.77^\circ 2\theta$. Measurements were obtained for each of the stress directions related to a cylindrical coordinate system as axial σ_a , radial σ_r and hoop σ_h stresses.

5.3.1.1. Measurement procedure

The residual stress measurements consisted of four line scans taken in various locations in the housing as shown in Figure 5.6, which for simplicity are referred to as positions 3, 6, 9 and 12 in relation to a clock face. The positions were chosen to maximise the coverage of the turbine housing and all line scans were taken internally in the housing through the central divider wall which separates the two gas passages. This is often an area prone to fatigue crack initiation and as such, an important location to obtain measurements from.

A cubic gauge volume of dimensions 2mm x 2mm x 2mm, over which the measured strain was averaged, was created by the intersection of the incident and diffracted beam and defined by a series of primary and secondary collimators. The gauge volume was chosen to ensure the sampling volume would remain within the divider wall, which at the narrowest point was ~3mm in thickness.

The alignment of the turbine housing within the neutron beam was achieved by marking reference locations on the bearing flange face in the 3, 6, 9 and 12 positions in line with that shown in Figure 5.6. A series of cameras were then used to correctly position the sample within the gauge volume via a computer-controlled system in which dimensions of the turbine housing were input to align the gauge volume in the location of the internal divider wall.

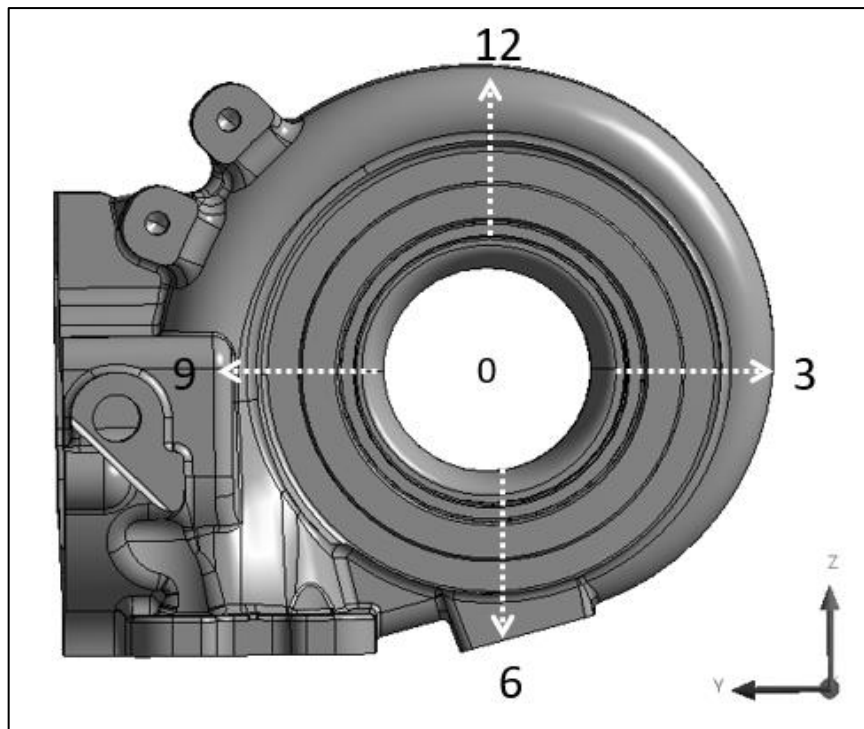


Figure 5.6 Exterior view of the turbine housing and the four arrows indicate the positions along which neutron diffraction measurements were obtained.

Two reference points were used to align the neutron beam within the divider wall, the centre point of the housing at position zero as shown in Figure 5.7 and the bearing side flange.

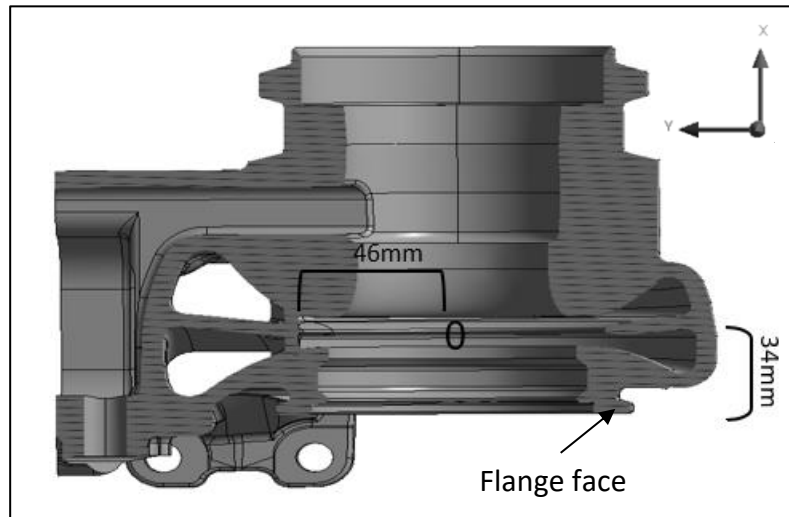


Figure 5.7 The position of the neutron beam in the divider wall referenced from the centre point of the housing and the bearing side flange.

To check the positioning of the gauge volume and to ensure it was positioned within the divider wall, intensity vs. position scans were performed. If the neutron beam was not within the material, there would be a drop in the measured intensity. A gauge volume which is not completely within the material can be a large source of error in the measurement (Creek, Santisteban, & Edwards, 2005). An intensity vs. position scan to locate the centre of the divider using the bearing flange face as a reference is shown in Figure 5.8 and shows that the centre of the divider wall at its leading edge is $\sim 34\text{mm}$. An assessment of the diffracted peak position at 2θ during the same position scan shows a minimal shift in the peak positions suggesting the uncertainty in the measurement due to pseudo strain would be low. During the subsequent measurement the intensity was monitored to ensure the gauge volume remained within the divider wall.

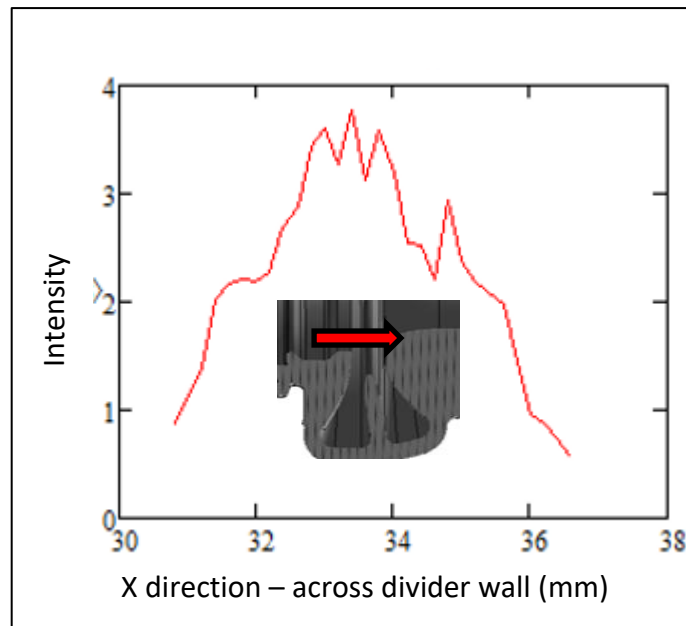


Figure 5.8 Positioning the neutron beam within the divider wall, the intensity scan shows that the centre of the divider wall is ~34mm from the bearing flange reference point.

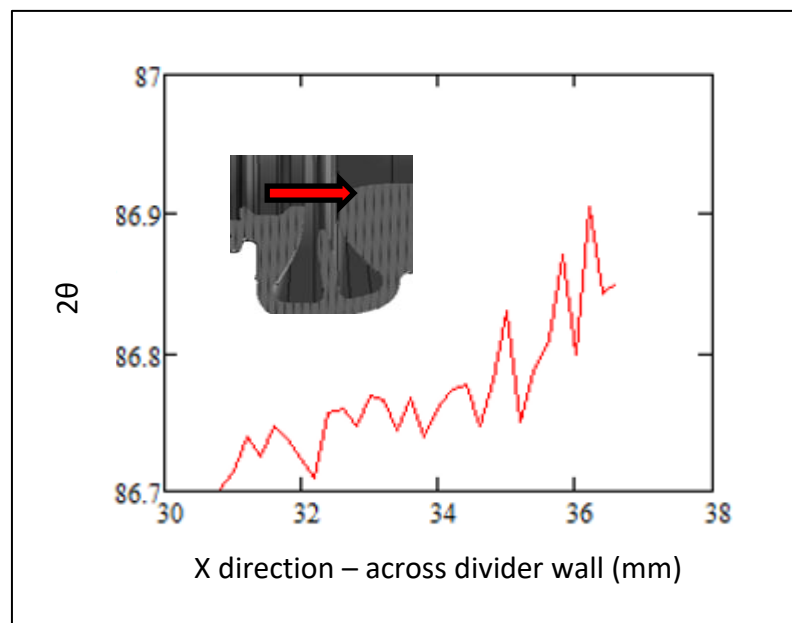


Figure 5.9 Change in 2θ peak position across the divider wall. Only a small variation is observed when the gauge volume is within the divider wall between ~31.4mm and 35.4mm, suggesting that the effect of pseudo strain would be low.

The individual line scans for each location are shown in Figure 5.10 and Figure 5.11.

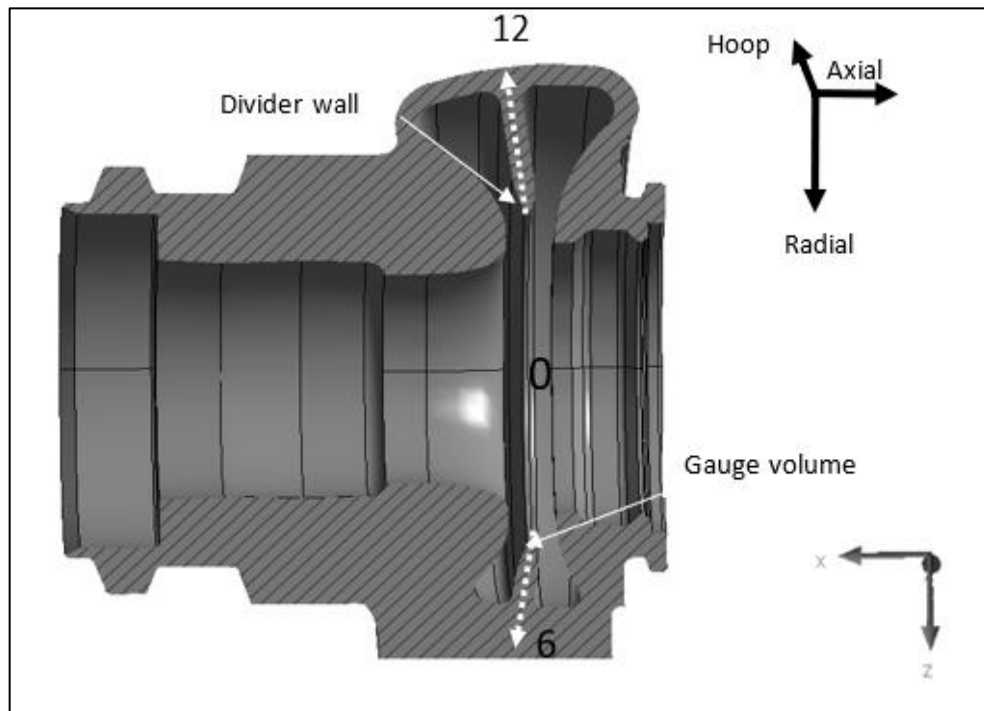


Figure 5.10 Cross sectional view of the neutron diffraction line scans in positions 12 and 6 of the divider wall and the three stress components measured

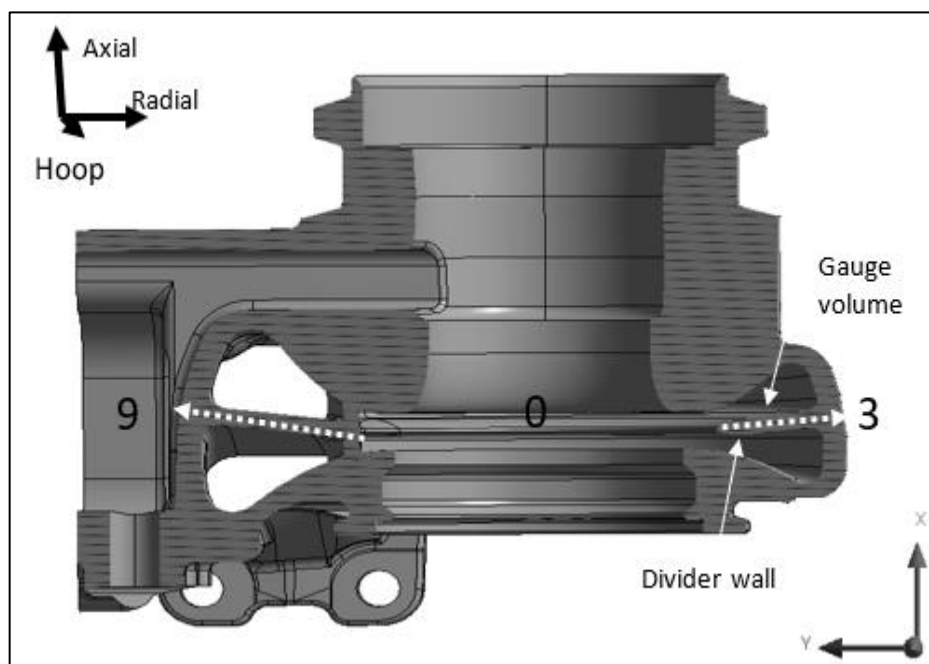


Figure 5.11 Cross sectional view of position 3 and 9 line scans in the divider wall.

Measurements of the residual strains were then obtained by scanning along the divider wall from its edge towards the outer perimeter of the housing.

During the first neutron experiment on the housing prior to gas stand testing measurements were obtained every 1.5mm subsequent testing on the turbine housing after gas stand testing involved measurements every 2.5mm. This change was made to increase the distance covered by the measurements in the limited time available on the SALSA instrument.

Depending on the line scan location and the target strain component, the turbine housing was positioned so that the required strain component was in the direction of the scattering vector, Q . An example of the orientation of the housing for the measurement of each of the strain components at position 6 is shown in Figure 5.12. The positioning of the housing should also minimise the path length that the neutron beam must travel through the material, as path lengths greater than $\sim 60\text{mm}$ will result in a drop-in intensity and increased uncertainty in the measurement.

During the experimental measurements obtained on the turbine housing prior to the gas stand test, difficulty was encountered when attempting to position the neutron beam within the divider wall while maximising the available neutron path length of 60mm. As a result, a full line scan of each position was not possible due to timing constraints. However, the subsequent neutron experiment on the turbine housing after thermal cycling benefited from the lessons learned regarding optimal positioning of the turbine housing from the first experiment resulting in additional measurements covering a greater distance along the line scan locations.

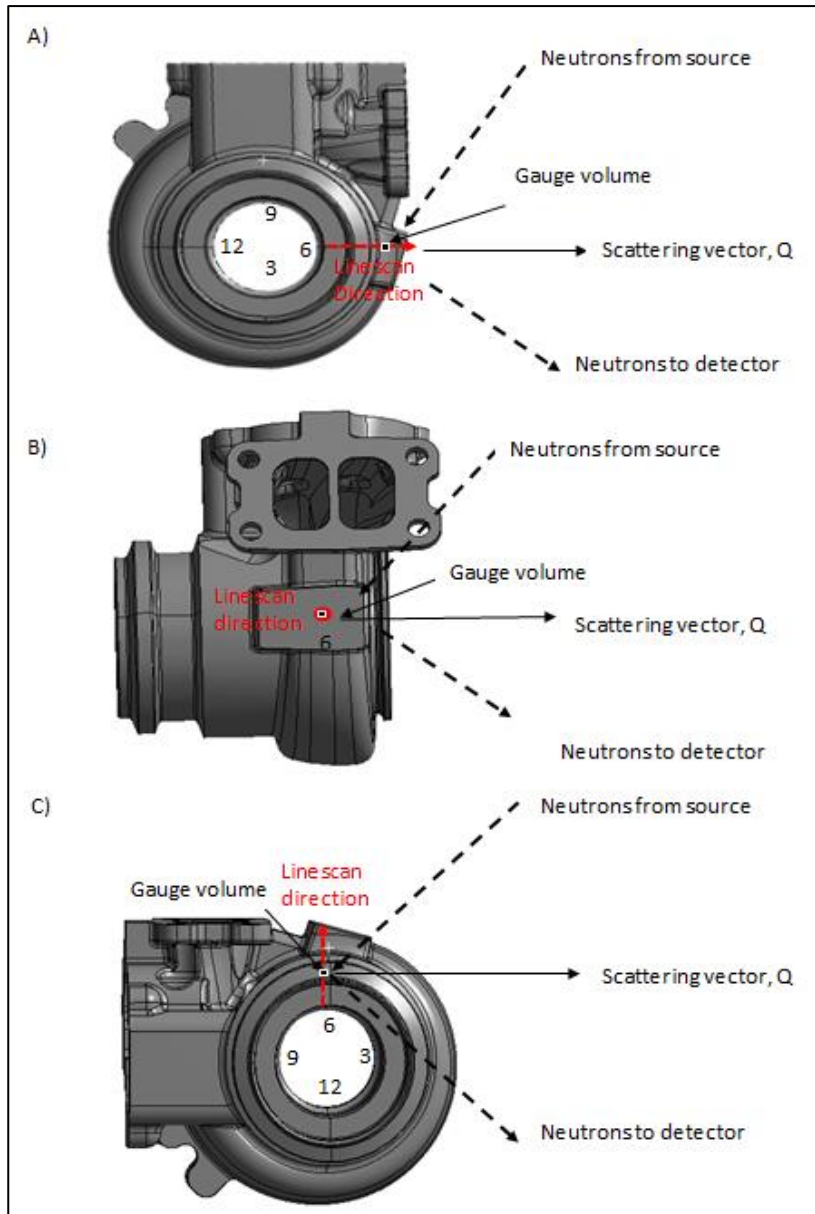


Figure 5.12 Orientation of the turbine housing for the measurement of the three strain directions of the line scan of position 6. A) Radial strain component B) Axial strain component C) Hoop strain component.

5.3.1.2. Stress-free reference sample

The stress-free reference (d_0) sample was obtained from the interior of the turbine housing as shown in Figure 5.13, this location was chosen because as well as having a enough material from which to remove a sample, it was located far from the feeder boss location at the bottom of the housing, therefore minimising the potential effects

of texture, which could arise due to possible large grain size as a result of the slower rate of cooling that takes place in the feeder area. The sample was removed from the turbine housing using wire electrical discharge machining (WEDM), which relieved any stresses that were present as well as minimised the potential introduction of stress during the cutting process. First a rectangular block was removed from the interior of the turbine housing then a 40 x 20 x 2 mm plate was sliced from the block.

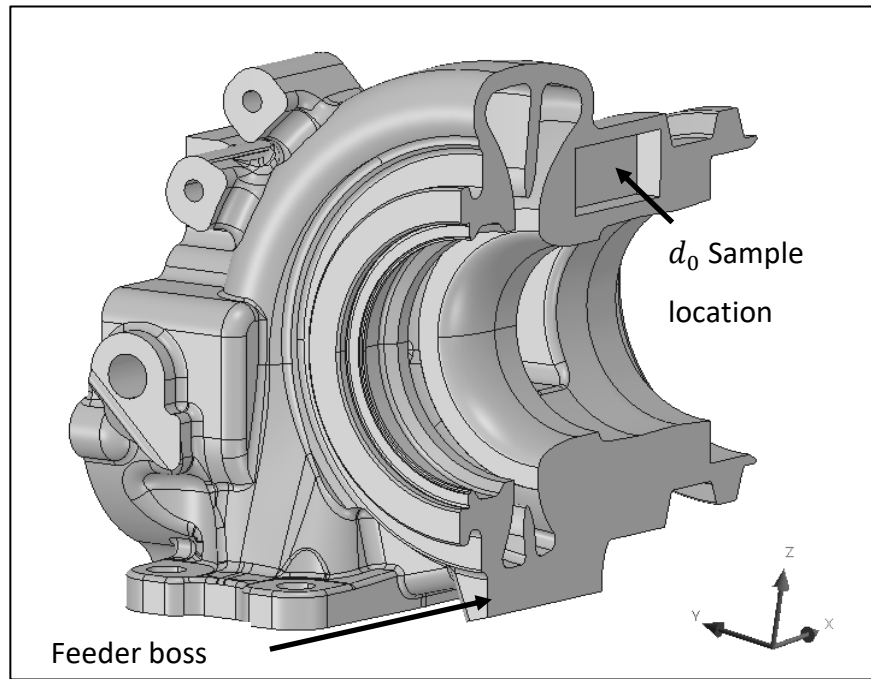


Figure 5.13 Location of stress free reference sample.

Measurements of the d_0 reference were obtained using a gauge volume of 2x2x2mm from the central point of the rectangular plate, as shown in Figure 5.14, well away from the EDM cut surfaces as it was rotated through ω . Measurements were obtained every 5° for a total of ~180°. The scan through ω was carried out to increase the number of diffraction grains sampled and minimise the uncertainty due to random positioning of grains within the gauge volume (Holden et al., 2015), taking the average value of the d_0 cancels out any error introduced as a result of grain size variation. The d_0 measurements were completed during the second phase of the testing, as part of the experiment which considered the turbine housing after it had been subjected to thermal cycling. The wavelength used in the second experiment ~1.61 Å was slightly different to that of the first, which was ~1.726 Å. The stress-free reference was

therefore corrected to consider the $\sim 1.726 \text{ \AA}$ wavelength from the earlier experiment before it was used for strain calculations obtained in the first experiment.

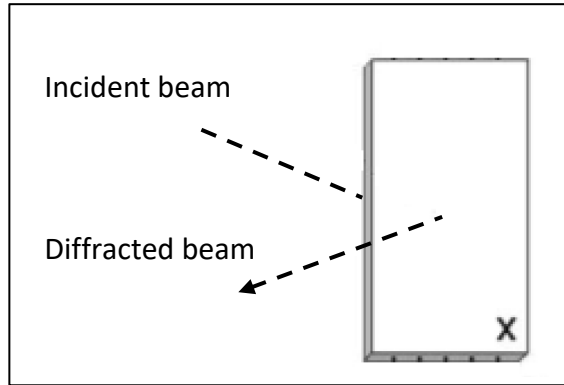


Figure 5.14 Location of d_0 scan on the reference plate, through $180^\circ \omega$

5.3.1.3. Determination of stress

In order to determine the value of residual stress from the measured strain, raw diffraction data obtained for each point on the line scans at each location was manipulated using LAMP data handling software (Richard, Ferrand, & Kearley, 1996). Using LAMP the RAW data was first converted from a 3D data set to 2D using an integrate command to collapse the data vertically and create a peak to fit. Afterwards the peak of each spectra was fitted with a Gaussian function and smoothed with a background filter which was generated using Mathcad. An example of a fitted peak is shown in Figure 5.15 taken from a point scan on the housing, prior to thermal cycle, with the peak position corresponding to $86.77^\circ 2\theta$. Once the 2θ peak position was obtained for each of the points along the line scan and shift in peak position compared to the stress-free d_0 therefore the elastic lattice strain could then be determined using equation 4.3 as detailed in section 4 and subsequently the stress for each component was determined using equations 4.9 and 4.11, with elastic constants of 0.28 for Poisson's ratio and 169GPa for Young's Modulus.

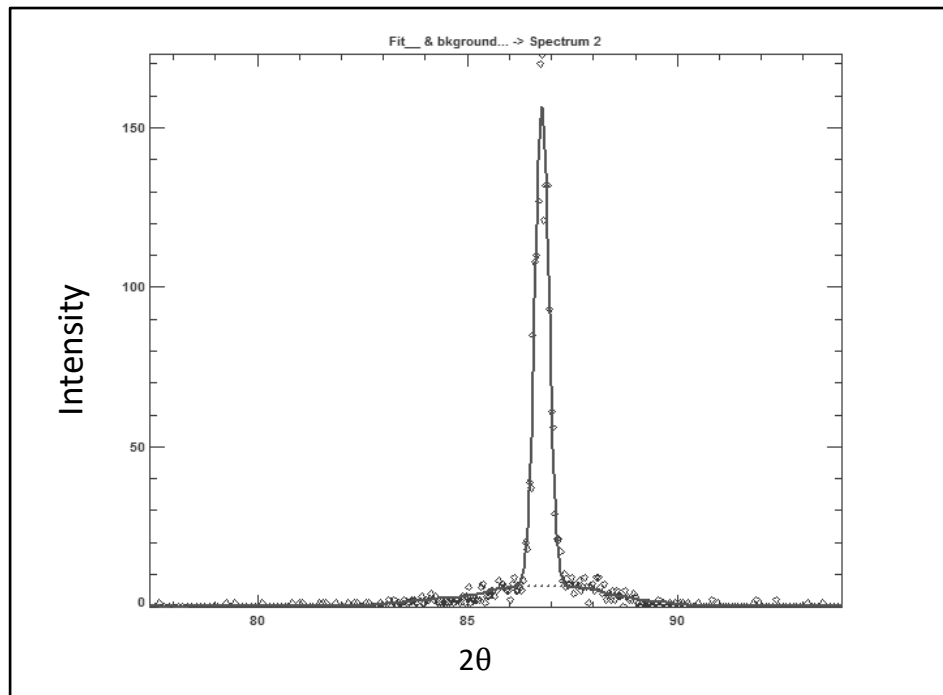


Figure 5.15 Best fit (solid line) to the hkl Bragg peak (open circles). The dotted line shows the background component of the fit.

5.3.2. Contour method procedure

Residual stress measurements using the contour method were performed at the Stress Map facility, which is part of the Open University in the UK (Stress Map, 2018). The turbine housing was one of the most challenging geometries upon which the contour method had been used by the Stress Map group (Oliveira, 2016) and as a result will be presented by a member of the group as an example of the application of the method on a complex geometry at the upcoming Society for Experimental Mechanics conference in June 2019.

The contour method relies on the measurement of the displacement of cut surface contours after stress relief has occurred when the component is sectioned. It is primarily used to obtain a single stress component, which is normal to the cut surface. In the case of the turbine housing, the hoop stress was selected as the direction to be measured, as it offered a large surface area for measurement. The hoop stress is often the largest stress component in cylindrical geometries and therefore potentially the most interesting component to measure.

The contour method procedure was carried out in four main stages:

- i. Cutting stage.
- ii. Measurement of surface contours.
- iii. Data analysis.
- iv. Finite element analysis to determine residual stress.

5.3.2.1. Cutting procedure

To relieve the stress in the hoop direction, the turbine housing was first sectioned radially as shown in Figure 5.16. This was achieved in two separate stages starting from the inner diameter and moving outwards. The cut was made to avoid cutting through the large feeder boss on the bottom of the casting which, due to its thickness, would make it difficult to achieve a clean cut. As a result, the contour measurements are in a slightly different plane compared to the neutron line scans. To ensure a straight (planar) cut with minimal width and a smooth surface, a wire electric discharge machining (WEDM) with a 250 μm diameter uncoated brass wire was used to section the housing with a cut feed rate of 3 mm. min⁻¹. During the cutting stage the housing was securely clamped as shown in Figure 5.16 at both ends on to the WEDM table to ensure minimal deviation from the original cut plane. Both the part and the clamp arrangement were submerged in deionised water and allowed to reach thermal equilibrium before cutting took place, to reduce the risk of introducing thermal stresses.

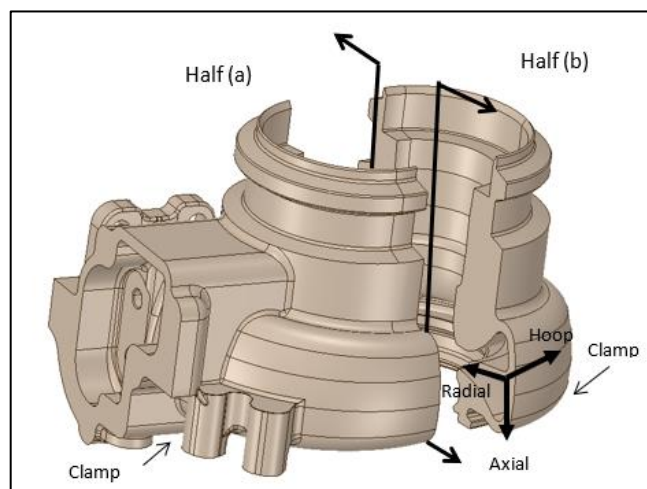


Figure 5.16 Showing the initial WEDM cuts and the three residual stress components in the radial-axial cross section.

5.3.2.2. Surface measurement procedure

The contours of the cut surfaces of both halves of the sectioned housing were then measured with a Zeiss Eclipse coordinate measurement machine (CMM), which was equipped with a TP8 Renishaw touch-trigger probe and a Micro-Epsilon laser triangulation probe. The resolution of the CMM was $\pm 0.5\mu\text{m}$ and limited by the CMM axis. Control of the CMM axes was via computer with Inspect 3D software version 4.00r (Araujo de Oliveira, Fitzpatrick, & Kowal, 2014). Prior to the surface measurements, the touch-probe had been calibrated using a precisely manufactured sphere with a known diameter, to check for any probe shape abnormalities. The laser probe was also calibrated using the same calibration sphere to determine the offset between the separate probe systems. The coordinate system of the CMM was then aligned to match the sample, so that the z axis was aligned perpendicular to the cut face, the x axis was aligned to a straight edge of the cross-section and the y-axis was zeroed at a corner between said straight edge and a second edge. The perimeter of each surface was then measured using a 2mm diameter ruby tip touch-probe with a 0.25 mm point spacing, after which the surfaces were scanned using the laser probes with a $25\mu\text{m}$ spacing in both x and y, resulting in a relatively fine grid of measurement points. During the surface measurement the CMM was protected from thermal fluctuations with the temperature maintained at a constant temperature of $21^{\circ}\text{C} \pm 0.1$.

5.3.2.3. Data Analysis procedure

After the surface measurements were completed on both cut faces, as shown in Figure 5.17, the points measured on half (b) were mirrored about the y-z plane. The perimeter scans were then used to align the measurement points from both halves to a common xy grid which were then averaged point by point. The averaging of the data removes the effects of shear stress and should cancel out the presence of any cutting artefacts which could be introduced.

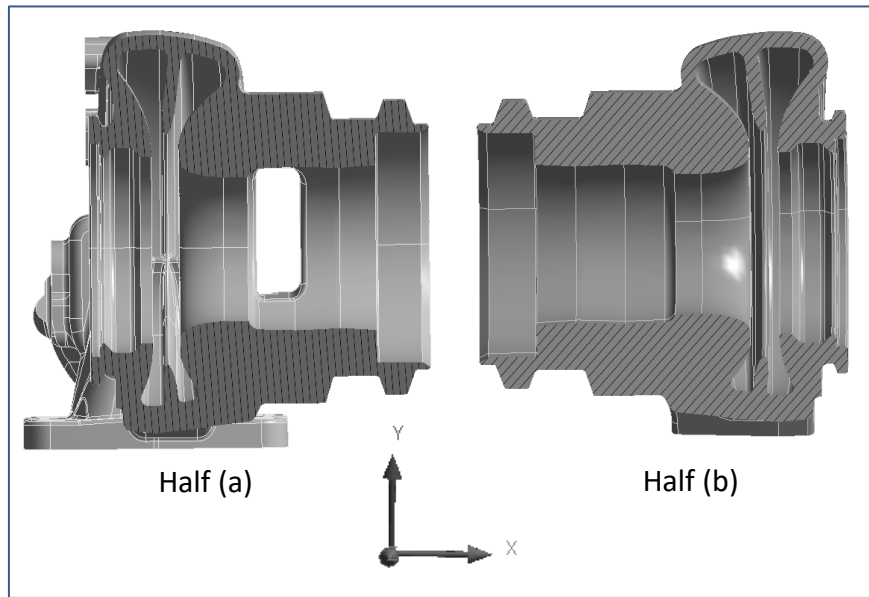


Figure 5.17 Faces on which surface contours were measured after the initial contour cut (cut1) was made.

5.3.2.4. Data smoothing (fitting)

A spline fitting routine was used to fit the measured and averaged displacement data to an appropriate function to enable an accurate representation of the contour surface, while filtering out unnecessary noise (Hosseinzadeh, Kowal, & Bouchard, 2014). The fitting routine was performed using a MATLAB based, 3D cubic spline algorithm to smooth the displacement data by curve fitting. The technique as described by Traore, Paddea, Bouchard, & Gharghour (2012) uses different 'knot' spacings which represent the points at which polynomial pieces are joined together to smooth the measured data, the knot spacing used will dictate the fit of the data. For example, a knot spacing that is too small could inadvertently capture the surface finish of the measured contour surface, while a large knot spacing results in over-smoothing and detrimentally affects the spatial resolution and ability to detect sudden stress gradients. An optimal level of smoothing was then determined following the procedure as detailed by Hosseinzadeh et al. (2014) which involved increasing the refinement of the knot spacing and determining the stress for each increment until a

reduction in the uncertainty in the calculated stress was observed (Prime, Sebring, Edwards, Hughes, & Webster, 2004).

5.3.2.5. Creation of finite element model

A finite element model of half (b) of the turbine housing which was based on the laser perimeters measurement scan of the CMM, was created using Abaqus FE software (Simulia, 2017). First order linear hexahedral (brick) elements, type C3D8R were used and based on the guidance given by Hosseinzadeh et al. (2014) an element mesh size of less than one quarter of the knot spacing was defined. The Abaqus input file was then read into MATLAB, before the negatives of the smoothed displacements were interpolated to the nodal positions. Abaqus software was initially used as this was the software available at the Stress Map facility. The subsequent stress analysis using the same mesh parameters was performed with Ansys mechanical software (ANSYS, 2016) due to the availability of the software to the author. The mesh input file was exported from Abaqus and read into Ansys and a 3D isotropic linear-elastic material model with a Young's modulus of 169GPa and a Poisson's ratio of 0.28 was defined. To determine the stress in the hoop direction, the negative of each of the smoothed displacements was imported into the analysis as an external loading and mapped to the surface nodes of each face as a displacement in a direction normal to the cut surface.

5.3.2.6. Additional cuts

The initial cutting process (cut1) (see Figure 5.18 for details of each of the cuts) was found to introduce horizontal lines referred to as 'cutting artefacts'. These were aligned in the direction of the cutting wire and would produce unrealistically high magnitudes of stress (which will become apparent in the results section 6.3). Attempts to reduce the uncertainty in the measurements were unsuccessful and due to the complexity of the turbine housing geometry several different cuts were ultimately necessary to reduce the potential error in the measurement. The various cuts performed during the contour method are shown in Figure 5.18 and are also compared

to the location of the neutron diffraction residual stress measurement locations. The second cut (cut 2) was performed to reduce the uncertainty in cut 1. Unfortunately, the cut was still not satisfactory, producing unrealistically high levels of stress and a final cut (cut 3) was required with the use of a sacrificial layer before acceptable results were obtained. The process of scanning the contour surface, analysing the data and producing the FE model remained the same for each cut.

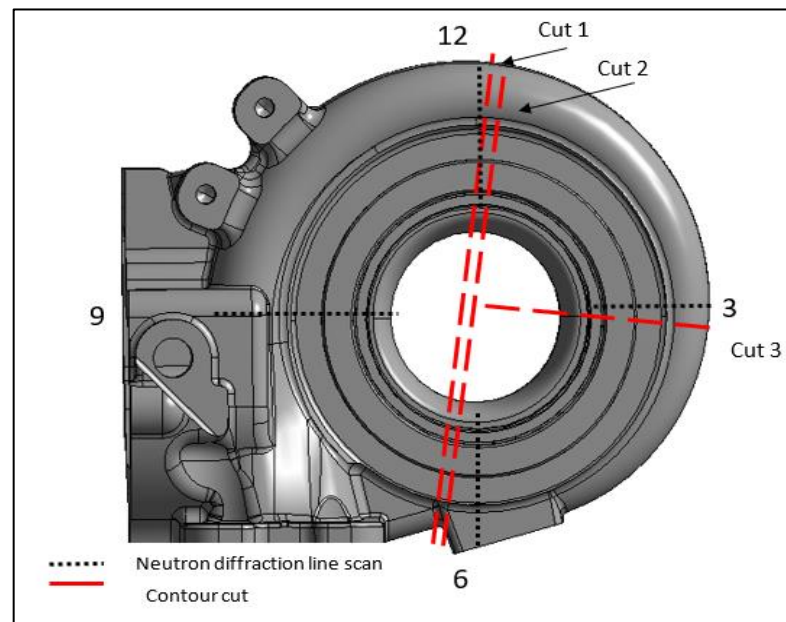


Figure 5.18 Comparison of neutron line scan position and contour cuts, cut 1 initial cut, cut 2 to correct averaged displacement profile from cut 1, cut 3 performed with sacrificial layer.

To attempt to further correct the uncertainty in the first cut a reference cut (cut 2) as shown in Figure 5.18 was performed on the smaller of the two halves, half (b). This involved removing a thin slice from the previously cut surface. It could then be assumed that any out of plane stresses were zero, and any stress that was subsequently measured was due to a cutting artefact. Given that this is now a stress-free cut, any measured displacements will not be related to any residual stress relaxation, but from artefacts created because of the EDM wire contact length. This results in an isolated measurement of the artefact without any residual stress. To then determine the surface displacements related only to residual stress, the surface

displacements of cut 2 (artefacts only) were subtracted from the average displacement of cut 1 (residual stress and artefacts) leaving only the surface displacements due to residual stresses. Smoothing was then performed again, unfortunately the artefacts of the two cuts did not match and would therefore not cancel out and as a result cut 2 could not be fully subtracted from the previous cut.

Finally, half (b) was further sectioned in half (cut 3) and sacrificial layers were introduced to both the interior and around the perimeter of the cut surface. This was performed to keep the wire contact length uniform throughout the cut and therefore reduce the chances of introducing cutting artefacts. The sacrificial layers were first 3D printed as plastic parts to fit inside the gas passage and around the perimeter of the housing to ensure the correct size. The parts were then re-created and cut from metal plate using WEDM. The metal plates were then attached to the interior as shown in Figure 5.19 and to the exterior of half (b) as can be seen in Figure 5.20 using a conductive epoxy glue. To constrain the housing during cutting four holes were drilled and tapped on the previously created cut face to securely clamp the housing onto the flat metal plate. During the CMM measurement stage the sacrificial layers were removed to avoid any interference with the surface measurements.

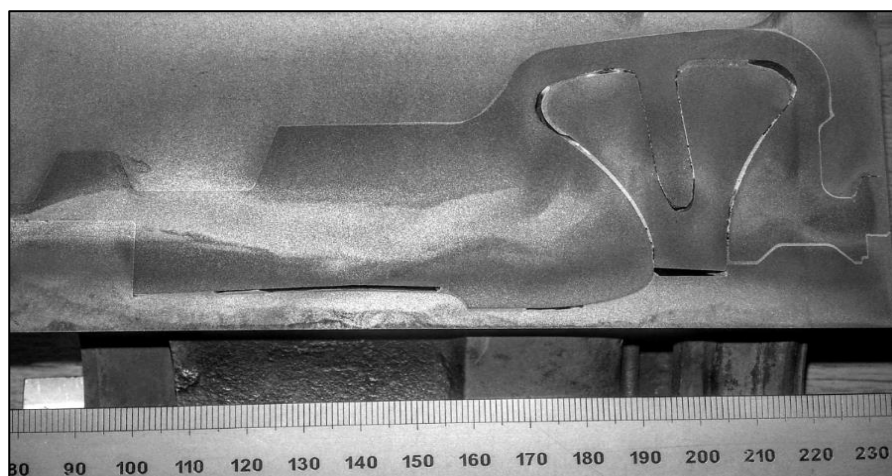


Figure 5.19 Sacrificial layers added to the interior gas passage and around the exterior of the turbine housing



Figure 5.20 Positioning of half (b) with sacrificial layer around the exterior and mounted on to a plate prior to cutting.

Ultimately it is the results of cut 3 that will be compared against simulation models and neutron diffraction measurements. The final FEA model used for the cut 3 surface plane is shown in Figure 5.21. The model was constrained to prevent rigid body motion in two directions perpendicular to the cut surface and meshed with 1mm First order linear hexahedral (brick) elements.

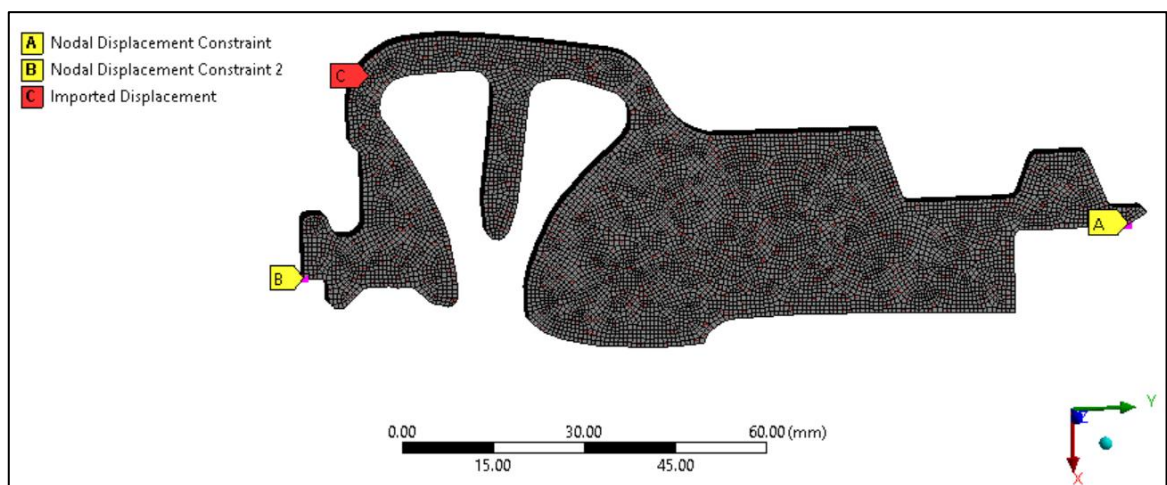


Figure 5.21 FEA model of cut 3 with 1mm mesh size

5.4. Temperature and strain measurements during thermal cycling

To obtain measurements of strain during operation of the turbine housing a thermal cycle test was performed in a test cell, with a set up referred to as a gas stand, located within the BorgWarner UK plant. The use of a gas stand allows for parameters such as mass flow, turbine speeds, pressure ratio, and in this test, temperatures and strain to be monitored which would be difficult, if not impossible in the field. The gas stand test is designed to subject the turbine housing to a standardised thermal cycle which has been designed to replicate a potential worst-case, thermal cycle that the turbine housing could be subjected to in service, to assess its durability. The turbine housing is shown in-situ during the gas stand test in Figure 5.22. During the gas stand test, natural gas is used instead of diesel exhaust gases to drive the turbine and thermally cycle the turbine housing. The hot phase temperature and the mass flow of the 'exhaust gas' is controlled by a burner. During the cooling phase of the thermal cycle the burner is switched off and cold air is pumped into the housing initiating the cooling phase of the thermal cycle.

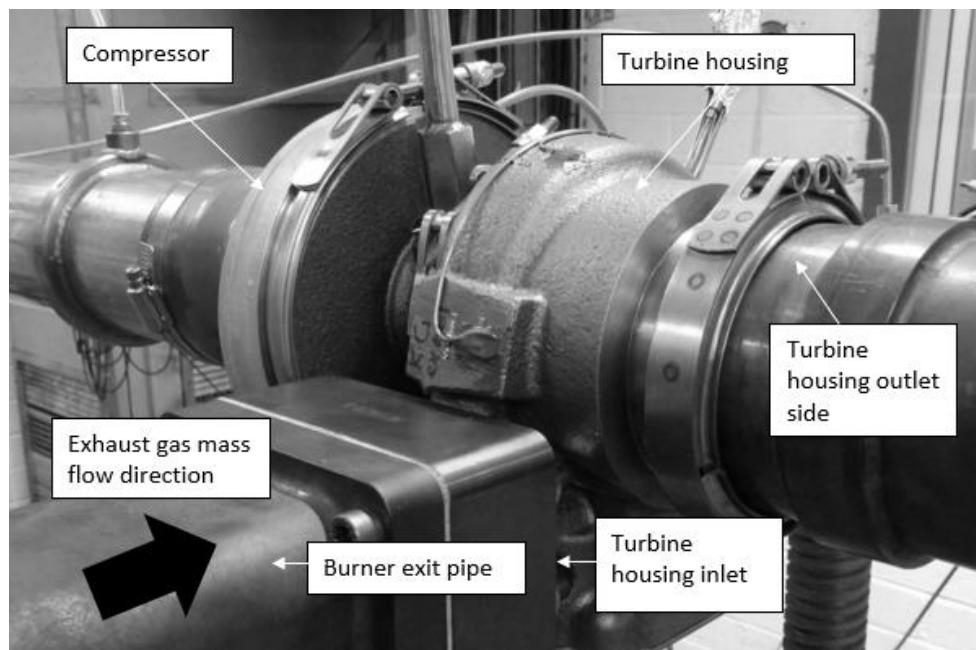


Figure 5.22 The turbocharger assembly in position in the gas stand test cell

5.4.1. Instrumentation

The turbine housing was instrumented with eight type-K thermocouples. Type-K thermocouples were chosen due to their high temperature capabilities well above the 760°C maximum gas temperature that will be considered in this test. Four high temperature strain gauges as shown in Figure 5.23 and detailed in Table 5.3 were also applied. The strain gauges (SG) were carefully attached to the turbine housing by a specialist company, using ceramic cement, within a clean, controlled environment (PCM, 2019). The thermocouples were either spot welded to the surface for surface temperature measurements or drilled and tapped into the surface for subsurface metal temperature measurements.

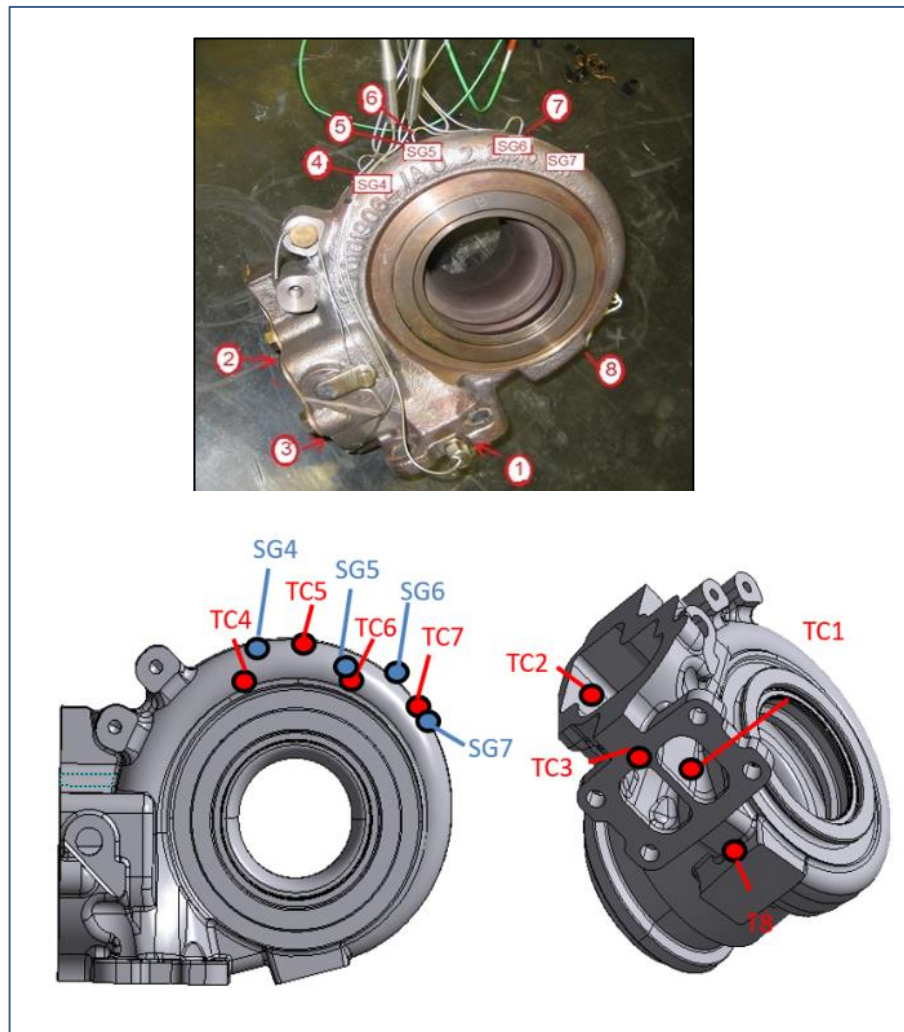


Figure 5.23 Strain gauges (SG 4-7) and thermocouples (1-8) locations

Instrumentation	Description
TC1	Gas temperature within passage at inlet
TC2	Metal temperature 25 mm inside material above inlet flange
TC3	Metal temperature 25 mm inside material toward divider wall near inlet flange
TC4	Metal temperature 25mm inside volute near divider wall
TC5	Metal temperature outside volute surface
TC6	25 mm inside volute near divider wall
TC7	Metal temperature outside volute surface
TC8	Metal temperature outer feeder boss surface
SG4	Axial strain near TC4
SG5	Axial strain near TC5
SG6	Axial strain near TC7
SG7	Hoop strain near TC7

Table 5.3 Strain gauges (SG4-7) and thermocouples (TC1-8) location details

Of the four strain gauges, three were aligned to measure axial strain and one to measure strain in the circumferential hoop direction. The strain gauges were installed in a half bridge arrangement, with each half bridge wired to a bridge completion unit. The half bridge set up consists of two active gauges, one aligned in the direction of the strain to be measured, and the other aligned in the transverse direction and subject to Poisson's strain. The presence of the second gauge corrects for the change in gauge resistance due to temperature. This was covered in detail in Section 4.2. For this installation the following parameters were considered as detailed in Table 5.4. A close-up image of the strain gauges is shown in Figure 5.24.

Excitation Voltage (V)	5
Gauge factor	2.32±1%
Poisson ratio	0.285

Table 5.4 Parameters considered for the strain gauge installation

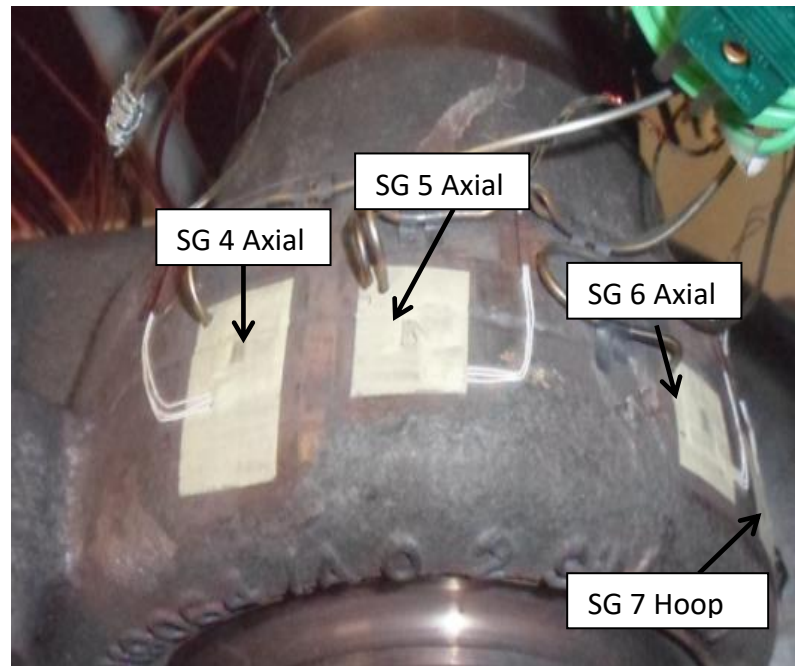


Figure 5.24 Strain gauge locations and measurement directions

5.4.2. Test conditions and thermal cycle definition

A simplified thermal cycle is routinely used to assess the TMF durability of the turbine housing, on a gas stand and aims to replicate the worst-case behaviours that the turbine housing could be subjected to during operation in terms of ramp rates, soak periods and potential altitude usage, as any altitude usage will cause an increase in the temperature of the exhaust gas compared to that at sea level. The cycle consists of alternating hot and cold phases related to the operation of the engine. The hot phase corresponds to a full load operating point, while the cold phase relates to the motoring of the engine (representing a downhill drive), with ramps in between. The cycle used in the gas stand test is commonly used to test an off-road diesel-powered vehicle.

The thermal cycle performed during the test is shown in Figure 5.25 and the test parameters are outlined in Table 5.5. The thermal cycle was repeated 10 times to ensure maximum stabilised temperatures were obtained and both temperature and strain were recorded at a frequency of 1Hz.

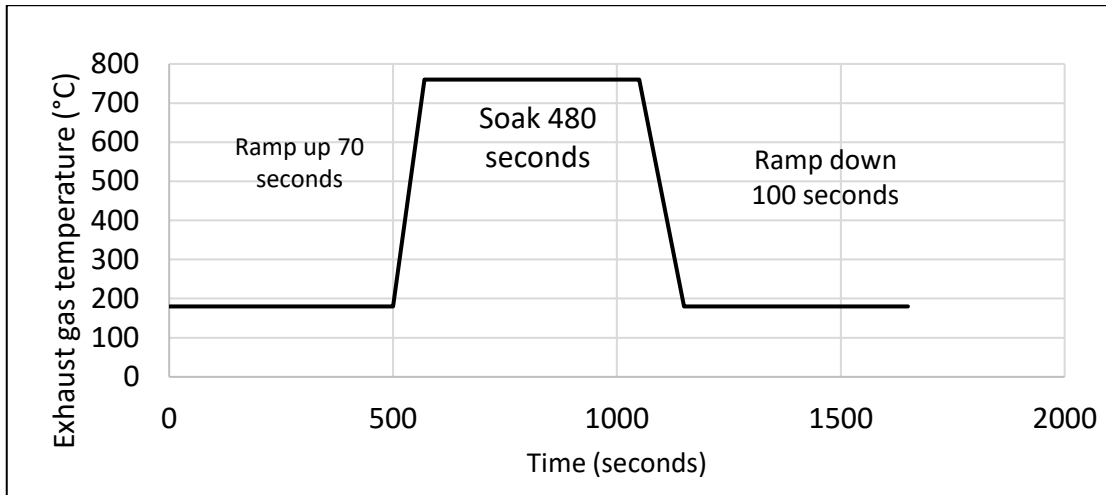


Figure 5.25 Exhaust gas temperature during gas stand thermal cycle test

Hot phase (motoring condition)	
Maximum exhaust gas temperature	760°C
Total mass flow	0.22kg/s
Maximum turbine speed	126.5 krpm
Cold phase (Idle condition)	
Minimum exhaust gas temperature	180°C
Total mass flow	0.26kg/s
Minimum turbine speed	107 krpm

Table 5.5 Thermal cycle control parameters

5.4.2.1. TMF testing of turbine housing for neutron diffraction experiment

The thermal cycle as defined in Figure 5.25 was replicated on the turbine housing which was used to obtain neutron diffraction residual stress measurements before and after thermal cycling, once again 10 cycles were performed to ensure temperature stabilisation was achieved. Dimensions of the housing were recorded prior the test, and the same measurements were repeated after for comparison.

5.4.2.2. Durability testing

An additional turbine housing was subjected to an extended durability test. The test ran for a total of 1200 cycles following the cycle definition defined in Figure 5.25. The test was stopped at intervals of 300 cycles to check for cracks initiation and propagation.

6. Experimental results

This chapter will first provide the results of the initial checks that were undertaken to ensure chemical, mechanical and dimensional measurements met the requirement of the SiMo5.1 material and design requirements of the drawing. The results of the residual stress measurements using neutron diffraction and the contour method will then be presented. This is followed by the results of the high temperature strain gauge and thermocouple measurements obtained during thermal cycling of the housing. The results of the extended durability test will then be given, in which fatigue crack initiation and propagation was monitored at 300 cycle intervals over a 1200 cycle test.

6.1. Material and dimensional checks

During the casting process a housing from the batch of castings was tested both mechanically and chemically to ensure it met the requirements for the SiMo 5.1 specification. The results of the mechanical test are shown in Table 6.1. The material was found to meet the requirements of the SiMo5.1 material specification.

Property	Tensile strength (MPa)	Yield strength 0.2% (MPa)	Elongation (%)	Hardness (HBW 10/3000)
Specification At 22°C	Min. 550	Min. 480	Min 5	210 - 250
Actual (at ambient) Wt. %	643	527	7	225

Table 6.1 Mechanical test results

A test specimen was obtained from the housing and a chemical analysis was undertaken using a spectrometer. The results of the chemical analysis are shown in Table 6.2, and were found to be within the specification for the material.

Element	Specification (Wt.%)	Actual (Wt.%)
Fe	Balance	91.4
C	2.8-3.6	3.23
Si	4-5	3.91
Mo	0.8-1.2	0.774
P	Max 0.01	0.025
Mg	0.03 to 0.06	0.036
Al	n/a	0.1
Mn	n/a	0.072
Ni	n/a	0.147
S	n/a	0.008
Cu	n/a	0.016
Cr	n/a	0.133
Co	n/a	0.001
Sn	n/a	0.002

Table 6.2 Chemical analysis of the turbine housing material.

A dimensional check of the housing prior to the application of the thermal cycle was undertaken, this involved checking 32 measurements against drawing requirements. The housing was found to meet the drawing limits, the dimensional check was repeated after thermal cycling and resulting measurements are detailed in Appendix 1.

6.2. Neutron diffraction residual stress measurements

As previously detailed in the experimental methodology section the measurement of the stress-free lattice parameter d_0 was obtained using a rotation method to improve grain size statistics. Measurements were obtained from the EDM cut sample as it was rotated through ω at intervals of 5° . The lattice parameter for the BCC SiMo 5.1 material was then determined from equation 4.4 for each of the 30 measurement

points. The variation of the lattice parameter a , through rotation in ω is shown in Figure 6.1. Due to path length limitations points between 60- 80 ° were not obtained.

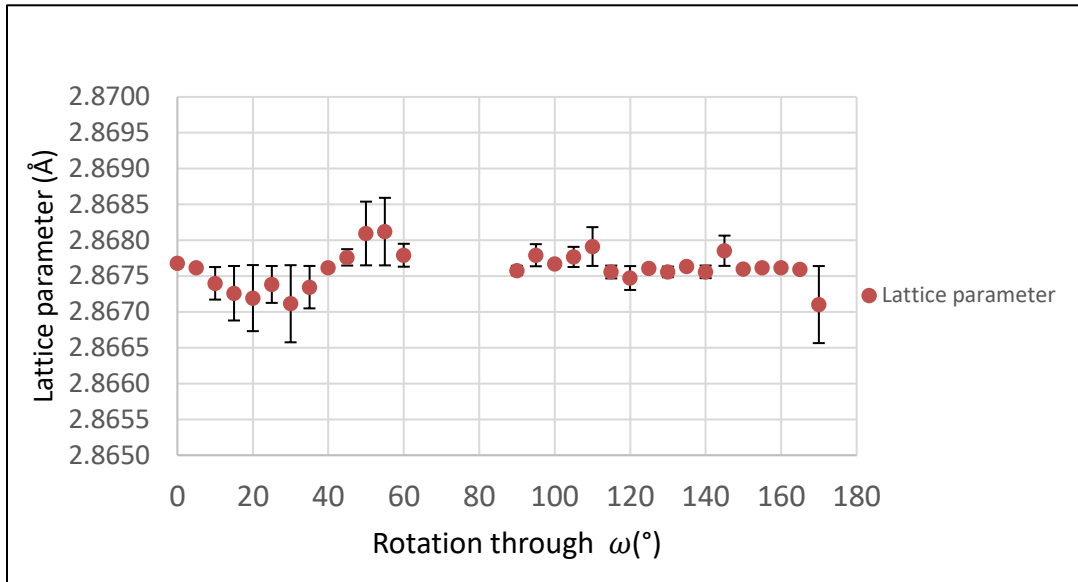


Figure 6.1 Variation in the d_0 lattice parameter through rotation in ω about the centre of the plate.

The average value was determined as 2.86759 Å which was subsequently used as the stress-free reference for all directions and locations in the measurements on the turbine housing.

The next sections detail the neutron diffraction measurements obtained before and after thermal cycling of the housing. For clarity the neutron diffraction measurement locations are once again shown through Figure 6.2 to 6.4.

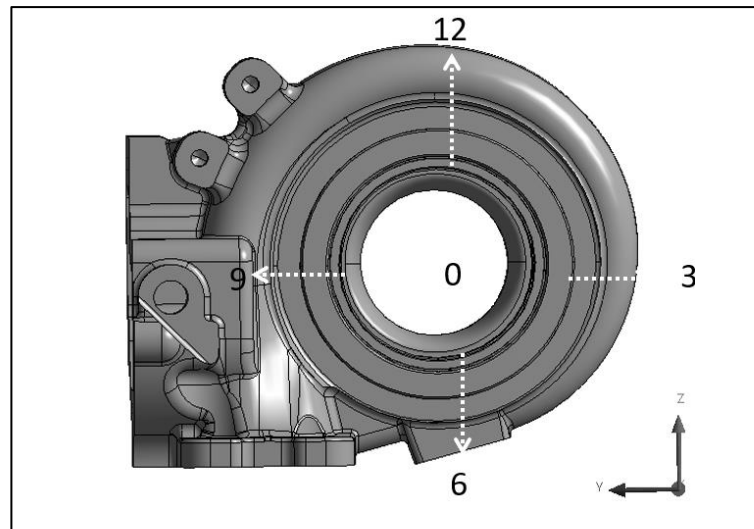


Figure 6.2 Line scans locations corresponding to 3,6,9 and 12 at which measurements were obtained through the divider wall using neutron diffraction

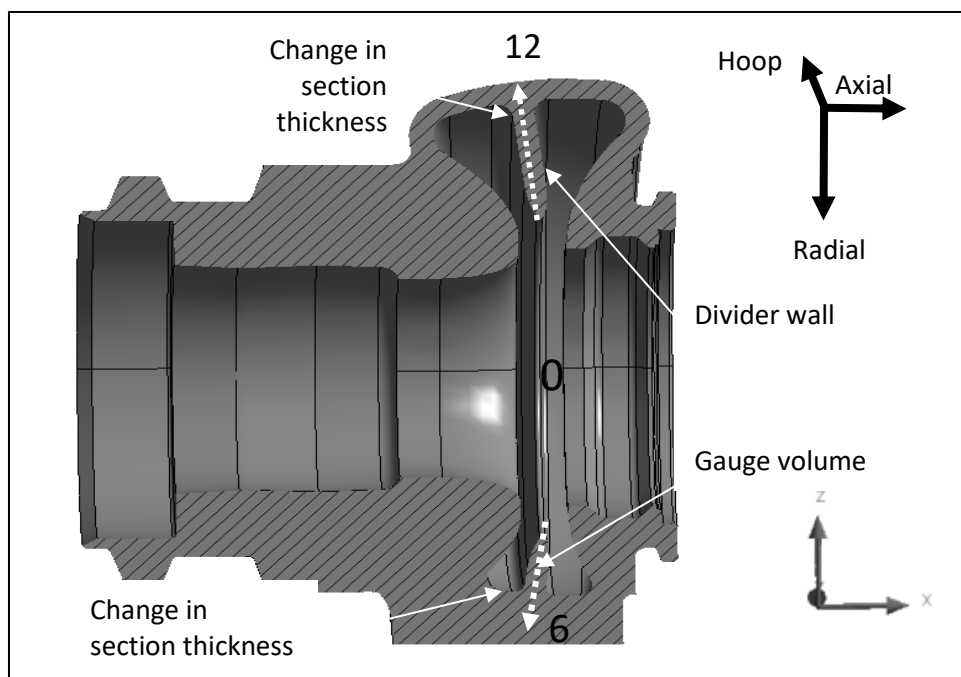


Figure 6.3 Neutron diffraction line scan locations corresponding to locations 12 and 6.

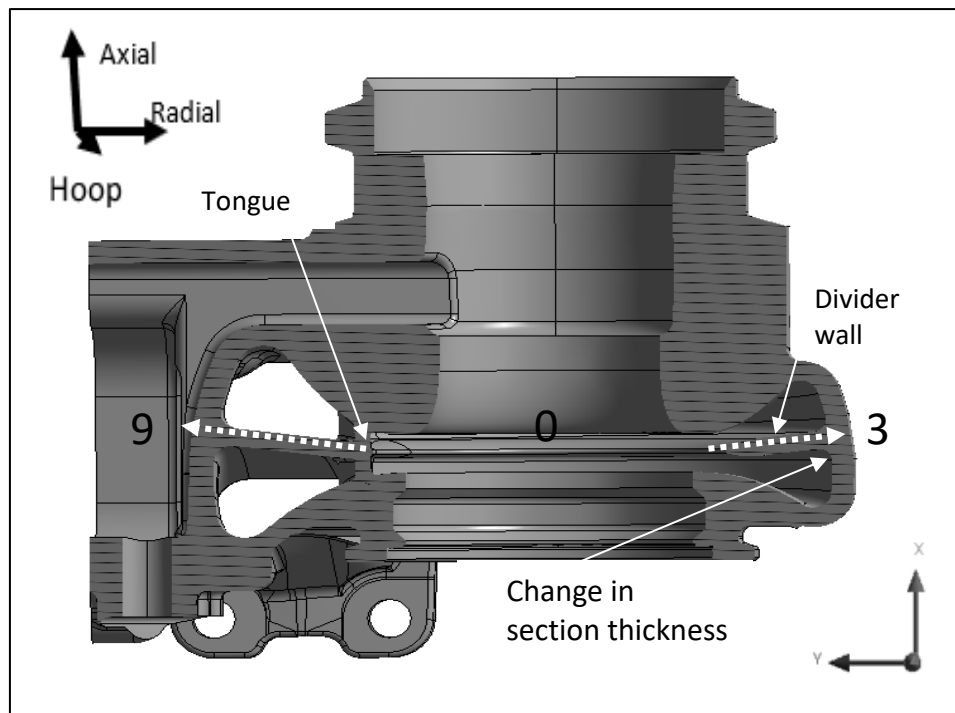


Figure 6.4 Neutron diffraction line scan locations corresponding to locations 3 and 9.

6.2.1. Before thermal cycling

The axial, radial and hoop residual stress results as measured across each of the line scans at positions 3,6,9 and 12 are shown in Figure 6.5 to Figure 6.8. For reference the position of the outer wall of the turbine housing and the point at which a change in section thickness occurs as detailed in Figure 6.3 and Figure 6.4, are shown in order to understand how the residual stress distribution corresponds with the geometry of the turbine housing. The uncertainty in the measurement is associated with the fitting errors to the spectrum for the turbine housing and d_0 measurements.

The variation in stress across each of the measurement positions shows a similar trend, with residual stresses at the edge of the divider wall predominantly compressive or low tensile in nature. The largest compressive stress was observed at position 6 at $-137\text{MPa} \pm 17\text{MPa}$ in the hoop direction. The stress profiles then show a trend towards increasing tensile behaviour as the change in section thickness is approached.

Maximum tensile stress was measured in position 9 at $192\text{MPa} \pm 10\text{MPa}$. As the line

scan progresses towards the outer limit of the turbine housing wall, the stress distribution reverts towards a low magnitude tensile/ compressive state.

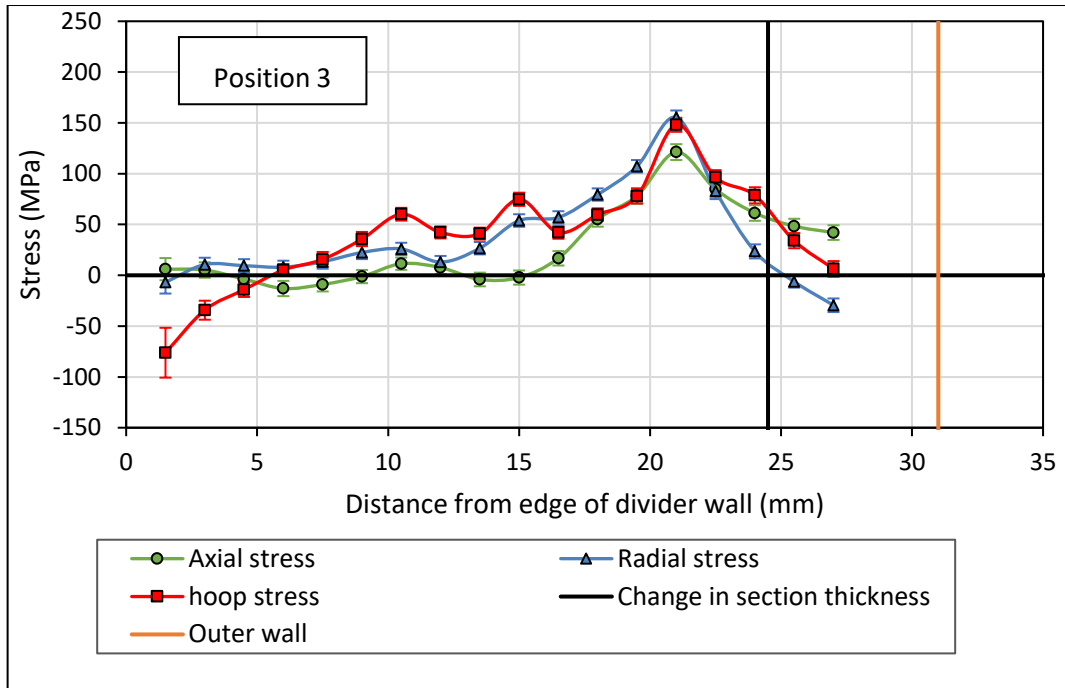


Figure 6.5 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 3 prior to thermal cycling.

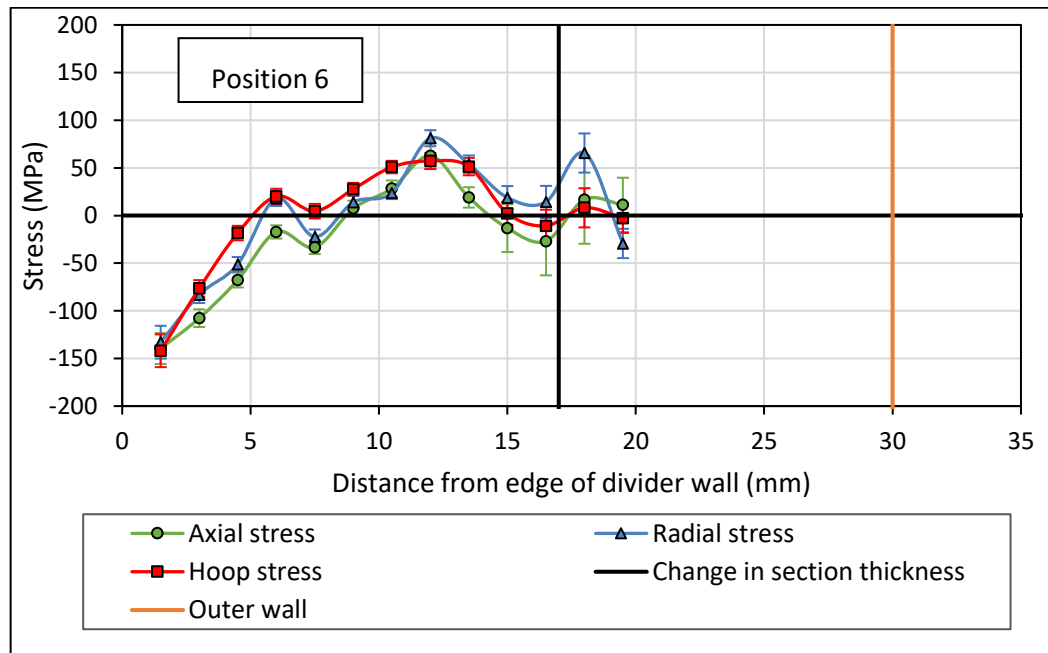


Figure 6.6 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 6 prior to thermal cycling.

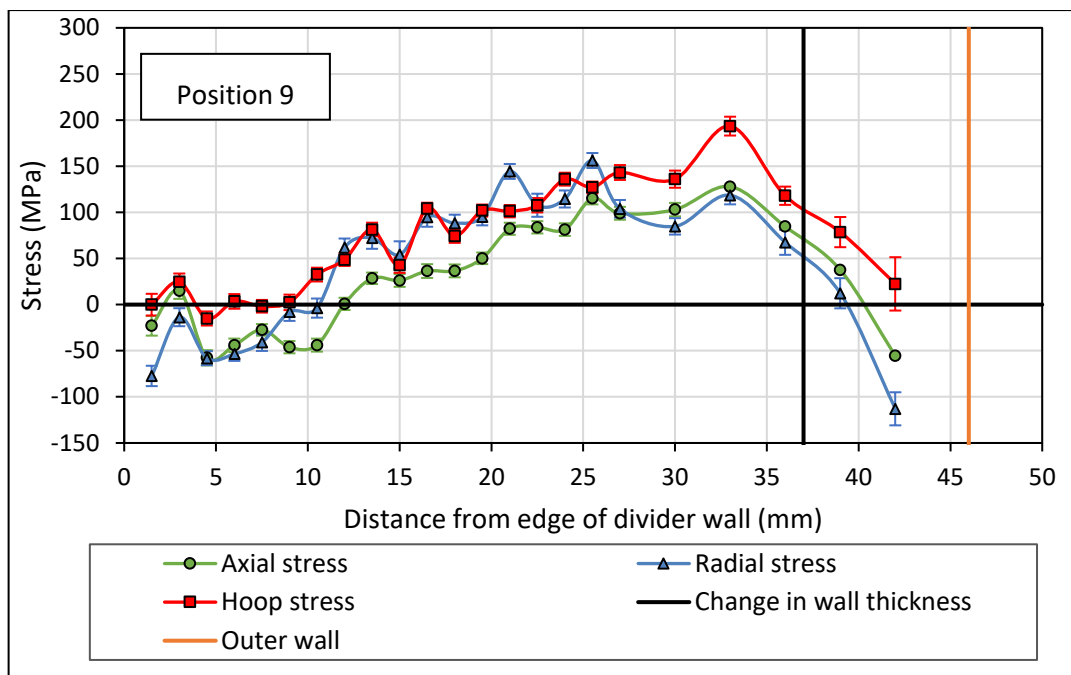


Figure 6.7 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 9 prior to thermal cycling.

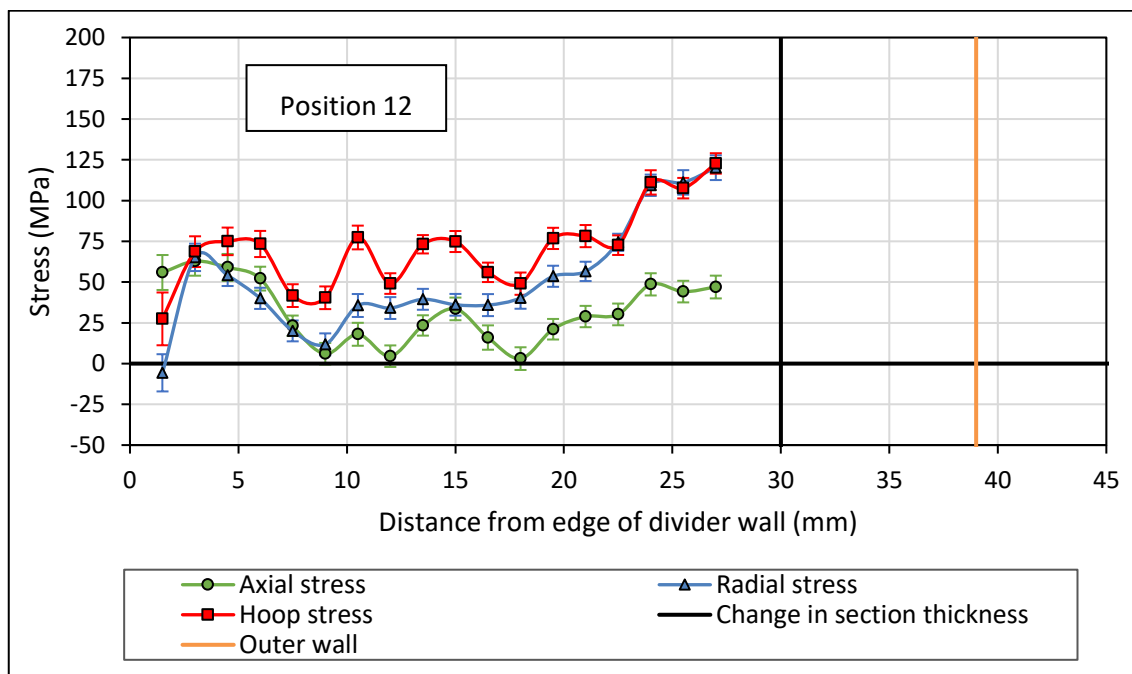


Figure 6.8 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 12 prior to thermal cycling.

6.2.2. After thermal cycling

The results of the axial, radial and hoop stress across the line scans at each measurement position are shown in Figure 6.9 to Figure 6.12. The trend across each of the measured stress components is similar and the variation with each position shows a tendency towards compression at the start of the divider wall, with a move towards tensile behaviour and then once again a reduction towards low magnitude tensile and compressive stresses after the change in section thickness. Each of the measurement positions exhibited reasonably large hoop stresses in the region of 100-150MPa at the beginning of the measurement line. Large tensile stresses in excess of 250MPa were measured in the hoop direction at position 9 towards the change in section thickness.

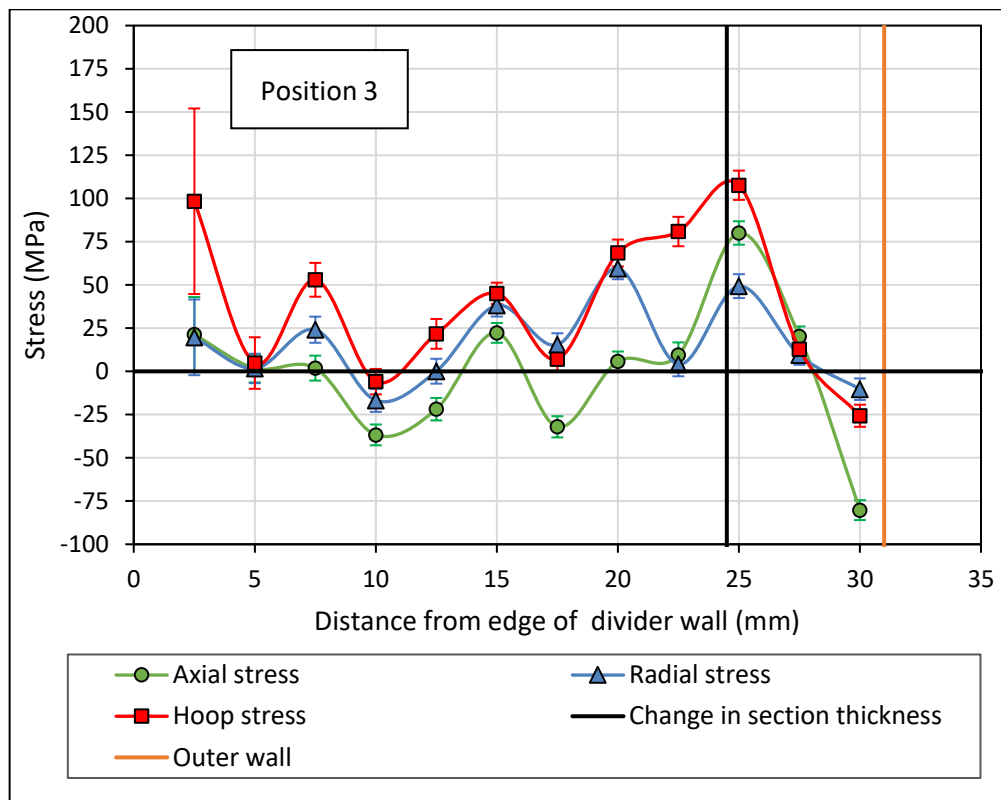


Figure 6.9 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 3 after thermal cycling.

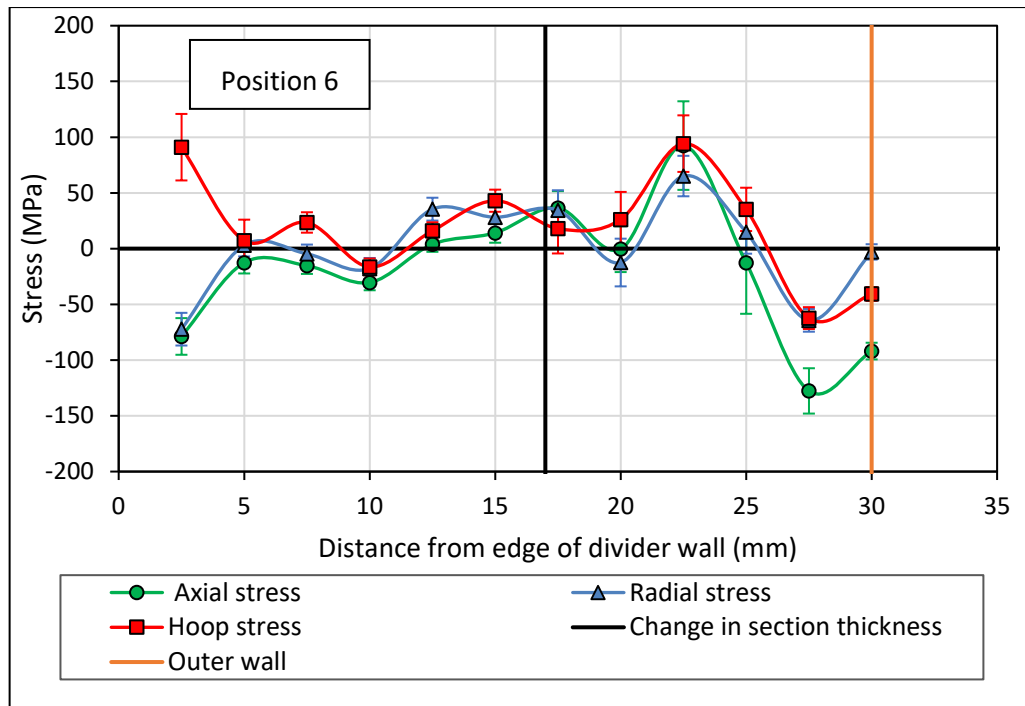


Figure 6.10 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 6 after thermal cycling.

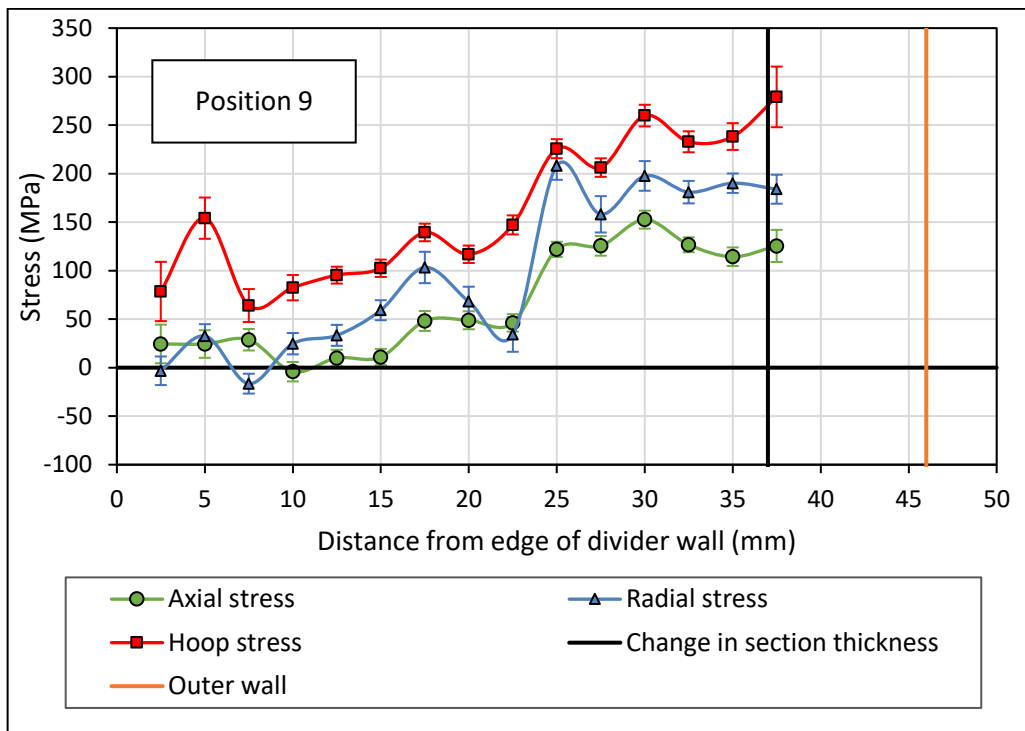


Figure 6.11 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 9 after thermal cycling.

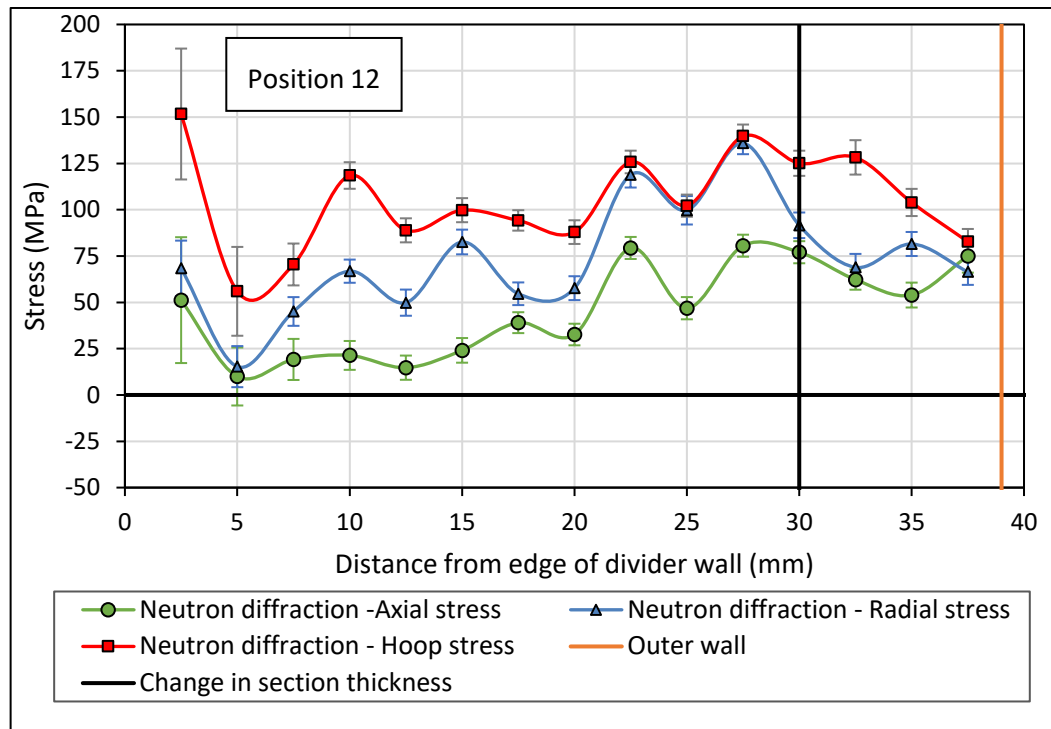


Figure 6.12 Variation in axial, radial and hoop stress along the neutron diffraction measurement line in position 12 after thermal cycling.

6.2.3. Comparison - before and after thermal cycling

A comparison for each of the measurement before and after thermal cycling is given in Figure 6.13 to 6.24.

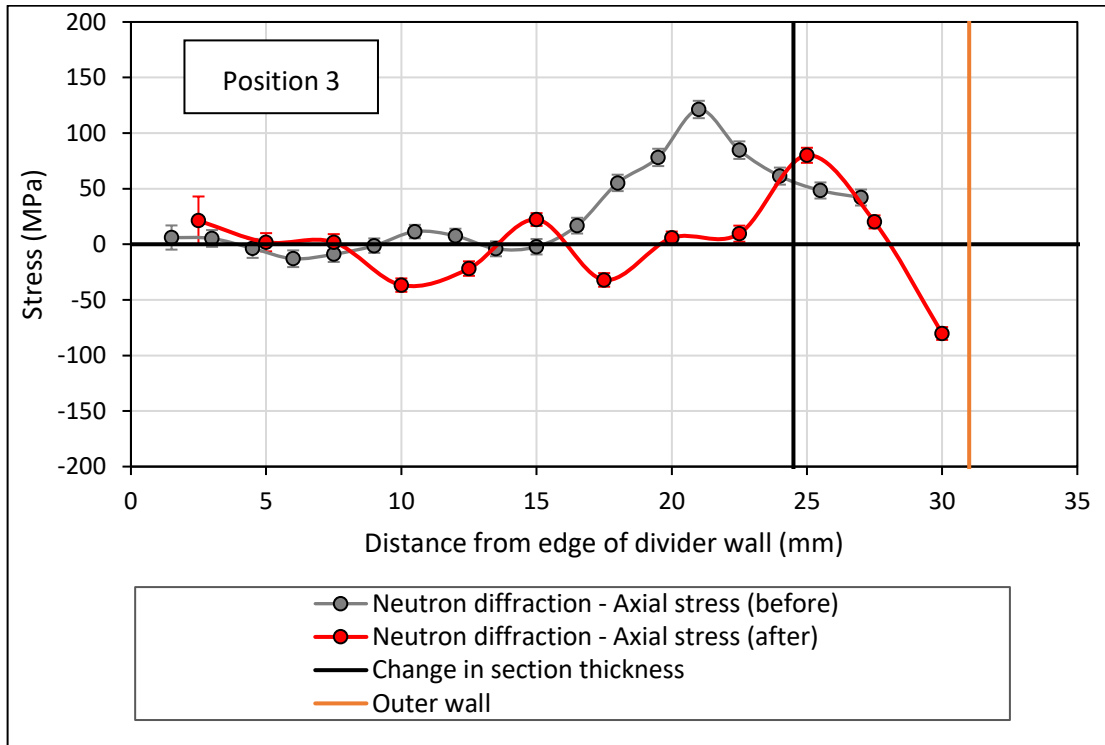


Figure 6.13 Comparison of axial stress neutron diffraction measurements at position 3, before and after thermal cycling.

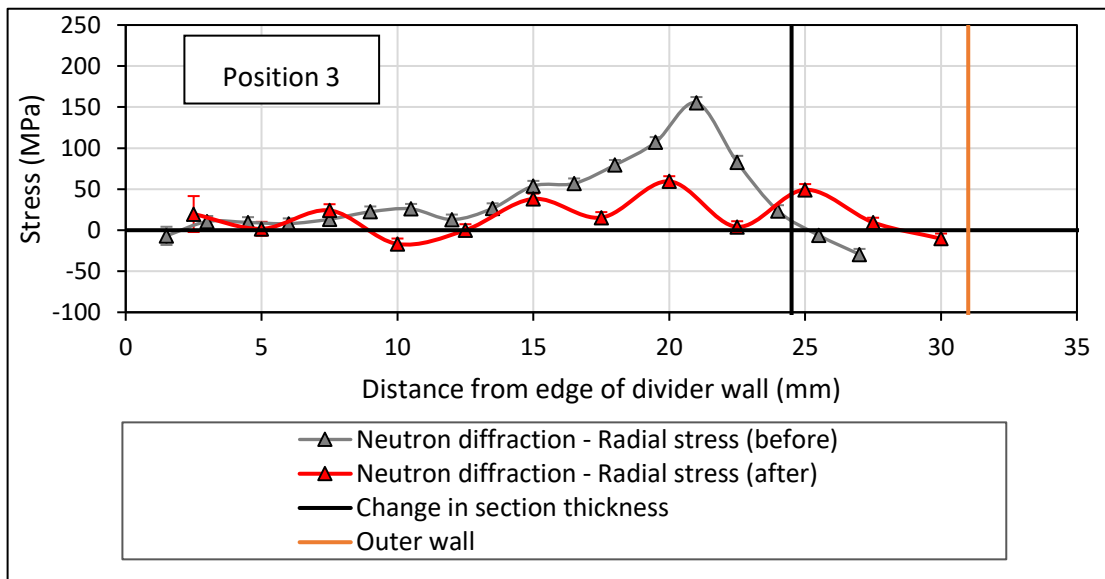


Figure 6.14 Comparison of radial stress neutron diffraction measurements at position 3, before and after thermal cycling.

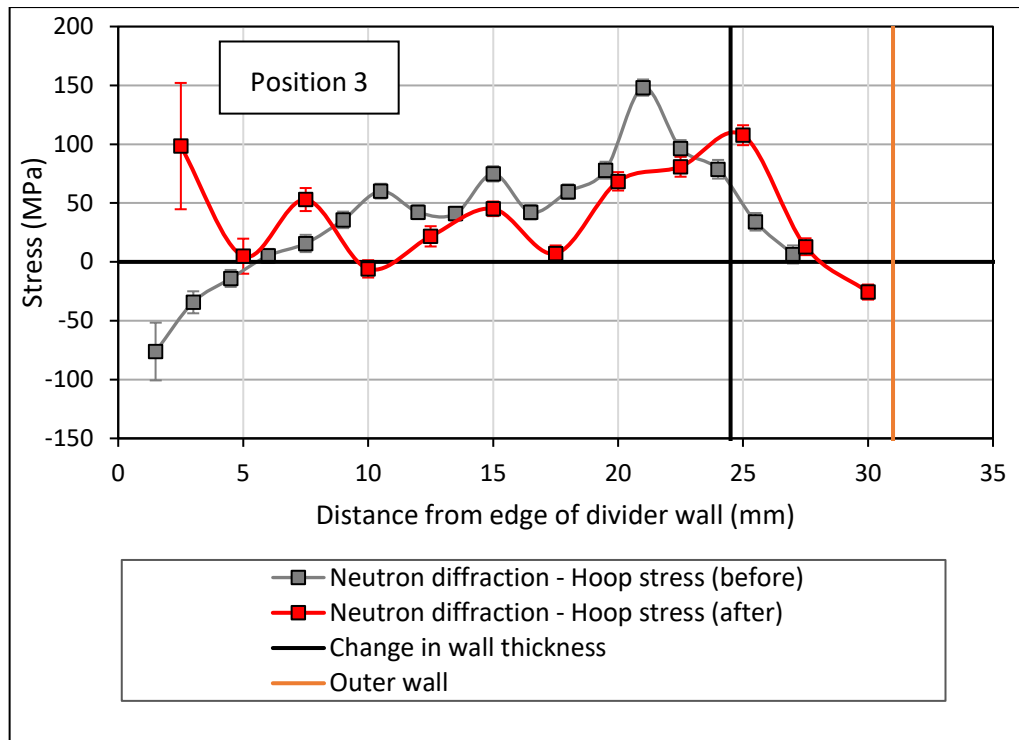


Figure 6.15 Comparison of hoop stress neutron diffraction measurements at position 3, before and after thermal cycling.

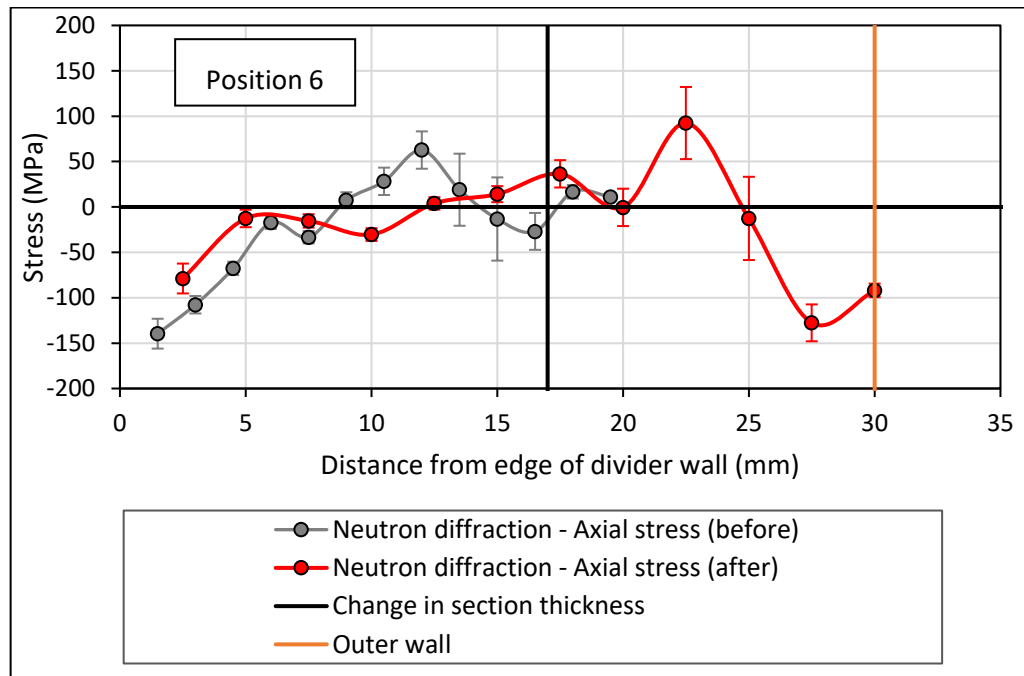


Figure 6.16 Comparison of axial stress neutron diffraction measurements at position 6, before and after thermal cycling.

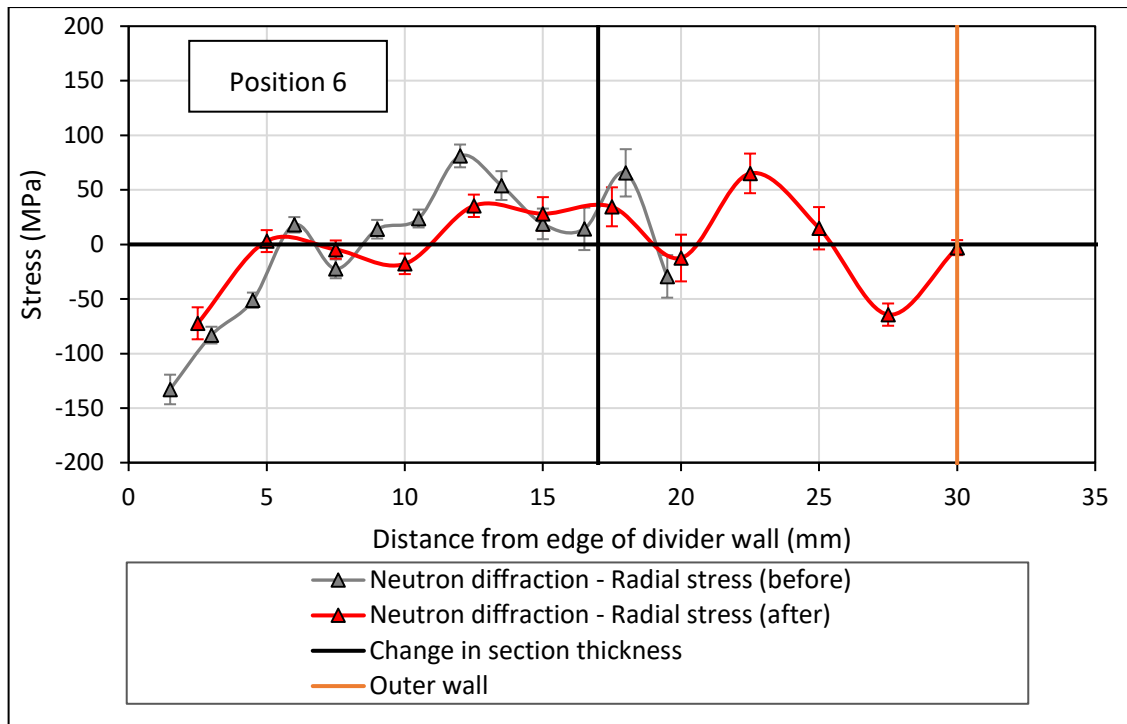


Figure 6.17 Comparison of radial stress neutron diffraction measurements at position 6, before and after thermal cycling.

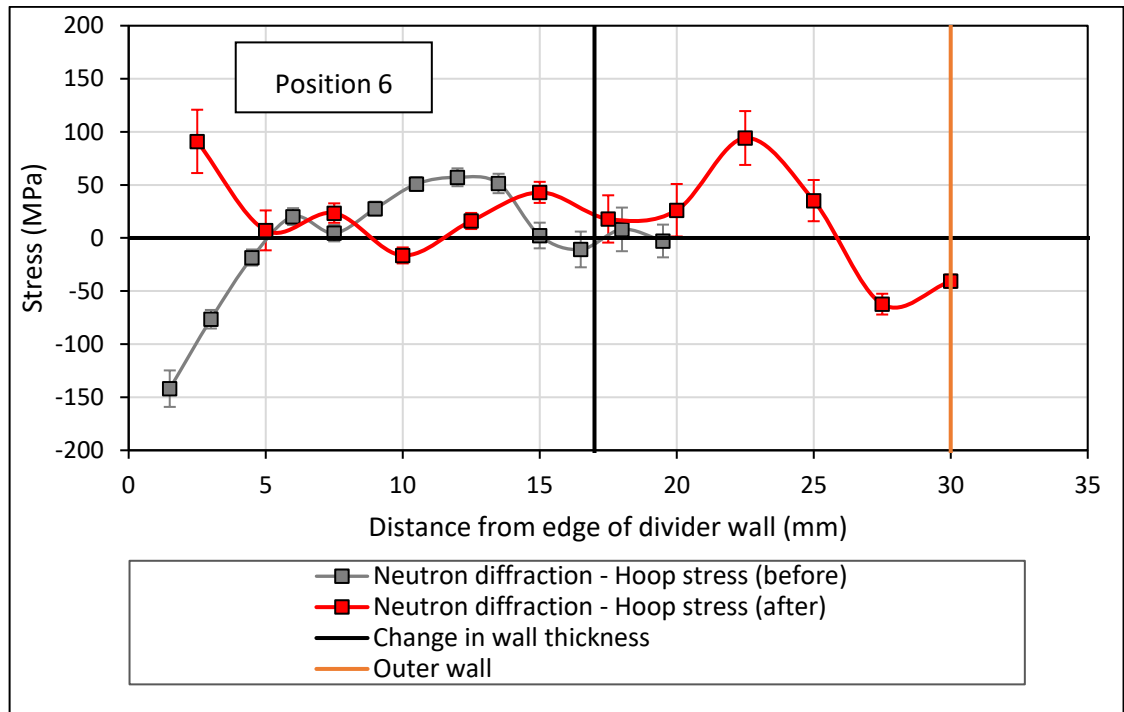


Figure 6.18 Comparison of Hoop stress neutron diffraction measurements at position 6, before and after thermal cycling.

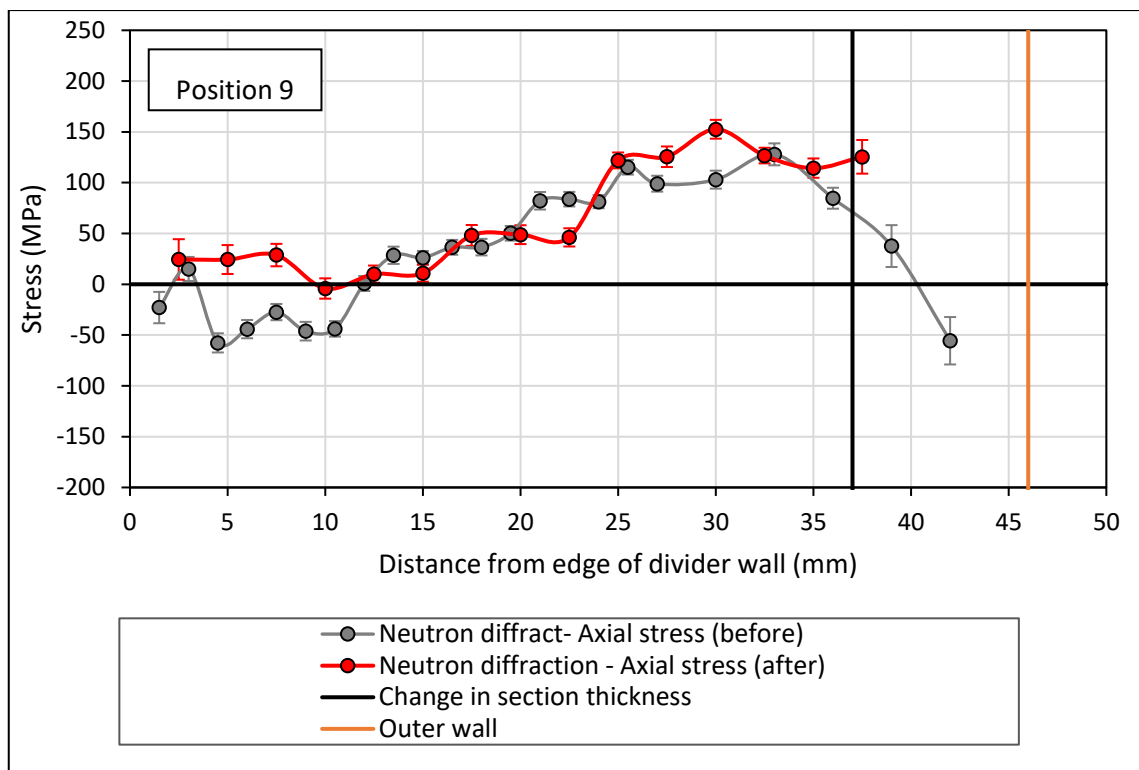


Figure 6.19 Comparison of axial stress neutron diffraction measurements at position 9, before and after thermal cycling.

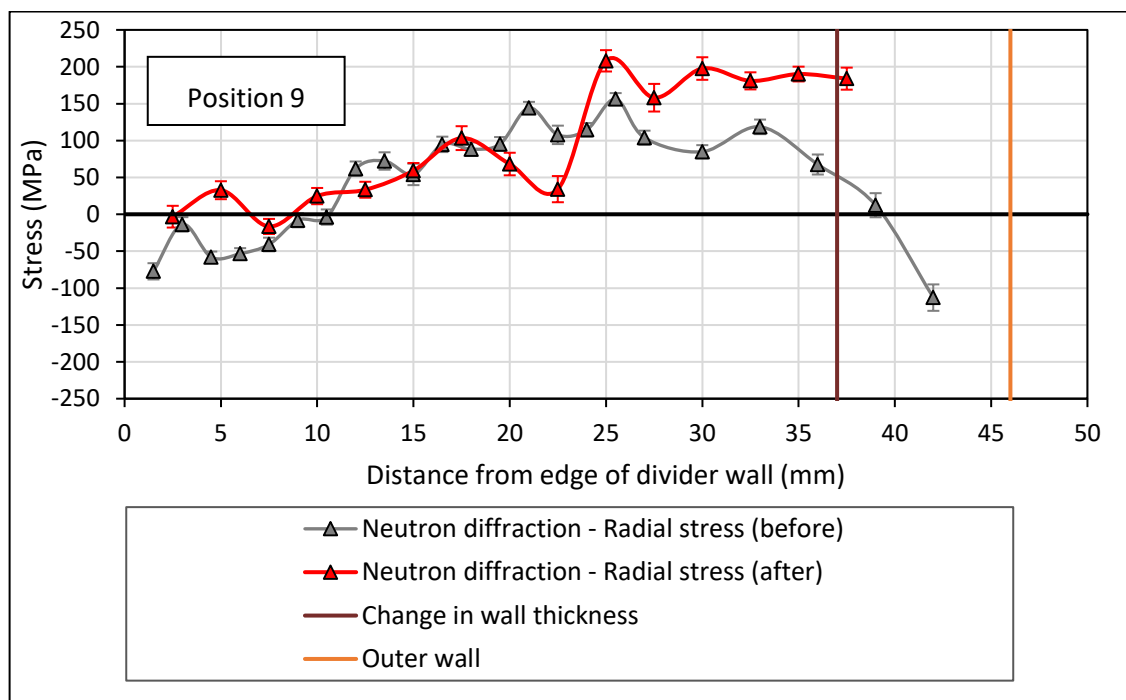


Figure 6.20 Comparison of radial stress neutron diffraction measurements at position 9, before and after thermal cycling.

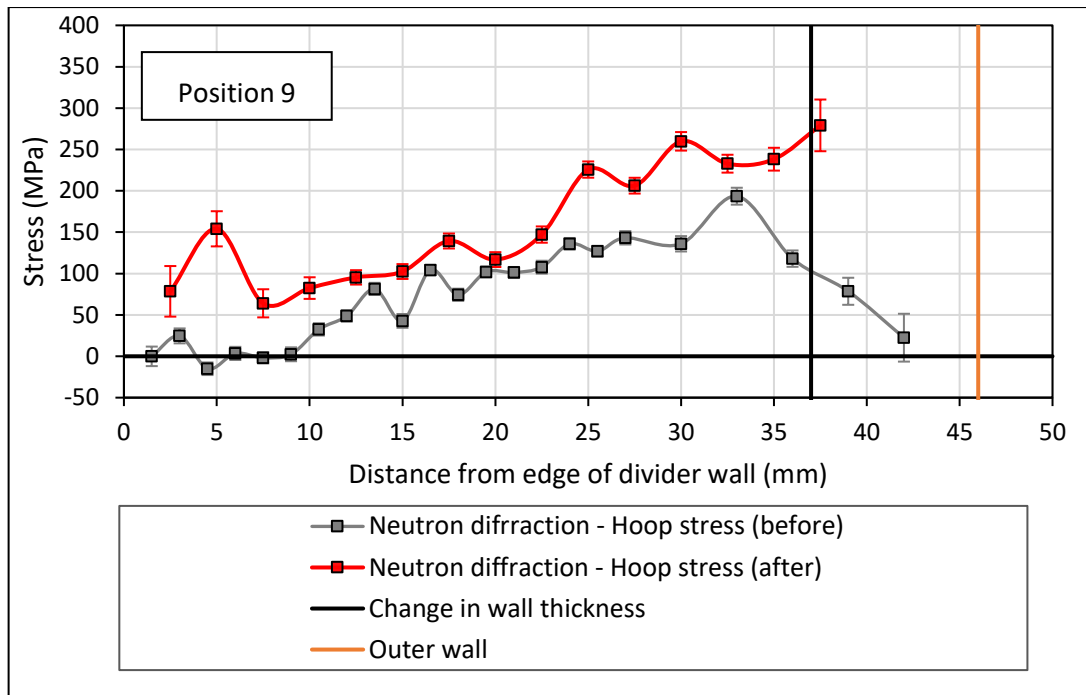


Figure 6.21 Comparison of hoop stress neutron diffraction measurements at position 9, before and after thermal cycling.

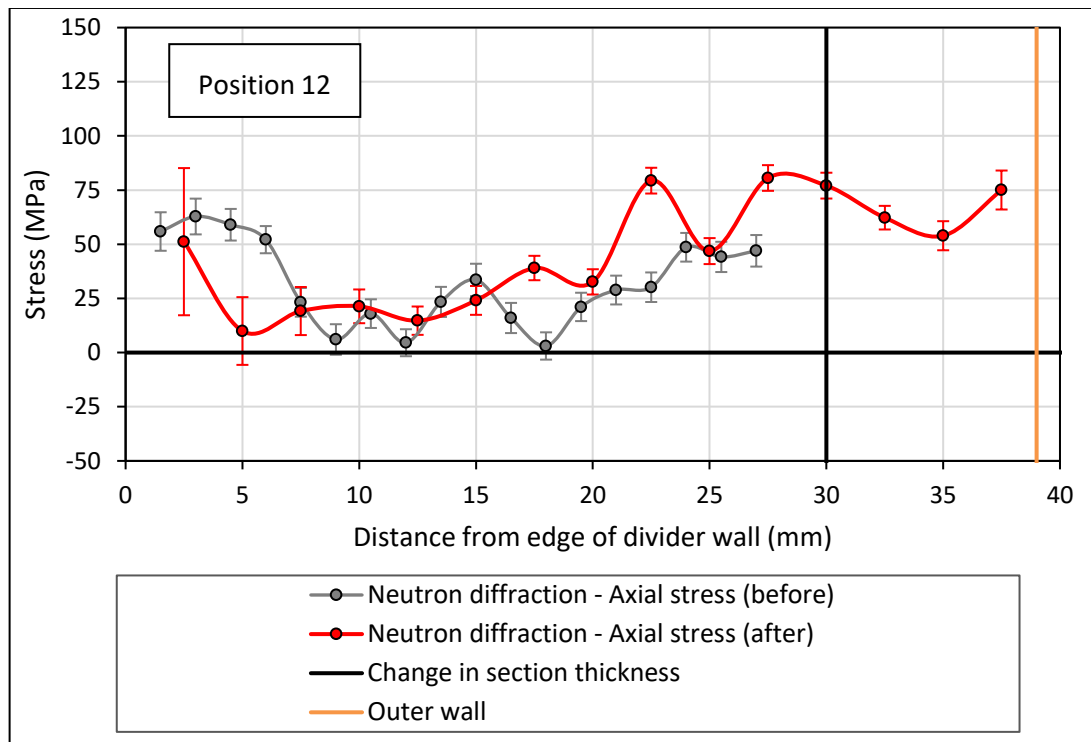


Figure 6.22 Comparison of axial stress neutron diffraction measurements at position 12, before and after thermal cycling.

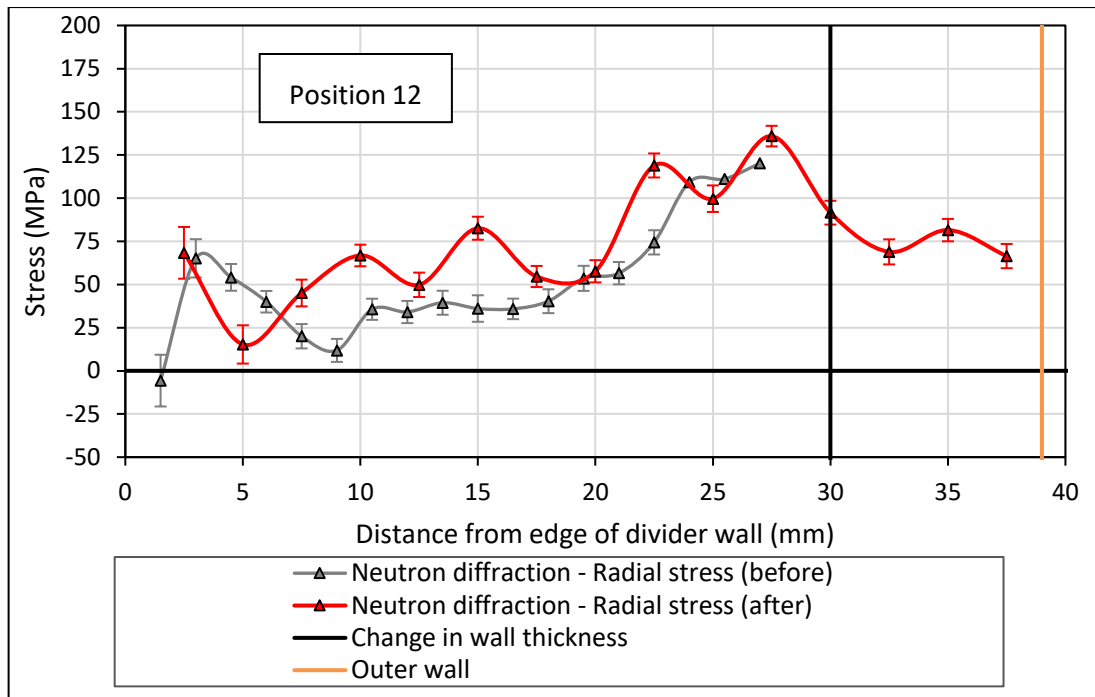


Figure 6.23 Comparison of radial stress neutron diffraction measurements at position 12, before and after thermal cycling.

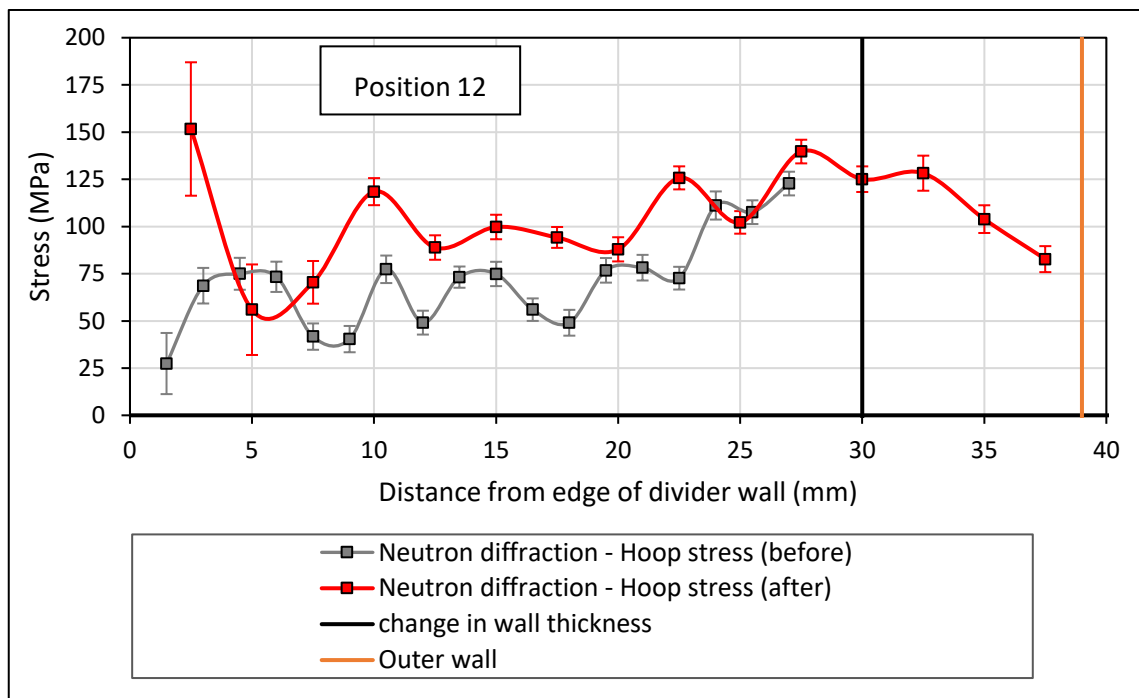


Figure 6.24 Comparison of hoop stress neutron diffraction measurements at position 12, before and after thermal cycling.

To summarise the results:

Position 3: After thermal cycling the axial stress component experiences largely a reduction in tensile stress of up to 120MPa. A slight increase in axial stress was evident at the start of the line scan and as the section thickness change occurs in the geometry with an increase of ~ 25 MPa. A similar trend was observed in the radial line scan, with a relaxation of ~ 100 MPa taking place just prior to the change in wall thickness, followed by an increase in tensile stress of ~ 50 MPa. The hoop stress results demonstrate an increase in tensile stress at the start of the line scan of ~ 135 MPa.

Position 6: The axial and radial stresses show a similar trend after thermal cycling, with reductions of up ~ 40 MPa in tensile stresses observed. An increase in axial stress is evident at the point of changing section thickness. The hoop stress shows a large increase in tensile stress from -76MPa to 91MPa at the start of the line scan, and a smaller shift towards increasing tensile behaviour as the change in thickness of the section occurs.

Position 9: All the measured stress directions experience an increase in tensile behaviour after thermal cycling, most evident at the start and point of changing section thickness of the line scan. The hoop stress shows a large increase in tensile stress from 100MPa to 275MPa.

Position 12: All the measured stress directions show a trend for increasing tensile stress throughout the line scan especially near the start. The hoop stress showed an increase of 70MPa at the edge of divider wall.

Figure 6.25 to Figure 6.28 detail the von-Mises stress distribution across each of the measurement lines, both before and after thermal cycling. The calculation of the von-Mises stress assumes that the strains measured via neutron diffraction are principal strains. A variation in the magnitude was observed, suggesting a degree of relaxation at some of the measurement points, and strain hardening at others. Positions 9 and 12 experience large increases in tensile stress with an increase up to 100MPa observed.

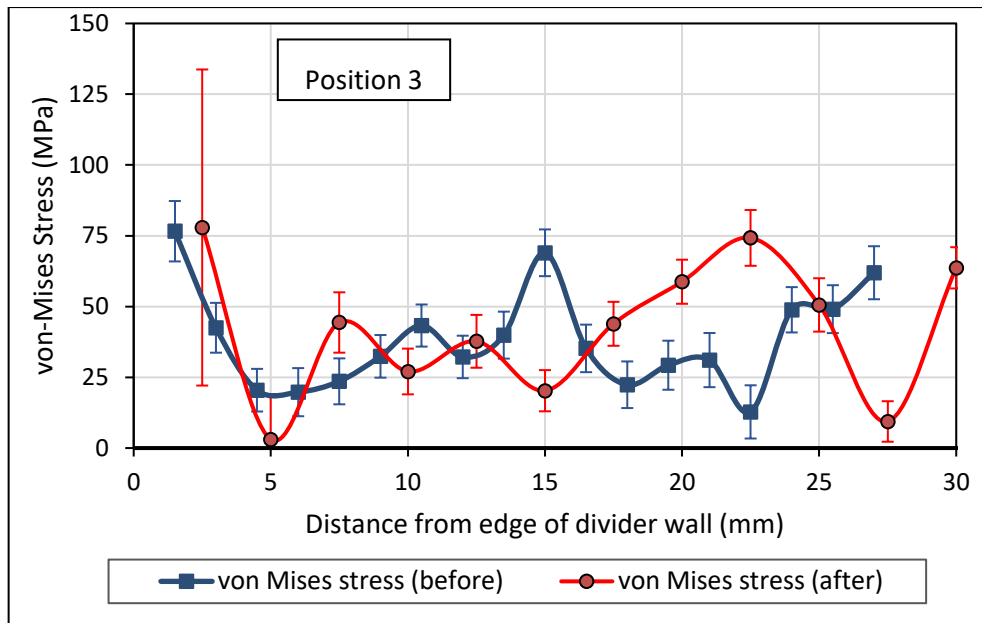


Figure 6.25 Comparison of von Mises stress in the neutron diffraction line scan of position 3 before and after thermal cycling.

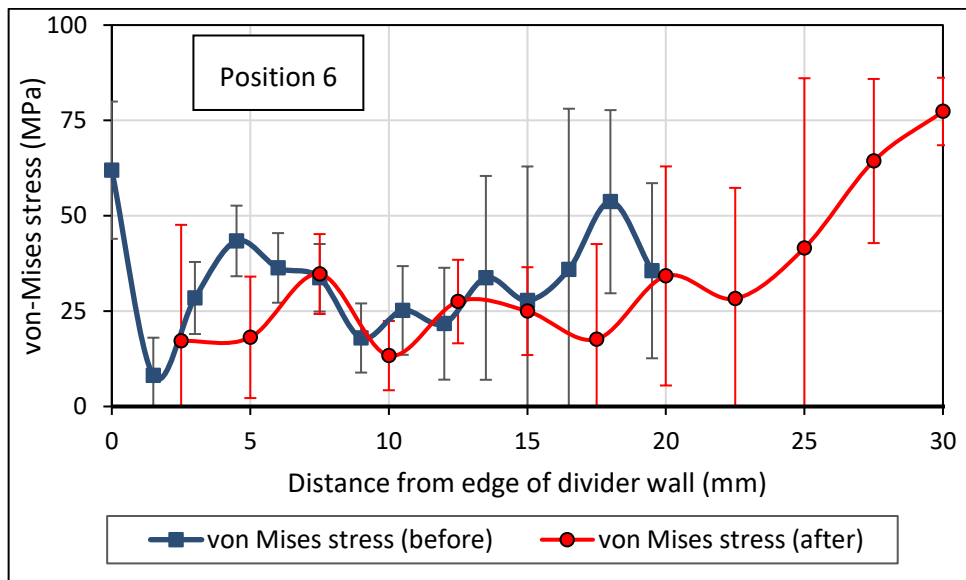


Figure 6.26 Comparison of von Mises stress in the neutron diffraction line scan of position 6 before and after thermal cycling.

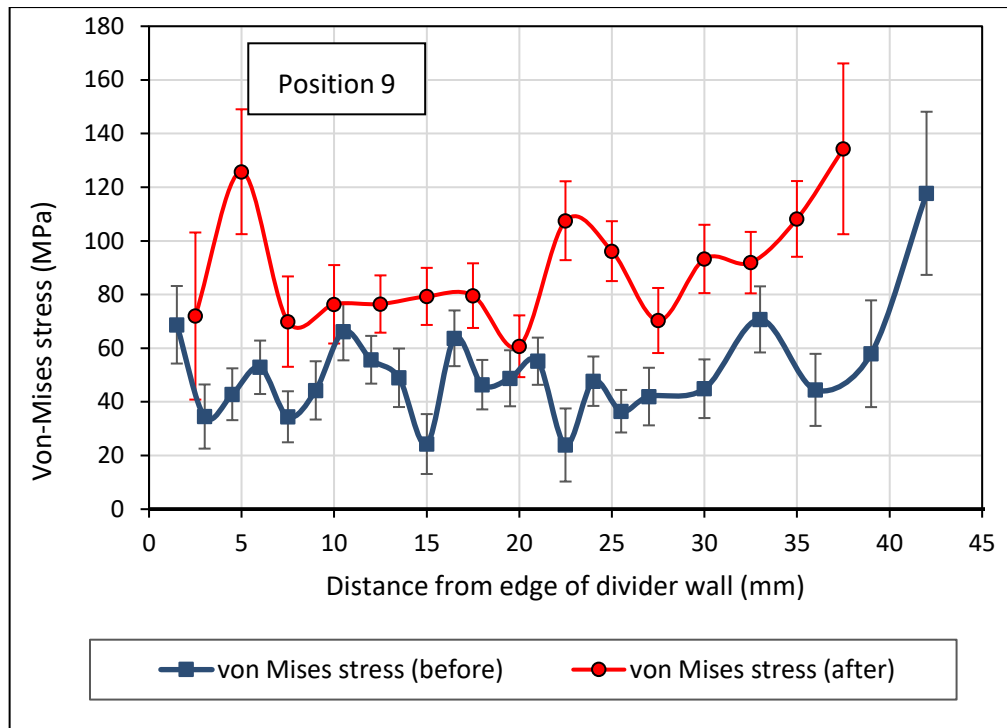


Figure 6.27 Comparison of von Mises stress in the neutron diffraction line scan of position 9 before and after thermal cycling.

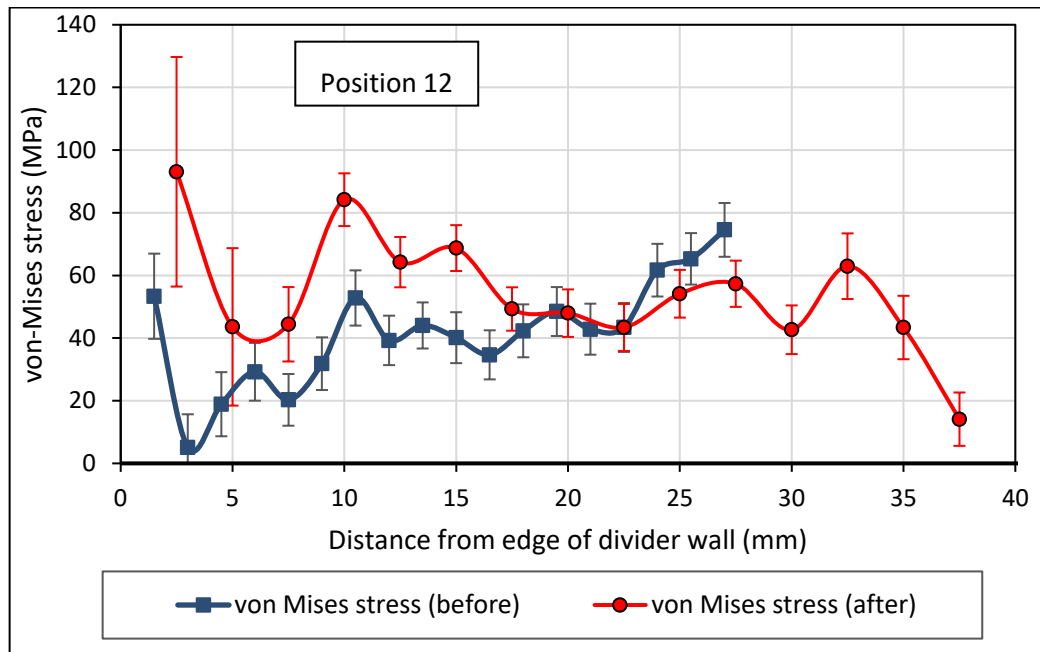


Figure 6.28 Comparison of von Mises stress in the neutron diffraction line scan of position 12 before and after thermal cycling.

6.3. The contour method residual stress measurement

The use of the contour method to measure residual stress in the turbine housing was challenging, and as detailed in the experimental methodology (Section 5), required numerous cutting stages to achieve realistic results. The different cuts made to the turbine housing are once again outlined in Figure 6.29.

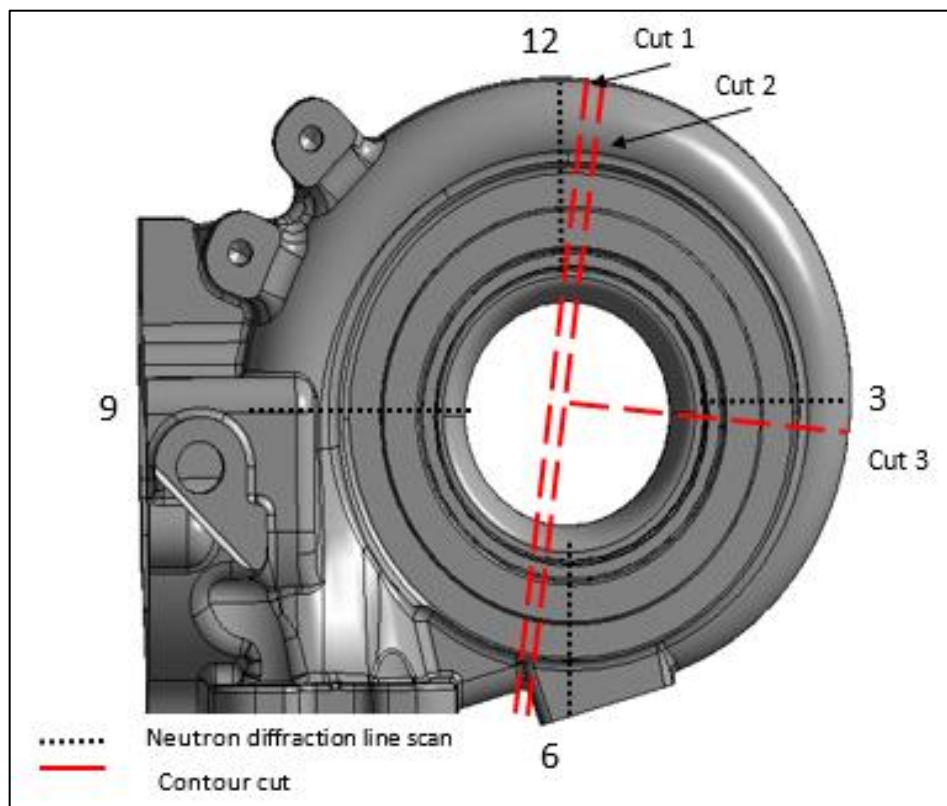


Figure 6.29 Contour measurement cutting planes

To determine the stress in the hoop direction, the negative of each of the measured displacements was imported into the FE model as an external loading and mapped to the surface nodes of each face as a displacement, in a direction normal to the cut surface. The displacement contours for each of the cuts are detailed in Figure 6.30 to Figure 6.32.

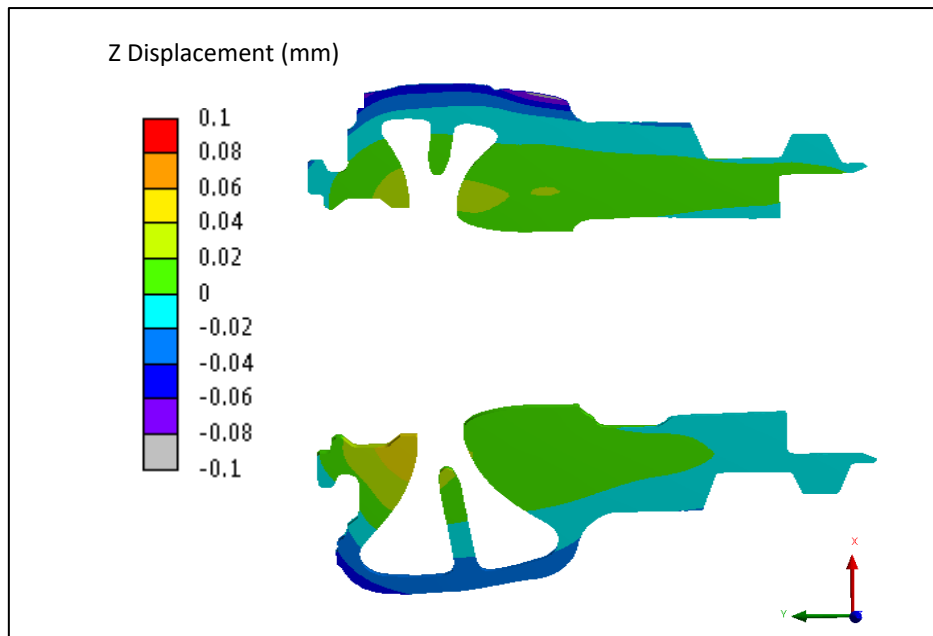


Figure 6.30 The averaged surface displacements in the z-direction after cut 1 smoothed to 1.05mm, 0.25mm mesh size.

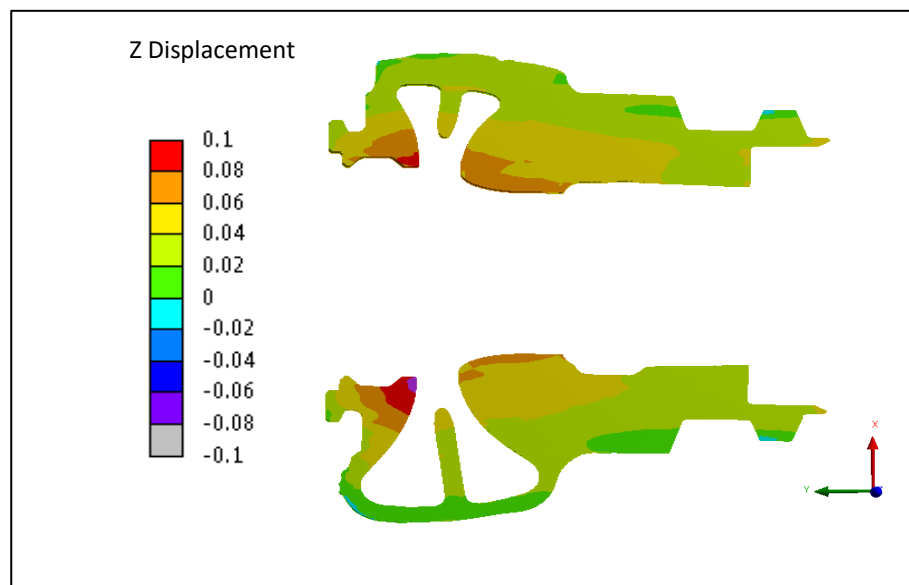


Figure 6.31 Surface displacements of cut 1 after correction from cut 2 (reference cut) smoothed to 1.05mm, 0.25mm mesh size.



Figure 6.32 Final surface displacements after cut 3 using sacrificial layer 3mm smoothing, 1mm mesh size.

The stress results of the first cut are shown in Figure 6.33. The results of cut 1 show horizontal lines referred to as ‘cutting artefacts’, across the cut surface, these were aligned in the direction of the cutting wire and increased the stress levels to unrealistically high magnitudes of more than 800MPa, well in excess of the ultimate tensile strength of the SiMo 5.1 material. To correct the error in the measurement due to the cutting process, a thin slice (cut 2) was taken to obtain an isolated measurement of the cutting artefacts which could then be removed from the cut 1 measurements. The results of this are shown in Figure 6.34 and still reveal unrealistic levels of stress.

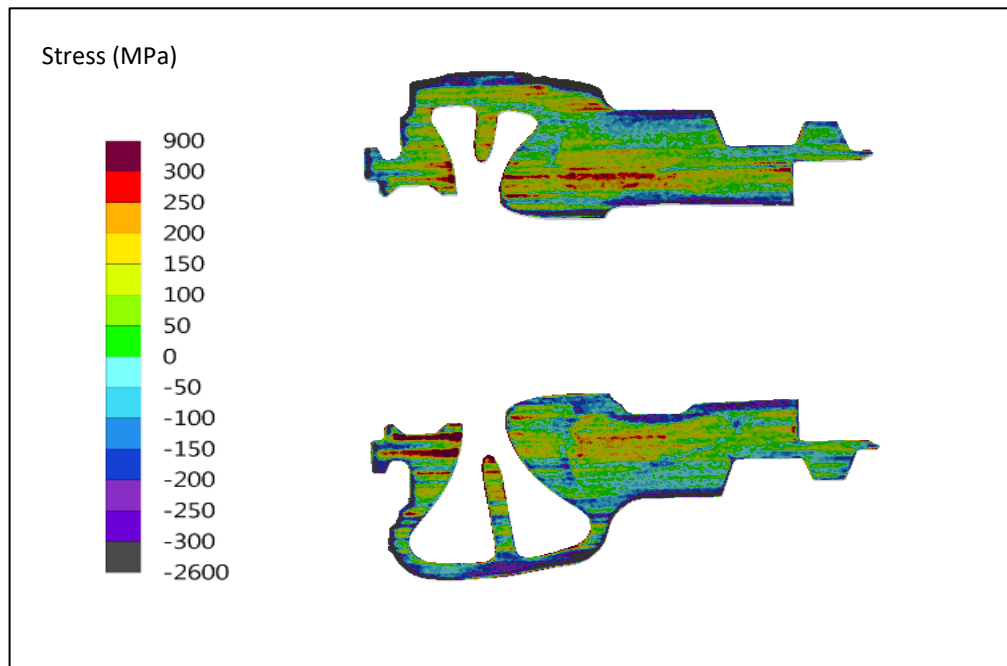


Figure 6.33 Stress normal to the cut plane (Hoop direction) after cut 1 with optimal smoothing at 1.05mm and 0.25mm element size.

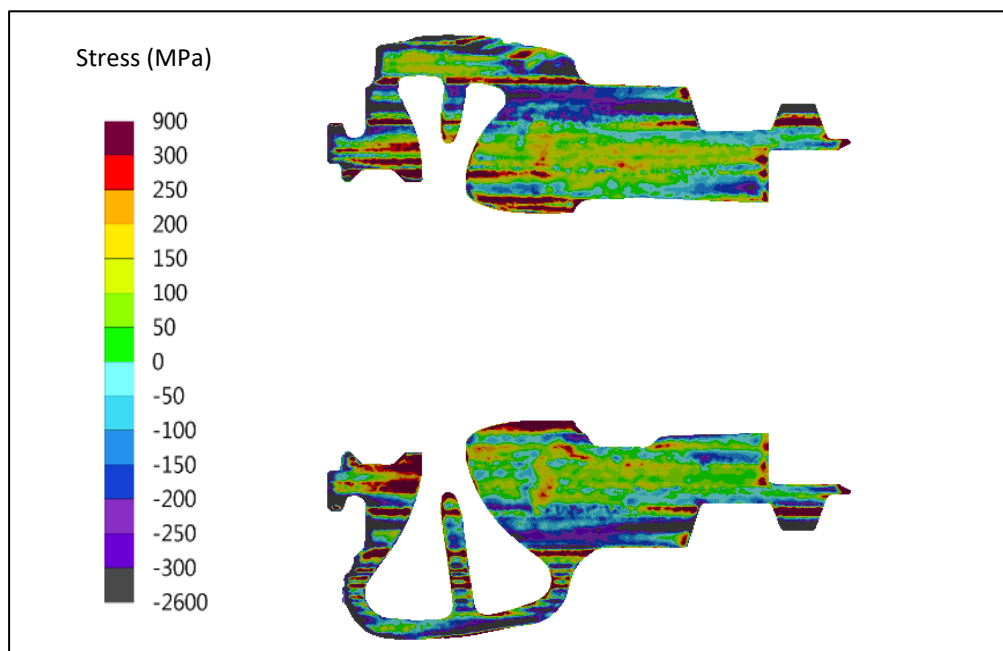


Figure 6.34 Stress normal to the cut plane (Hoop direction) after performing the reference cut (cut 2) smoothing is optimal at 1.05mm knot spacing and 0.25mm element size.

Finally, a sacrificial layer was used. This was performed to keep the wire contact length uniform throughout the cut and therefore reduce the chances of introducing cutting artefacts. The results of this final correction are shown in Figure 6.35. Stress levels were much more realistic with levels between $\pm 30\text{MPa}$ throughout most of the surface.

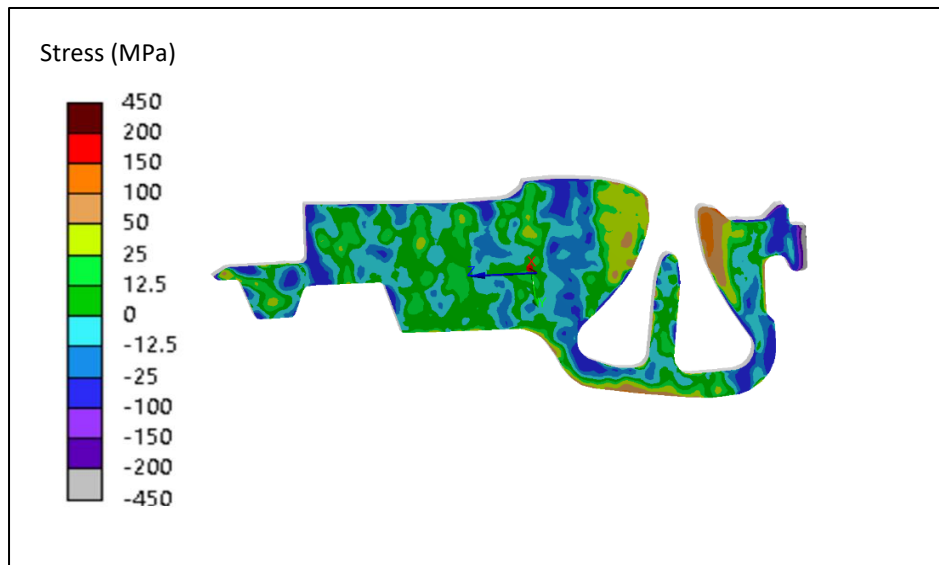


Figure 6.35 Stress normal to the cut plane (Hoop direction) cut 3, with optimal smoothing of 3mm knot spacing and 1mm element size.

6.4. Temperature and strain measurements during thermal cycling on a gas stand test.

Plots of the thermocouple readings over the first 4 of the total 10 cycles of the gas stand test are shown in Figure 6.36. The thermocouple locations align with those detailed in Section 5.4.1. Thermocouple 1 (TC1) was positioned within the mass flow of the exhaust gas at the inlet, therefore recording the exhaust gas temperature, and was the only thermocouple not recording a metal temperature.

The thermocouple readings stabilised after approximately two cycles, after which the target maximum gas temperature of 760°C and minimum of 180°C were achieved with

a fluctuation of $\sim \pm 5^\circ\text{C}$. During the testing it was found that TC5 was not working correctly and has therefore been removed and is not referenced further. A selection of results in this section have previously been published by the author in Gannon et al. (2016).

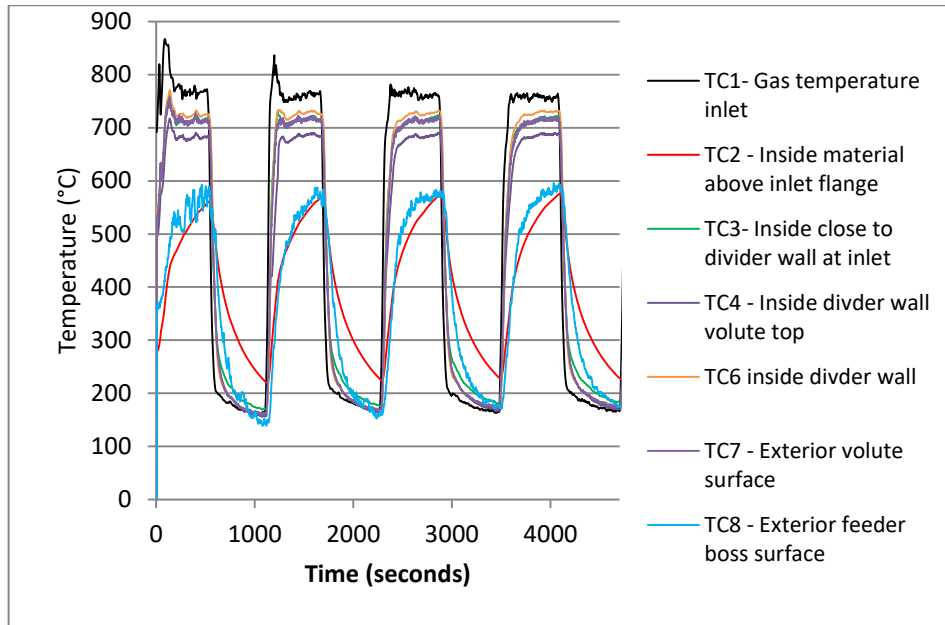


Figure 6.36 Thermocouple readings during gas stand test

Reviewing the thermocouples which record metal temperatures, TC6 within the divider wall of the volute recorded the highest temperature at approximately 732°C after temperatures had stabilised from cycle 4 onwards, closely followed by TC3 again within the divider wall but close to the inlet and TC7. The lowest temperatures were recorded by the thermocouples TC2 inside the material above the inlet flange and TC8 on the surface of the feeder boss at approximately 180°C . Strain gauges were balanced at a temperature of 450°C .

The strain gauge (SG) results are shown in Figure 6.37 alongside the gas temperature. Measured strains were in-phase with the applied temperature. The strain gauges positioned to measure axially recorded the largest magnitudes and a similar trend was observed across all measurement locations, with tensile strains increasing with the application of temperature followed by a reduction towards maximum compressive

strain as the temperature is reduced the cycle then repeats. All strain gauges were positioned on the outer volute wall with strain gauge 4 measuring in the axial direction recording the largest magnitude of $\sim\pm 1500\mu\epsilon$, strain gauge 6 also measuring axial strain recorded $\sim\pm 1200\mu\epsilon$, the strain gauge positioned to measure hoop strain (SG7) recorded maximum values of $\sim\pm 500\mu\epsilon$.

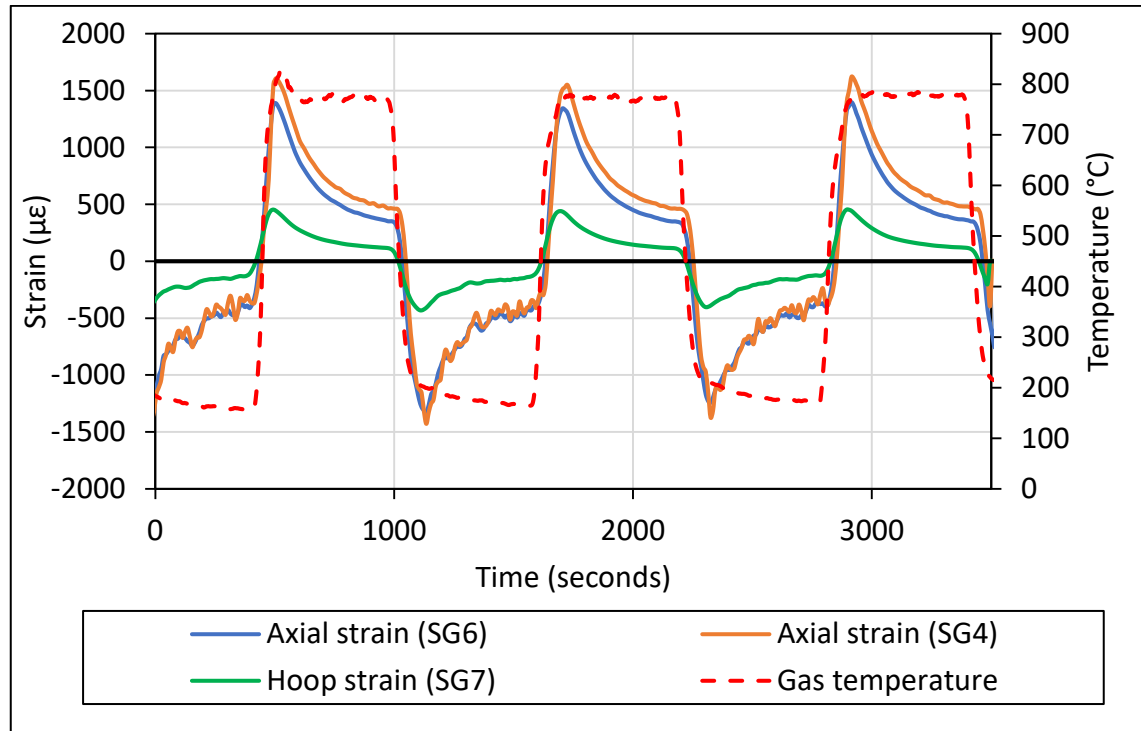


Figure 6.37 Experimental measurements of strain for the various strain gauge (SG) positions shown against gas temperature.

6.4.1. Fatigue crack initiation

The housings utilised for the strain gauge measurements and the neutron diffraction measurements were subjected to 10 thermal cycles each, it was observed after testing that the tongue location had cracked, suggesting rapid onset of fatigue crack initiation in this area.

During the extended thermal cycle testing of 1200 cycles on the gas stand, at intervals of 300 cycles the test was stopped, and the onset of fatigue crack initiation and propagation was monitored and photographed. Prior to sectioning the housing, it was

noted that the only visible fractures were located close to the inlet flange on both sides of the divider wall and in the tongue region. These fractures had already formed by the 300th cycle and by 1200 cycles had continued to propagate and branch out slightly. The photographs taken at 300 cycle intervals up to 1200 cycles are shown in Figure 6.38 to Figure 6.40.



Figure 6.38 Fatigue crack initiation in the divider wall in the outlet side volute.
(Clockwise from top left): after 300 cycles, 600 cycles, 900 cycles and 1200 cycles.

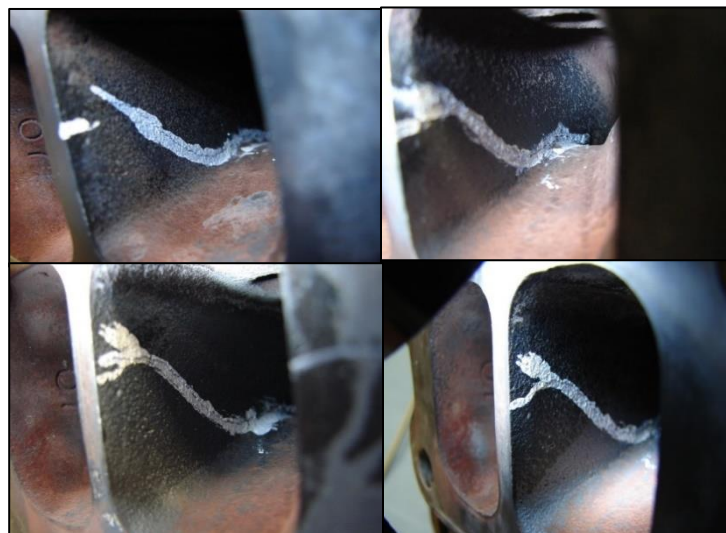


Figure 6.39 Fatigue crack initiation in the divider wall in the bearing side volute.
(Clockwise from top left): after 300 cycles, 600 cycles, 900 cycles and 1200 cycles.

There was no evidence of any external cracking on the surface of the turbine housing after the 1200 cycles were complete as shown in Figure 6.40.

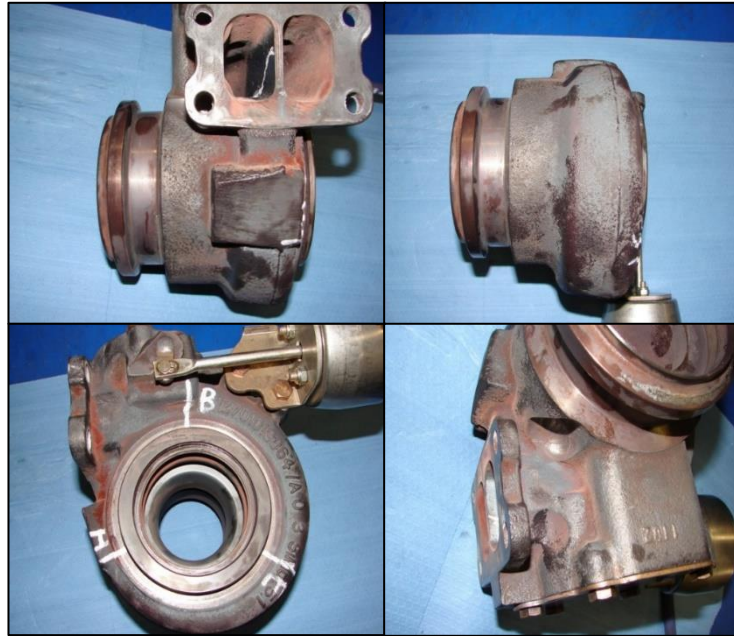


Figure 6.40 External view of the turbine housing after 1200 cycles of gas stand testing

To inspect the turbine housing for internal cracks, it was sectioned in half so that any fractures in the divider wall could be clearly seen. Figure 6.41 shows a cross sectional view of the fracture locations at the end of the test. Further crack locations were visible. In total there were four crack initiation sites which were numbered 1-4. Location 1 is the fracture shown in Figure 6.39 and has most likely initiated from the tongue location, continuing to propagate towards the inlet. The fracture on the opposite side of the divider wall at location 1 as shown in Figure 6.38 initiated separately, again most likely starting in the tongue location. The two fractures do not however meet and as such the crack does not completely go through the divider wall. Additional fracture locations at positions 2, 3 and 4 have initiated on the edge of the divider wall and propagated radially. Fracture 4 due to the narrower depth of the divider wall, was able to propagate towards the outer wall of the housing. It did not

however, propagate completely through to the outside, but instead began to travel circumferentially around the inside surface of the housing.

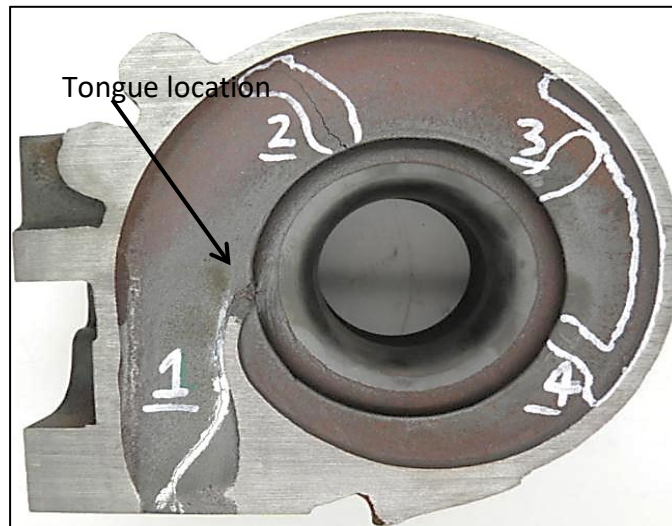


Figure 6.41 Fatigue fractures locations within the divider wall after 1200 thermal cycles.

7. Simulation methodology

This chapter will outline the methodology which will be followed to investigation, both residual and applied stresses in the turbine housing. The residual stress simulations will consider the main stages in the production process which could potentially introduce or alter residual stress levels these are:

- Solidification and cooling in the mould (before shakeout).
- Cooling outside of the mould to ambient temperature (after shakeout).
- Removal of feeding and gating system.
- Heat treatment.
- Machining (simplified approach)
- After the application of a thermomechanical load

The applied loading of the TMF will be investigated with a coupled transient thermal and structural analysis.

7.1. Residual stresses during the manufacturing process

The simulations of the casting process, heat treatment and machining was performed using MAGMA casting software (Magmasoft, 2018).

7.1.1. Geometry detail

To correctly simulate the solidification and subsequent cooling of the casting prior to its removal from the mould (before shakeout), a CAD model was created which consisted of a turbine housing casting positioned inside a green sand mould. Internal cavities such as the twin gas passages were filled with sand cores to replicate the production process. During the casting stage of the turbine housing molten metal enters the mould through a gating system; this was also represented. Figure 7.1 shows the model used in the before shakeout simulation (prior to the removal of the green

sand mould and cores). The positioning of the gating, and the orientation of the casting were assumed as this information was proprietary and could not be provided by the foundry therefore the following assumptions were made regarding the orientation and feeding location of the casting. It was assumed that the orientation of the casting inside the mould would be such that the outlet face of the turbine housing (as highlighted in Figure 7.1) would be at the top of the mould box, this was a reasonable assumption to make, because as the melt is poured into the mould, the molten liquid would have only a short distance to fall and therefore less of a chance of splashing resulting in turbulence and increasing the risk of potential casting defects during filling. It was also very likely that the casting was fed from the bottom due to the presence of the feeder boss on the finished component, hence the chosen location for the feeder on the model.

To model the behaviour of the casting after it is removed from the mould the mould was removed in addition to the gating system and sand cores.

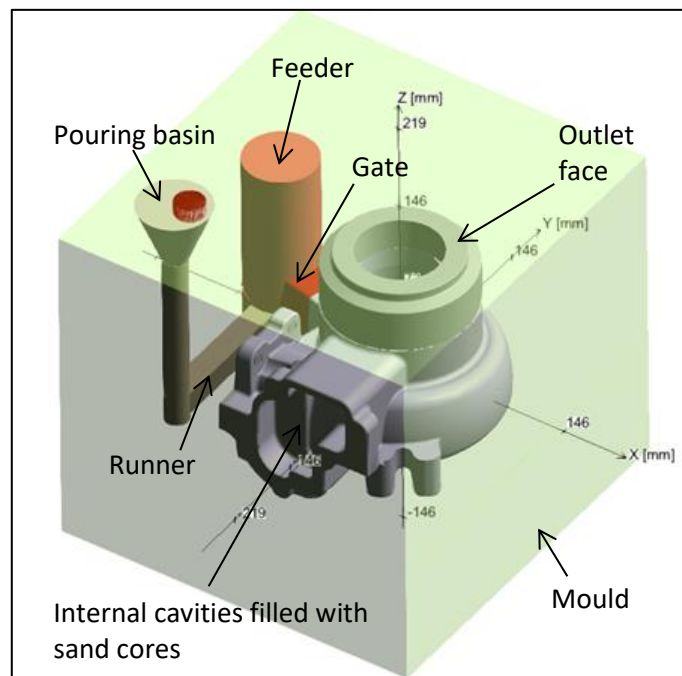


Figure 7.1 Before shakeout model used in the MAGMA simulation

7.1.2. Material assignment

The turbine housing material was defined as SiMo 5.1 in the MAGMA material database. The chemical composition was input to align with the values measured from the actual part as detailed in Section 6.1. Based on the defined chemical composition, the liquidus and solidus temperature were determined automatically by the software along with the density, the fraction solid curve and latent heat. A unified creep-plasticity model with temperature dependant creep data was selected to account for the time-dependant behaviour required to capture any stress relaxation that may occur during the heat treatment stages. The material models applied to each part of the model are detailed in Table 7.1 and the creep curves considered in the simulation are shown in Appendix 3. The temperature dependant thermal, physical and mechanical properties of the sand mould material available in the MAGMA data base, are well defined in existing literature by Echavarría & Namjoshi (2007). Temperature dependant, physical, thermal and mechanical properties were also defined for the SiMo 5.1 material and aligned with that considered in the subsequent TMF simulations.

Component	Material model
Turbine housing	EN-GJS-XSiMo5-1 unified creep-plasticity model
Feeding/gating system	EN-GJS-XSiMo5-1 unified creep-plasticity model
Sand core	Green sand thermo-physical model
Sand box	Green sand thermo-physical model

Table 7.1 Material models used for the MAGMA simulations.

7.1.3. Meshing

MAGMA utilises the finite-difference numerical method of meshing. This results in one node in the centre of each element as such the number of elements and nodes are the same. The mesh was generated based on the wall thickness with the minimum number of elements across a section set to 3 elements. Elements near the divider wall and

tongue region averaged $\sim 2\text{mm}$ in size, this was chosen to ensure elements were similar in dimension to the gauge volume used in the neutron diffraction measurements, such a methodology has been detailed by Johnson et al., (2011) in a benchmarking study comparing simulated stress in castings with experimental neutron diffraction measurements which resulted in good correlation. Figure 7.2 details the mesh used in the simulation. An inflation mesh was defined on the mould material which produced a fine mesh at the boundary with the casting.

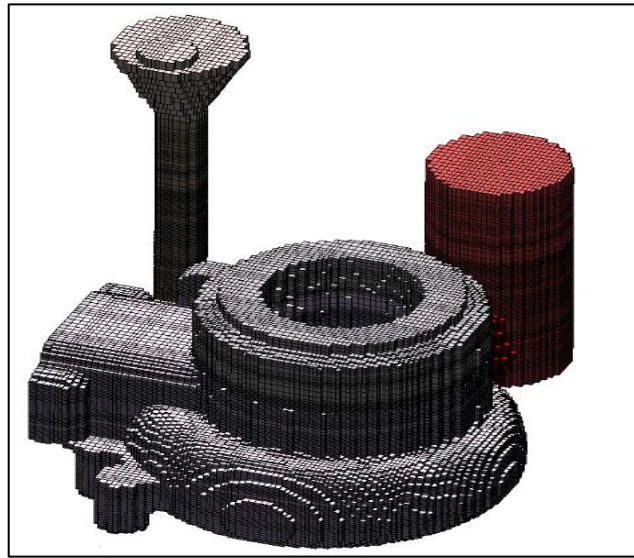


Figure 7.2 Mesh applied during the MAGMA simulations.

7.1.4. Boundary conditions

To capture the influence of gravity which is necessary to correctly simulate the flow of the molten liquid into the mould a gravity boundary condition was applied in the minus Z direction, aligned with the coordinate system shown in Figure 7.3. A fictional contact was defined between the casting and the mould wall to allow for the interaction between the casting and the mould wall and the mould behaviour was defined as deformable.

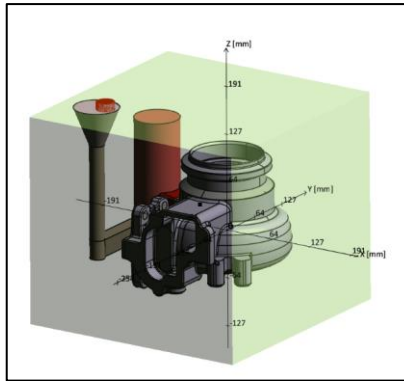


Figure 7.3 The gravity boundary condition was defined in the minus Z direction to ensure correct filling behaviour of the mould.

7.1.4.1. Before shakeout simulation

The initial temperature of the molten cast iron at the point it is poured into the mould was defined as 1400 °C. While the green sand mould and cores defined with an initial temperature of 25°C these parameters were obtained from the foundry technicians (Basha, 2015).

Temperature dependant convection boundary conditions were then applied to the contact faces between the exterior wall of the casting and the internal mould wall, to simulate the decreasing rate of heat transfer that takes places as the casting contracts away from the mould face during solidification. Based on the values detailed in Figure 7.4 which were obtained from the prior research of Echavarría & Namjoshi (2007).

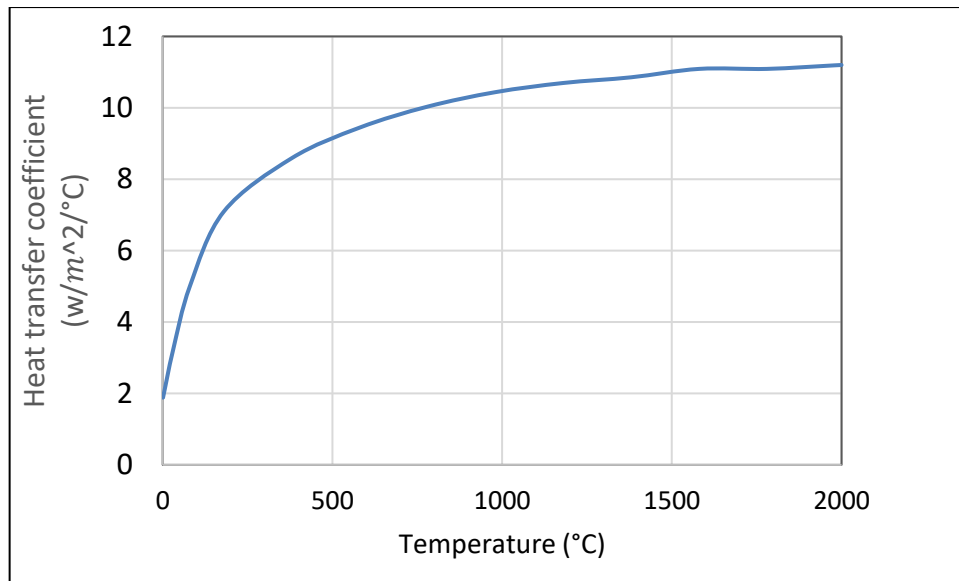


Figure 7.4 Heat transfer coefficient defined to account for the change in heat transfer between the mould wall and casting as the casting solidifies.

The total time taken to pour the melt into the mould cavity was defined as 7 seconds using a lip pouring method, after which solidification rapidly took place, with the casting remaining in the mould for 75 minutes in line with the foundry process (Basha, 2015).

7.1.4.2. After shakeout

After the 75 minutes of in-mould cooling the mould and cores were removed from the model and a natural convection boundary condition of $20 \text{ w/m}^2 \text{ } ^\circ\text{C}$ was applied to the exposed surface of the casting to simulate exposure to an ambient air temperature of 25°C , in addition a radiation boundary condition was defined with a temperature of 25°C and an emissivity of 0.8. The casting was then allowed to cool to 25°C and the feeding and gating system was removed.

7.1.4.3. Annealing heat treatment and machining

The heat treatment profile to which the turbine housing was subjected to in the MAGMA simulation was defined as shown in Figure 7.5. This was consistent with the heat treatment performed by the foundry (Basha, 2015). The heat treatment was controlled for approximately 17 hours, after which the casting was cooled under natural convection. This was replicated in the simulation by applying a natural

convection boundary condition to the exterior faces of the turbine housing once again of $20 \text{ W/m}^2 \text{ } ^\circ\text{C}$.

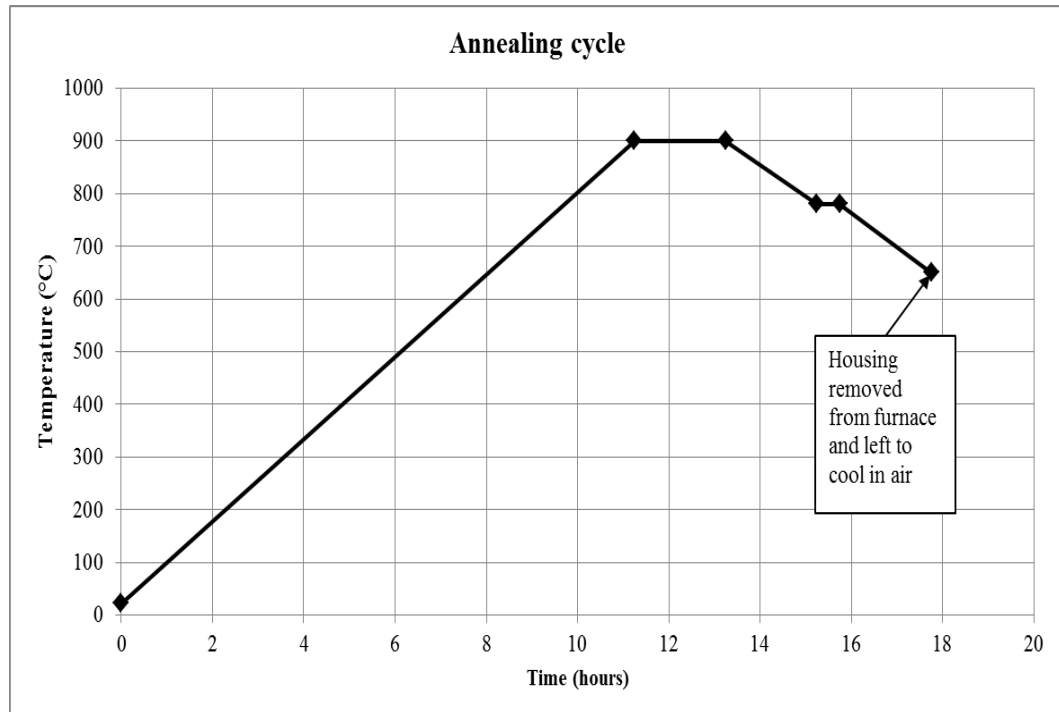


Figure 7.5 Annealing heat treatment cycle applied to simulation

To finish the simulation, a final machining operation was simulated to remove material to produce the machined finished part as is shown in Figure 7.6. This was achieved by morphing the cast model into the finished machined part, the software does not allow for any considerations of constraint during the machining operation.

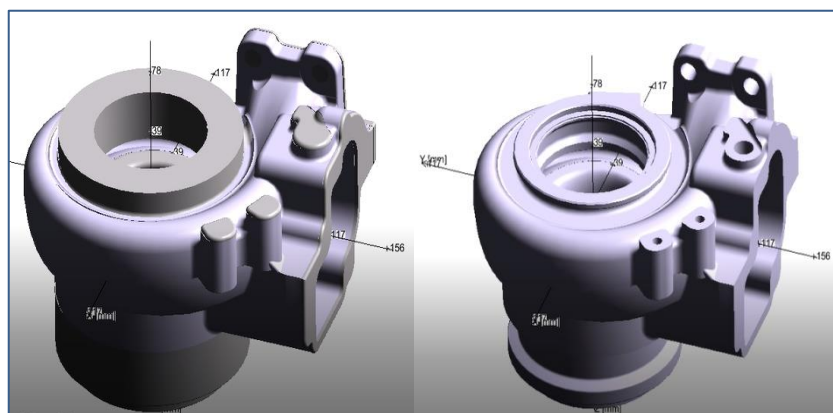


Figure 7.6 Morphing of the cast geometry into the final machined shape

The final stress distribution at the end of the simulated production process was then imported into ANSYS mechanical. MAGMA does not allow for the viewing of the individual stress components based on a cylindrical coordinate system, so this post processing was performed in ANSYS. As a result, a method was required to transfer the results into Ansys this is detailed in appendix 2. This method was also used to import the residual stress results as a pre-stress condition prior to TMF simulations.

7.2. Thermomechanical fatigue simulation

A Thermo-mechanical fatigue (TMF) simulation of the thermal cycle performed on the gas stand was carried out using ANSYS Mechanical software(ANSYS, 2016). The TMF simulation procedure involved two stages. The first stage consisted of a transient thermal analysis which to determine the thermal loads generated during thermal cycling, using a simplified 1D approach to determine the heat transfer coefficients and temperatures as detailed by Oberste-Brandenburg, Shoghi, Gugau, and Kruse (2012). The second stage involved a static structural analysis in which the thermal load from the previous step was imported and applied to the assembly model, while part of the assembly was constrained. Stress and strain values due to the cyclic thermal load were then obtained. Figure 7.7 shows a schematic of the analysis, in which the solution from the transient thermal portion of the analysis is coupled to the static structural analysis.

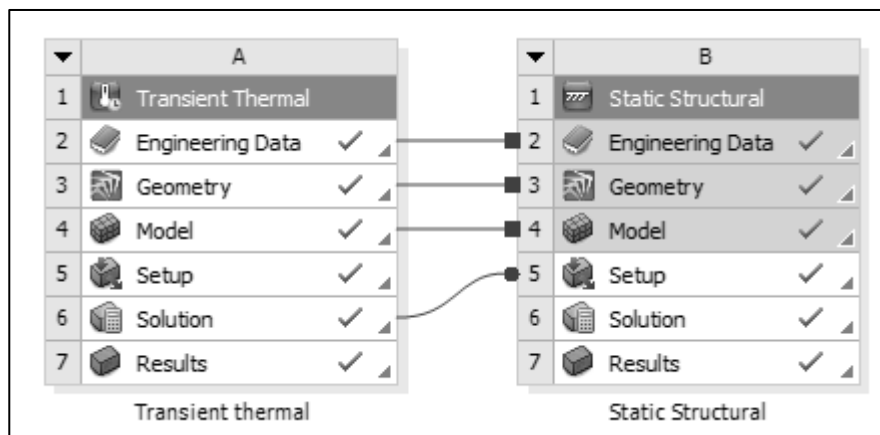


Figure 7.7 FEA schematic for the TMF simulation

7.2.1. Model details and material assignment

The simulated assembly model is shown in Figure 7.8 and consisted of the turbine housing, bearing housing, exhaust manifold, heat shield and a rectangular block representing the engine cylinder head. Simplified bolts (without threads) were also created for the connection between the turbine housing inlet flange and the manifold flange. The heat shield was only present for the thermal portion of the simulation as its impact on the mechanical behaviour of the turbine housing is insignificant.

In comparison to the parts considered on the gas stand test, the burner exit pipe was not considered, instead a model of a manifold was used to represent the on-engine behaviour. However, the thermal inertia of the manifold is generally comparable to that of the burner exit pipe (k. Shoghi, 2018). The simulation model does not contain the compressor stage of the turbocharger as was present during the gas stand test. This was done to simplify the model as the compressor side was not the focus of the investigation and its presence is not required in TMF simulations of the turbine housing. Any thermal influence of the compressor and oil cooling within the bearing housing will be approximated for during the simulation with heat transfer coefficients and temperatures.

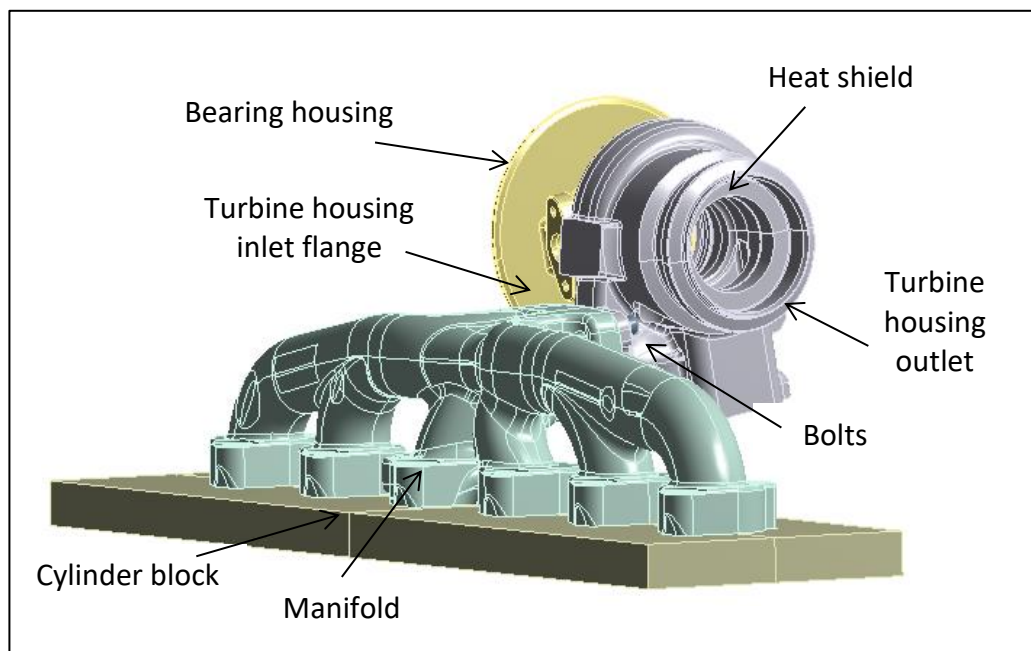


Figure 7.8 Model considered in the TMF simulation

The thermal part of the simulation requires density, and temperature dependant specific heat and thermal conductivity values to be defined from room temperature to beyond 760 °C. The properties for SiMo 5.1 were defined as follows.

To simulate plasticity in the turbine housing, nonlinear material data was required. The material model applied to the turbine housing consisted of an isotropic hardening material model which was defined based on thermophysical and temperature dependent mechanical test data for the SiMo5.1 grade of cast iron. Each of the material models was isotropic in behaviour with the following material applied to the various components as detailed in Table 7.2. This research will use a multilinear isotropic material models to identify the areas prone to fatigue crack initiation in the turbine housing considering the level of PEEQ after one simulated cycle.

Component	Material grade	Included in both structural and thermal simulation
Turbine housing	SiMo5.1	Yes
Manifold	SiMo5-1	Yes
Cylinder block	SiMo5-1	Yes
Bearing housing	GJL-250	Yes
Bolts x 4	1.4980	Yes
Heat shield	310S	No

Table 7.2 Material model assigned to each component

7.2.2. Meshing

Meshing was achieved with quadratic tetrahedral elements. A mesh convergence study was undertaken for the stress portion of the simulation, this focussed on increasing the number of elements across the wall thickness and the number of element across a radius, locally around the divider wall region. The mesh was refined using local proximity and curvature settings within ANSYS, this allowed for the number

of elements across a wall thickness and the number of elements spanning an angle using the curvature normal angle setting to be defined. The analysis time was set to 50 seconds rather than solving for the full thermal cycle. Details regarding the mesh refinement are shown in Table 7.3 with the results of the convergence study given in Figure 7.9. Examples of the mesh are shown in Figure 7.10

Iteration	Number of elements across divider wall thickness	Elements across fillet	von Mises stress Across divider wall (MPa)	von Mises stress in radius (MPa)
1	1	3	138	-
2	3	3	147	-
3	5	3	152	-
4	3	1	-	90.28
5	3	3	-	106.71
6	3	5	-	112.25
7	3	7	-	112.55

Table 7.3 von Mises stress results of ANSYS convergence study.

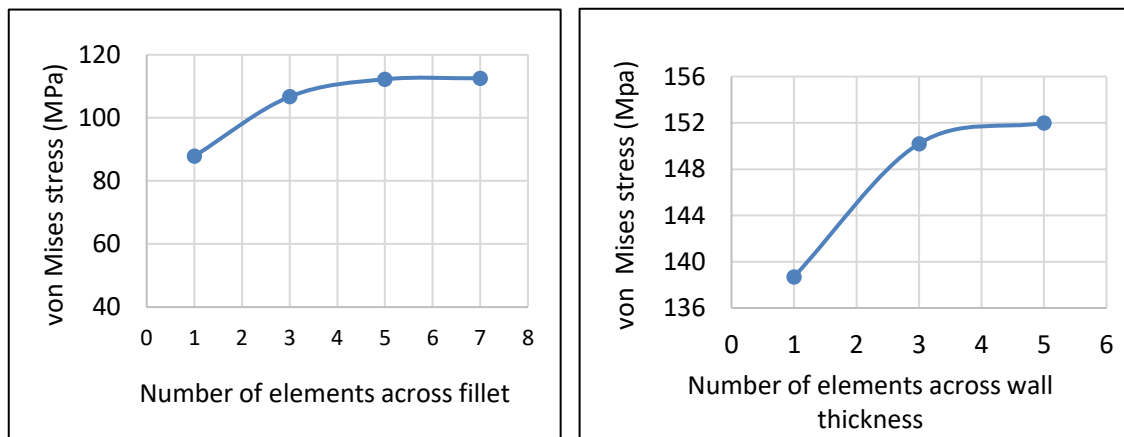


Figure 7.9 Mesh convergence results

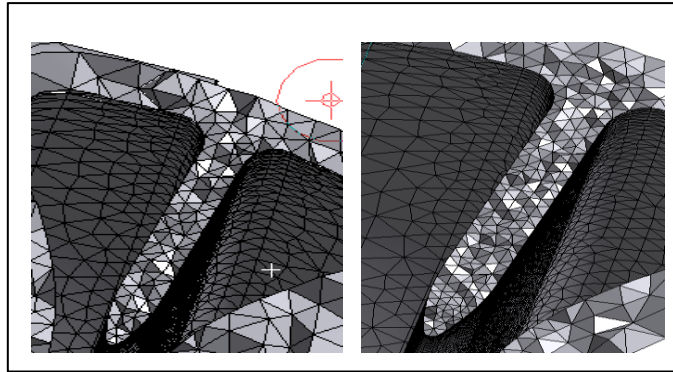


Figure 7.10 Mesh refinement for convergence study in ANSYS (Left: 3 elements across the wall thickness, Right: iteration 7 elements across the wall thickness).

It was decided for the turbine housing to allow for 3 elements across the wall thickness, and 5 elements across fillets in critical areas such as the tongue and divider wall region. Elsewhere, 2 elements across the wall thickness and 3 across the fillets will suffice in areas less likely to experience high strain gradients. A maximum element size of 3 mm was defined for the turbine housing. The other components in the assembly were defined with a 5mm mesh.

7.2.3. Contact definition

The contacts were defined as detailed in Table 7.4, frictional contacts were assigned with a friction coefficient of 0.2.

Contact pairing	Type
Cylinder – head to manifold	Bonded
Manifold to turbine housing	Frictional
Manifold to bolt shafts	Bonded
Heat shield to turbine housing	Bonded
Heat shield bearing housing	Bonded
Turbine housing to bolt head	Frictional
Turbine housing to bearing housing	Bonded

Table 7.4 The contact definitions assigned for the TMF simulation.

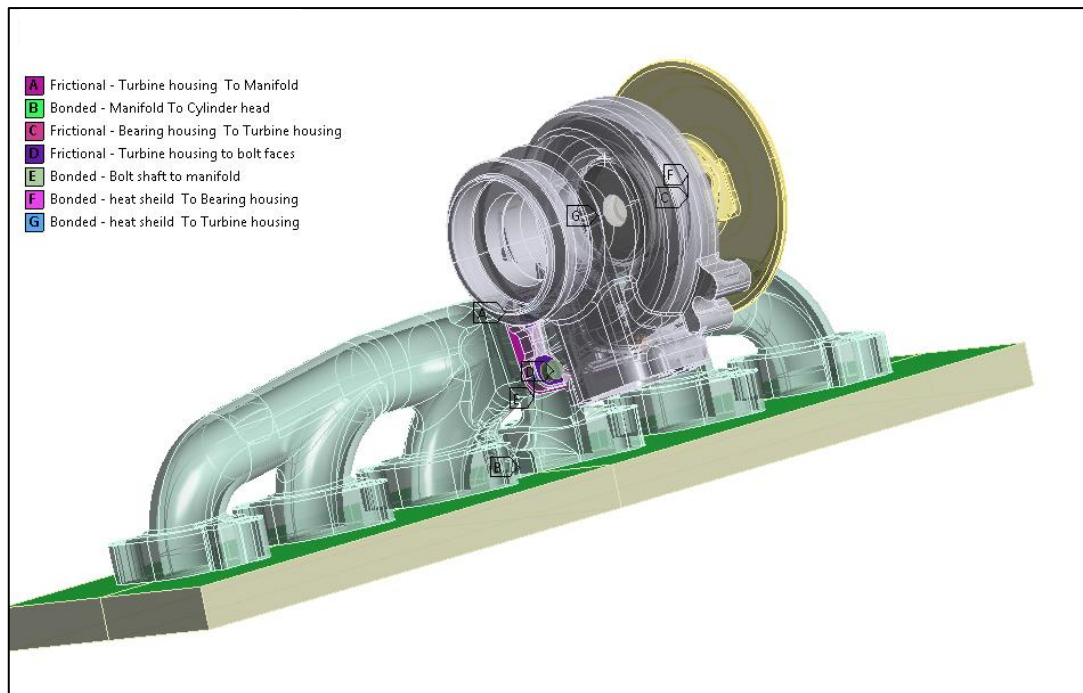


Figure 7.11 Contact definitions applied to the contact faces, the frictional coefficient was defined as 0.2.

7.2.4. Thermal simulation methodology

In order to simulate this convective transfer of heat within the turbine housing, a 1D programme, previously developed within BorgWarner Turbo systems (Oberste-Brandenburg et al., 2012), was used to calculate the convection values. This method has been proven to provide good correlation with computation fluid dynamic (CFD) generated data for turbine housing TMF simulation.

The 1D programme required thermodynamic inputs for both the hot and cold stages of the thermal cycle such as turbine inlet temperature, turbine inlet/ outlet pressure and exhaust gas mass flow, the values of which were aligned with the gas stand test as detailed in section 1. In addition, dimensions of the turbine wheel, turbine housing and exhaust manifold were entered into the programme. From the specified data the programme calculated the temperatures and convective heat transfer coefficients for specific regions in contact with the flow of exhaust gases within the turbine housing such as the shroud, volute, and manifold. These values were then applied to the

associated area on the turbine housing. Figure 7.12 shows the areas in which convective boundary conditions were applied internally within the turbine housing.

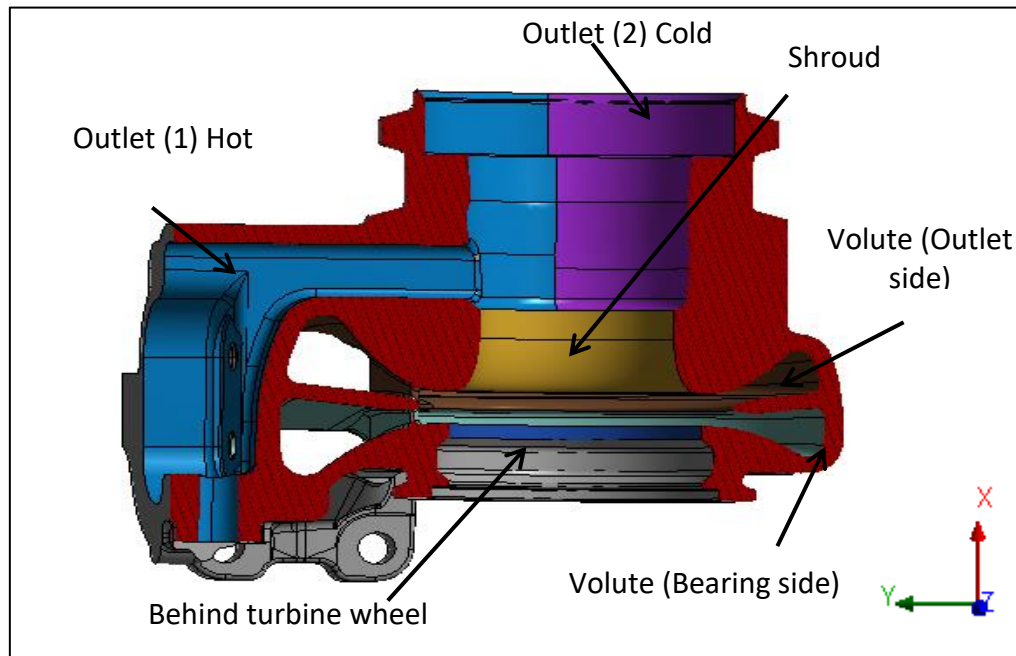


Figure 7.12 Cross sectional view of the internal faces of the turbine housing to which convective boundary conditions are applied for the TMF simulation.

The Heat transfer coefficients applied internally to the turbine housing and manifold are shown in Table 7.5 for both the hot and cold phases of the thermal cycle.

	Hot condition		Cold condition	
Location	Heat transfer coefficient (W/m ² .C°)	Temperature (°C)	Heat transfer coefficient (W/m ² .C°)	Temperature (°C)
Manifold	326.8	720	284.9	180
Outlet (1) Hot	1158.1	674.9	1186.9	161.4
Outlet (2) Cold	414.1	584.8	405.9	126.3
Shroud	3051.3	641.9	2788.7	146.7
Behind turbine wheel	513	620	580	160
Volute bearing side	442.9	717.9	419.8	178.5
Volute outlet side	429	718.5	440.8	177.7

Table 7.5 Heat transfer coefficients and temperatures applied to the internal areas of the turbine housing.

In addition to the heat transfer coefficients relating to the transfer of heat from the exhaust gas to the internal walls of the turbine housing, heat transfer coefficient boundary conditions were applied behind the heat shield and within the bearing housing to account for the cooling due to the oil as shown in Figure 7.13 and Figure 7.14. The values applied to these areas are defined in Table 7.6 and were based on approximations (Weber, Karamavruc, & Shoghi, 2015).

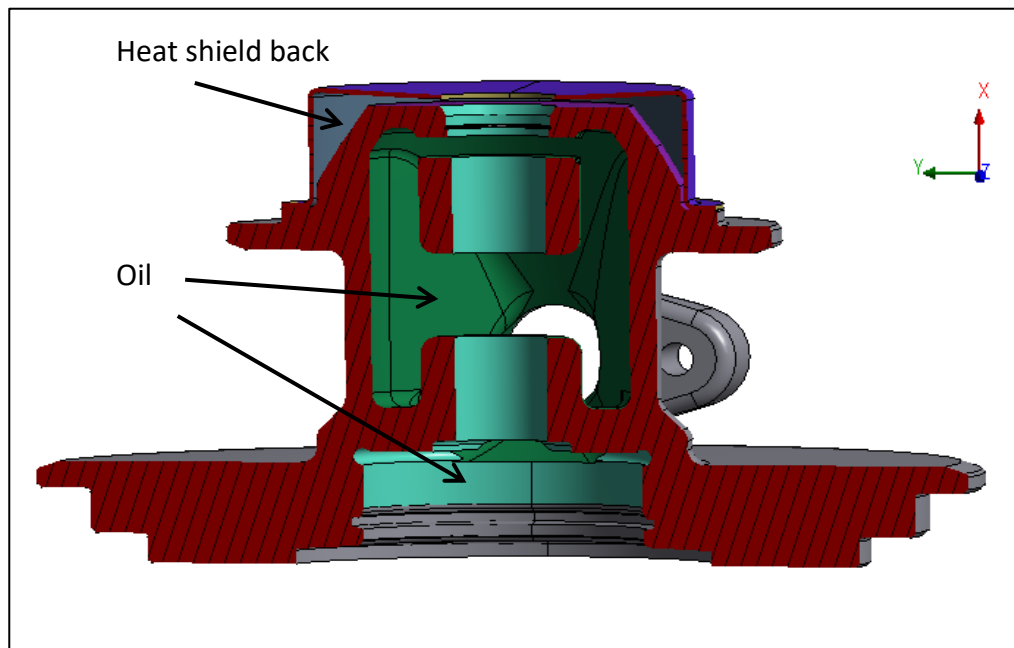


Figure 7.13 Cross sectional view of the internal faces of the bearing housing and heat shield to which convective boundary conditions are applied for the TMF simulation.

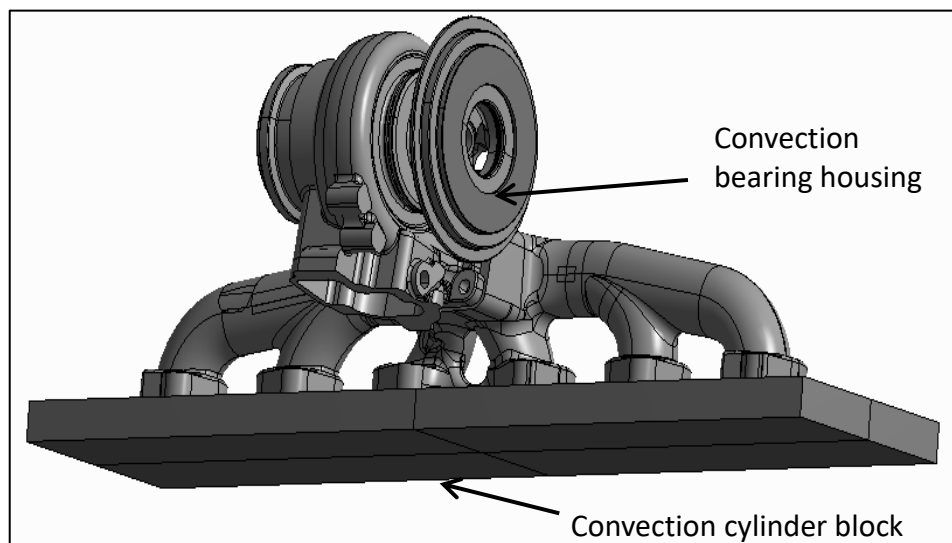


Figure 7.14 Convection heat transfer coefficients applied to the bearing housing and cylinder head block.

The thermal conductance between each part was controlled automatically by the software. The time setting for the transient thermal analysis was defined with automatic time stepping. This option allows the initial, maximum and minimum time step duration to be specified, and gives the program the freedom to select smaller or larger time steps up to the defined limits as necessary for convergence.

	Hot condition		Cold condition	
Location	Heat transfer coefficient (W/m ² .C°)	Temperature (°C)	Heat transfer coefficient (W/m ² .C°)	Temperature (°C)
Bearing housing face	1000	220	1000	150
Heat shield (back face)	300	608	300	144
Oil	1000	113	1000	110
Cylinder head	1000	220	1000	150

Table 7.6 Heat transfer coefficients and temperatures applied to the internal areas of the turbine housing.

The external surface of the manifold, turbine housing and bearing housing are exposed to ambient air. To account for this a natural convection boundary condition of 20 W/m² at a temperature of 30°C was applied to the external faces of all the components in addition to a radiation boundary condition with an emissivity of 0.8 and an ambient temperature of 30°C. Large convection values were also applied to the bearing housing face on the compressor side as well as to the cylinder head face as shown in Figure 7.14.

The time step was defined using the Ansys programme-controlled setting. This works by ensuring that no material properties change by more than 5% (i.e. specific heat, thermal conductivity) over the time step interval. In order to simulate the residual stress state at the end of the thermal cycle the temperature at the end of the thermal cycle was allowed to cool to an ambient temperature of 22°C.

7.2.5. Structural simulation methodology

For the structural portion of the FEA simulation the heat shield was removed as it would have little impact in inducing strain in the turbine housing. To prevent rigid body motion, the cylinder block was constrained by applying by assigning zero displacements in X, Y and Z as shown in Figure 7.15

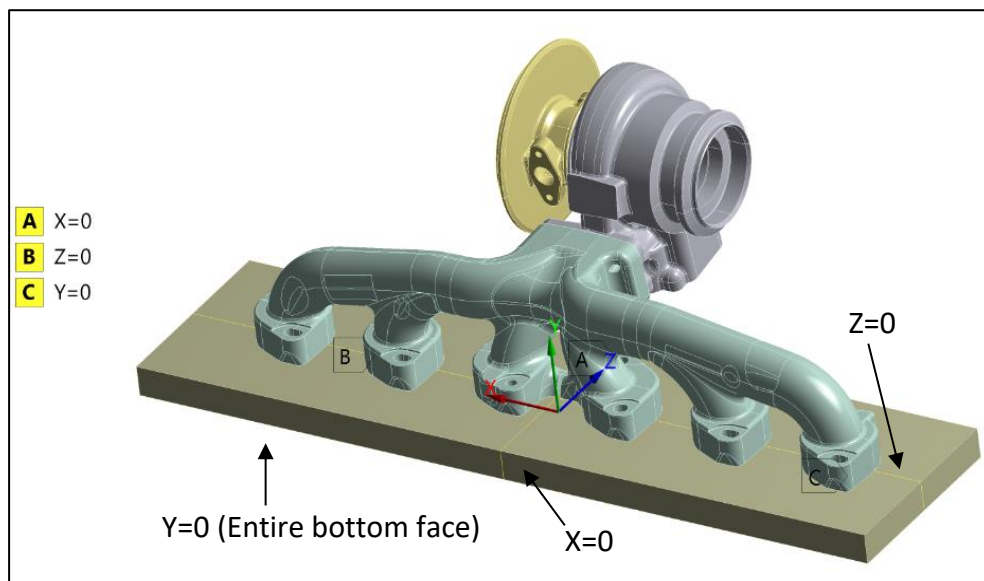


Figure 7.15 Displacement constraints applied to the FEA model.

Bolt preloading was then applied to the bolts to account of a 18KN clamp loading in the first time step, then results from the previous transient thermal analysis were imported as a load and applied to all parts of the assembly. The time step settings for

the structural simulation were the defined to match those used in the transient thermal stage, consistent with the main stages of the cycle.

7.2.5.1. Extraction of strain and temperature results

To analyse the stress and temperature results in the same location in which the strain gauges and thermocouples had been applied, a cylindrical coordinate system was defined. Areas and nodes in the areas on the FEA model corresponding to the strain gauge and thermocouples locations were selected for temperature and an average value obtained. The same was repeated for the extraction of the strain results with normal strains in the relevant direction (axial, radial hoop) extracted for comparison with strain gauge measurements. The accumulated plasticity was also determined using the command NLEPEQ to determine the areas likely to crack during prolonged thermal cycling; this was based on a 0.2% plasticity limit for the cast iron material and the last time step in the simulation.

7.2.5.2. Residual stresses after thermal cycling

To account for the residual stresses at the end of the thermal cycle, the imported thermal load step was deactivated for the final time step.

8. Simulation results

This chapter will present the results of the simulations which consider:

- i. The residual stresses generated through the manufacturing process of the turbine housing
- ii. The applied stress due to a coupled transient thermal load and mechanical constraint (TMF)
- iii. Residual stresses after simulated TMF cycle

8.1. Simulation of residual stresses due to manufacturing processes

8.1.1. Solidification

Residual stress simulation were performed using MAGA casting software (Magmasoft, 2018). The MAGMA simulation first focussed on the molten cast iron as it entered the mould via the feeding and gating system through the feeder boss location. Figure 8.1 shows the temperature within the cavity of the mould during this process with the sand moulds hidden for clarity. The initial pouring temperature was 1400 °C and the time to complete the pouring process was 7 seconds in line with foundry practices. The temperature of the liquid cast iron begins to drop rapidly. After almost 7 second areas within the cavity reach ~ 1230°C almost 200°C below the initial pouring temperature.

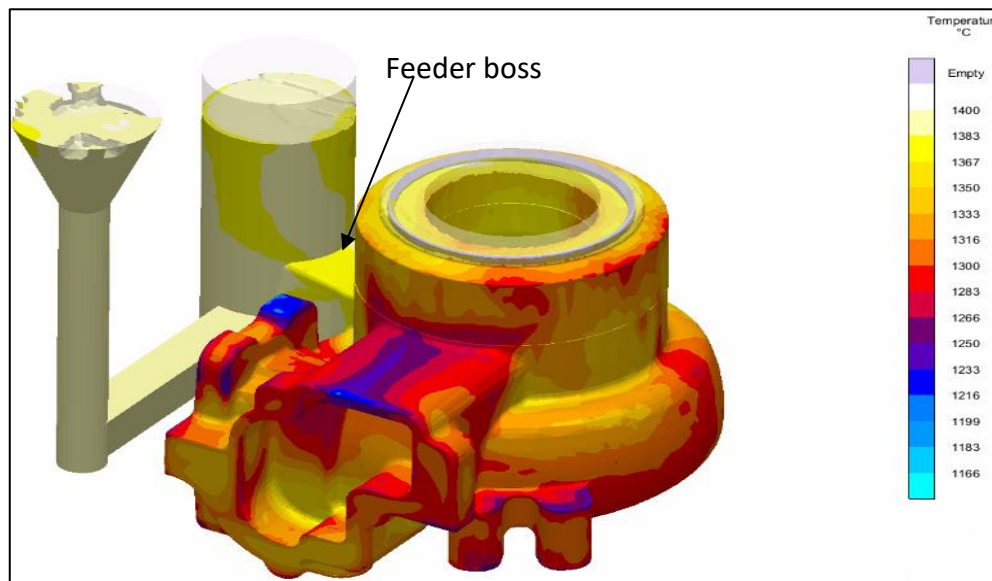


Figure 8.1 Temperature during the final stages of the filling of the casting ~ 6.8 seconds in to pouring.

As the casting continues to cool in the mould and the temperature passes below the liquidus of 1166°C the transition from liquid to solid begins and is complete as the temperature passes below 1160°C. The solidification of the casting via fraction liquid is shown in Figure 8.2 at various time intervals, with areas that are 100% solid shown as transparent. The simulation shows thin sections such as the divider wall cool rapidly in advance of thicker sections.

The maximum temperature within the casing was tracked throughout the in-mould cooling and after shakeout which occurred at 75 minutes and is shown in Figure 8.3. The curve shows that the temperature quickly drops from the 1400°C pouring temperature to below the liquidus temperature of 1166°C after which the temperature stabilises for a short duration as the transition from the liquid to solid phase begins. The simulation then allowed for cooling to an ambient air temperature of 25°C which took ~2.6 hours.

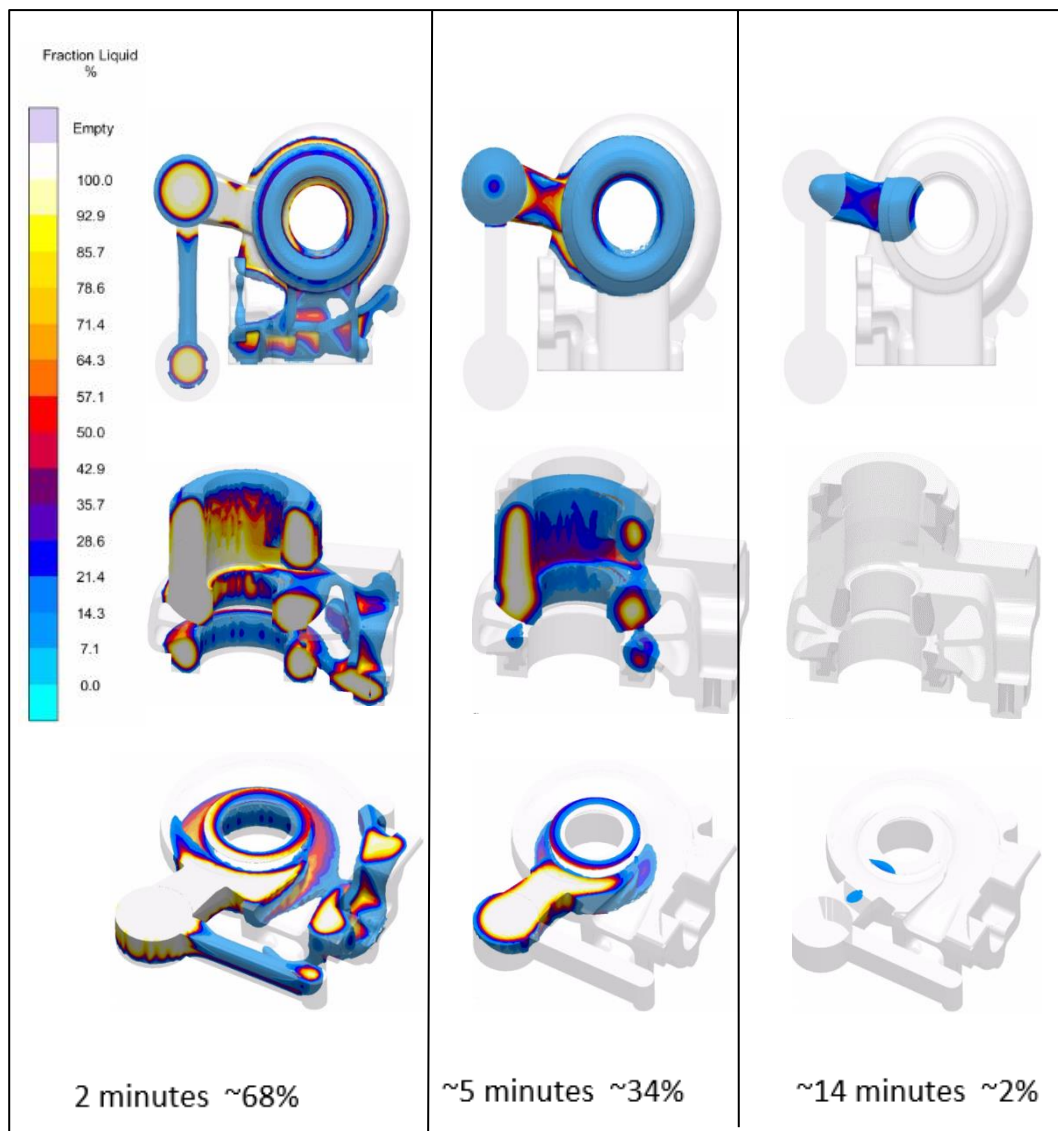


Figure 8.2 The solidification of the casting by liquid fraction from various viewpoints and time intervals from the start of the pouring sequence, the % of liquid fraction remaining is detailed, 100% solid is shown as transparent.

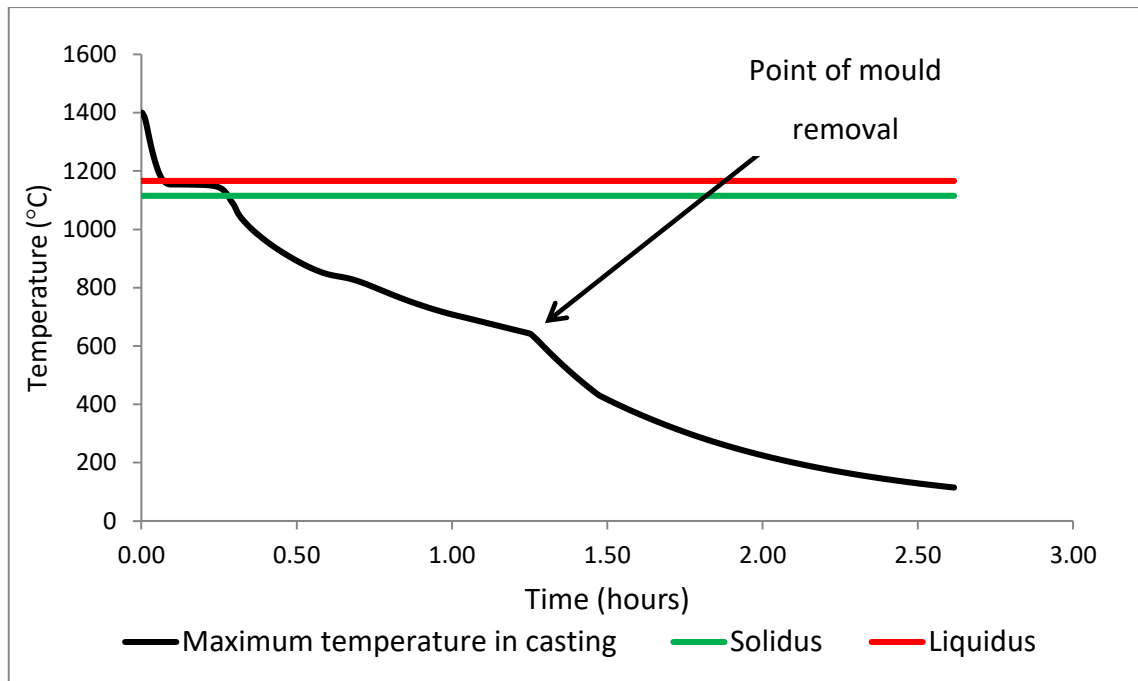


Figure 8.3 Maximum temperature cooling curve for the turbine housing casting and gating system. The mould was removed after 75 minutes.

The maximum principal stress approximately two minutes after the beginning of the pouring sequence is given in Figure 8.4. At this stage with reference to Figure 8.2 the thin section of the divider wall has already solidified and is attempting to contract but the constraint of the tongue and the surrounding thicker sections prevents it from doing so, inducing slightly tensile behaviour. At the point of mould removal (shakeout) the exterior of the casting is exposed to ambient air temperature this induces tensile stress on the outer surface of volute with magnitudes up to $\sim 140\text{MPa}$ as detailed in Figure 8.5.

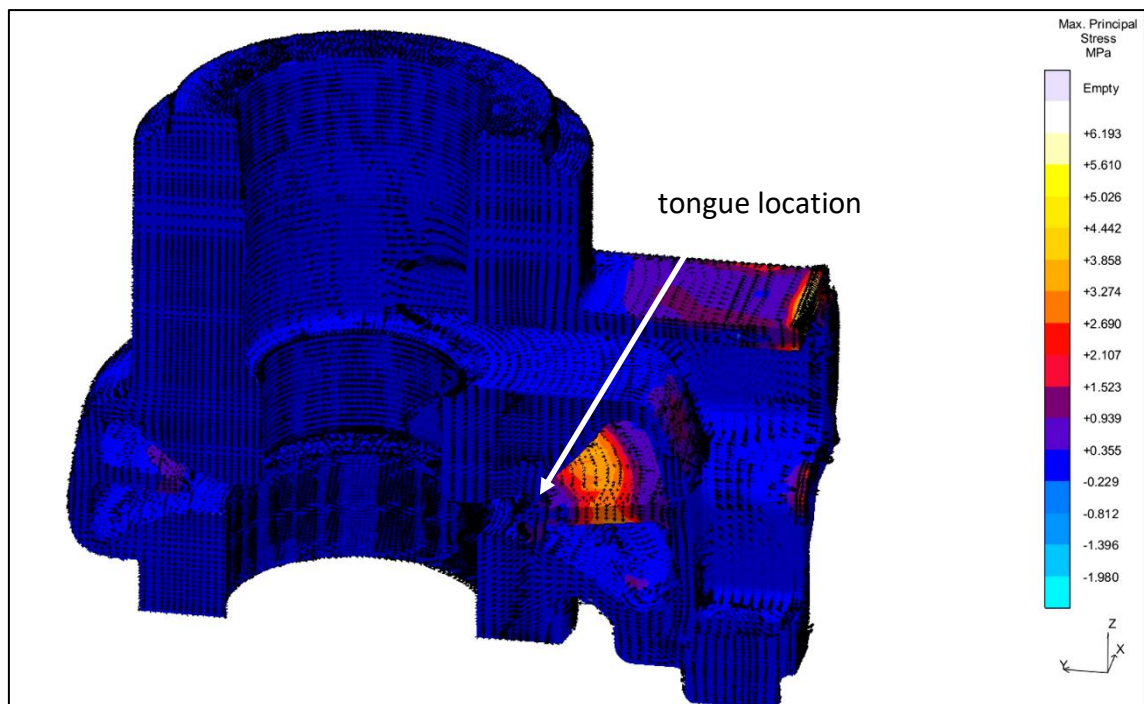


Figure 8.4 Maximum principal stress ~2 minutes after the initiation of the pouring sequence.

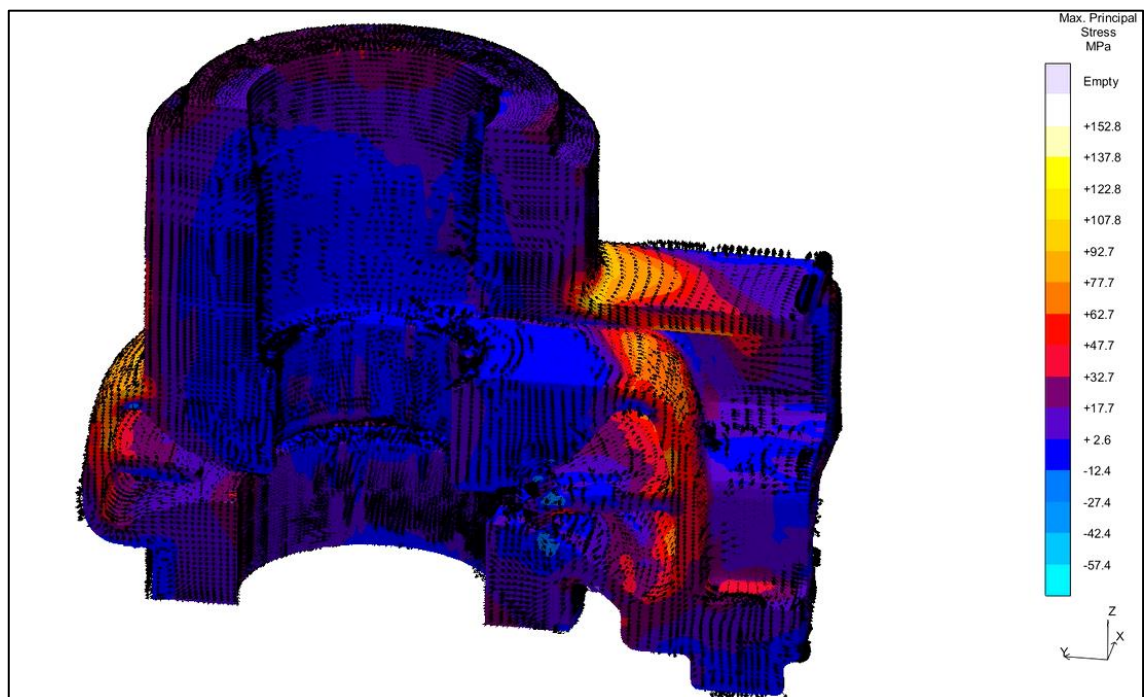


Figure 8.5 Maximum principal stress at the point of mould removal (shakeout).

8.1.2. Removal of feeder and gating system

The distribution of the maximum principal stress at the end of the solidification and shakeout stage, after cooling of the casting to ambient temperature is shown in Figure 8.6a and is Figure 8.6b after the removal of the feeder and gating system. The von-Mises stress distribution for the same processing step is given in Figure 8.7.

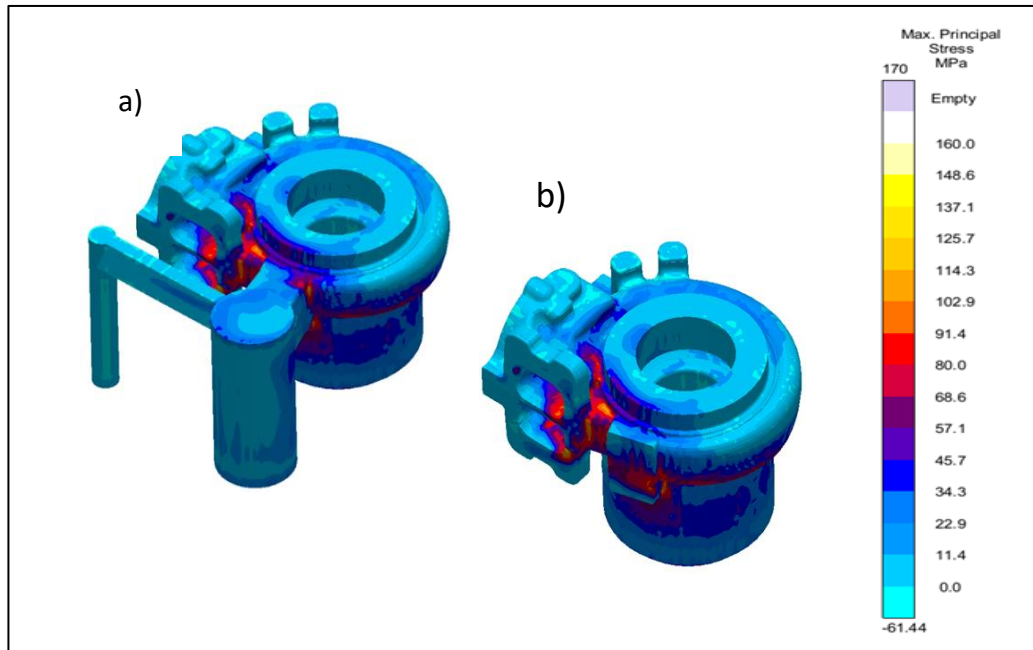


Figure 8.6 Exterior view of the maximum principal stress distribution a) after casting and b) after removal of the gating system.

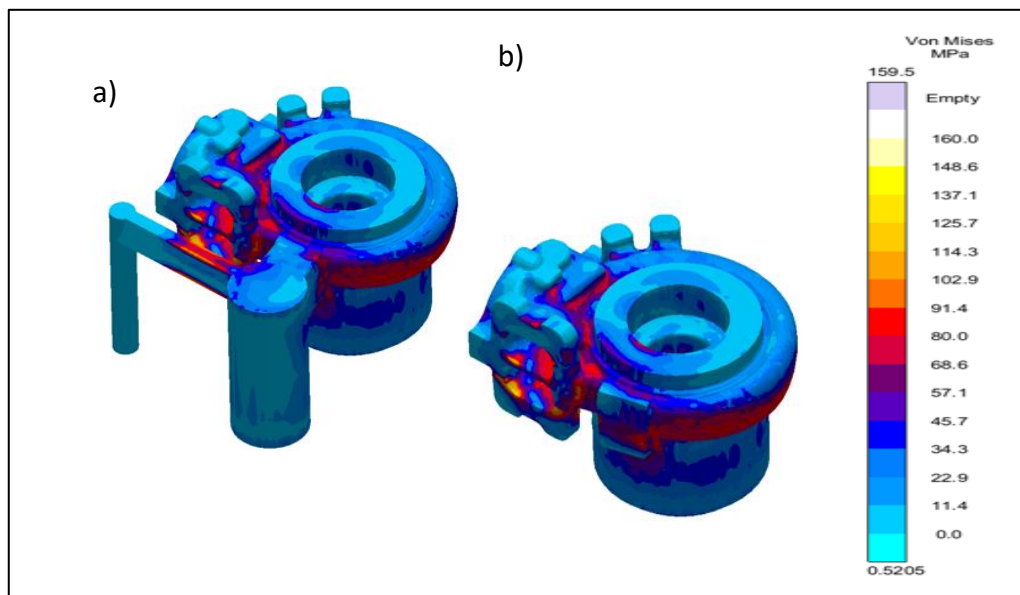


Figure 8.7 Exterior view of the von Mises stress distribution a) after casting and b) after removal of the gating system in the turbine housing.

A localised reduction in maximum principal stresses from a maximum of 95MPa to ~ 15 MPa was observed on the turbine housing close to the feeding location, as the feeder and gating system was removed. Within the interior of the housing no effect was observed on the stress distribution with the removal of the feeding system as is evident in Figure 8.8.

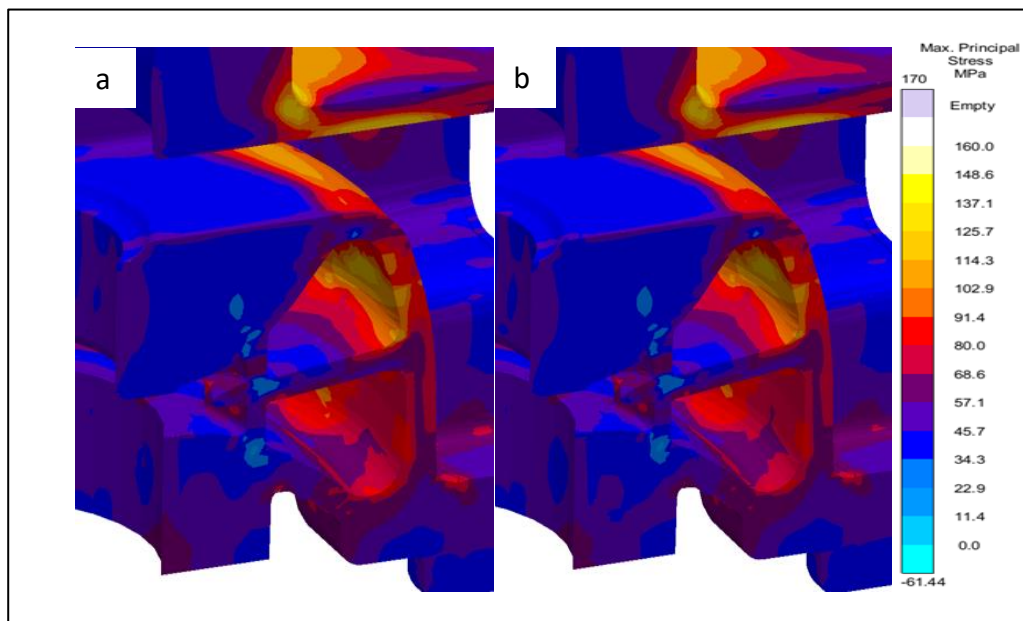


Figure 8.8 Internal view maximum stress distribution in the tongue location a) after casting b) after removal of the gating system.

8.1.3. Heat treatment

The simulated results for the maximum principal stress throughout the annealing heat treatment cycles are given in Figure 8.9 the heat treatment is aligned with the process as detailed in section 7.1.4.3. Prior to heat treatment the maximum tensile principal stresses recorded in the casting were in the region of 140MPa, after heat treatment an almost complete relaxation occurs throughout the bulk of the housing to levels of ± 10 MPa with localised areas in the tongue region showing slightly larger magnitudes. Residual stresses in the divider wall location prior to the heat treatment were primarily compressive with a magnitude ranging between ± 15 -40MPa.

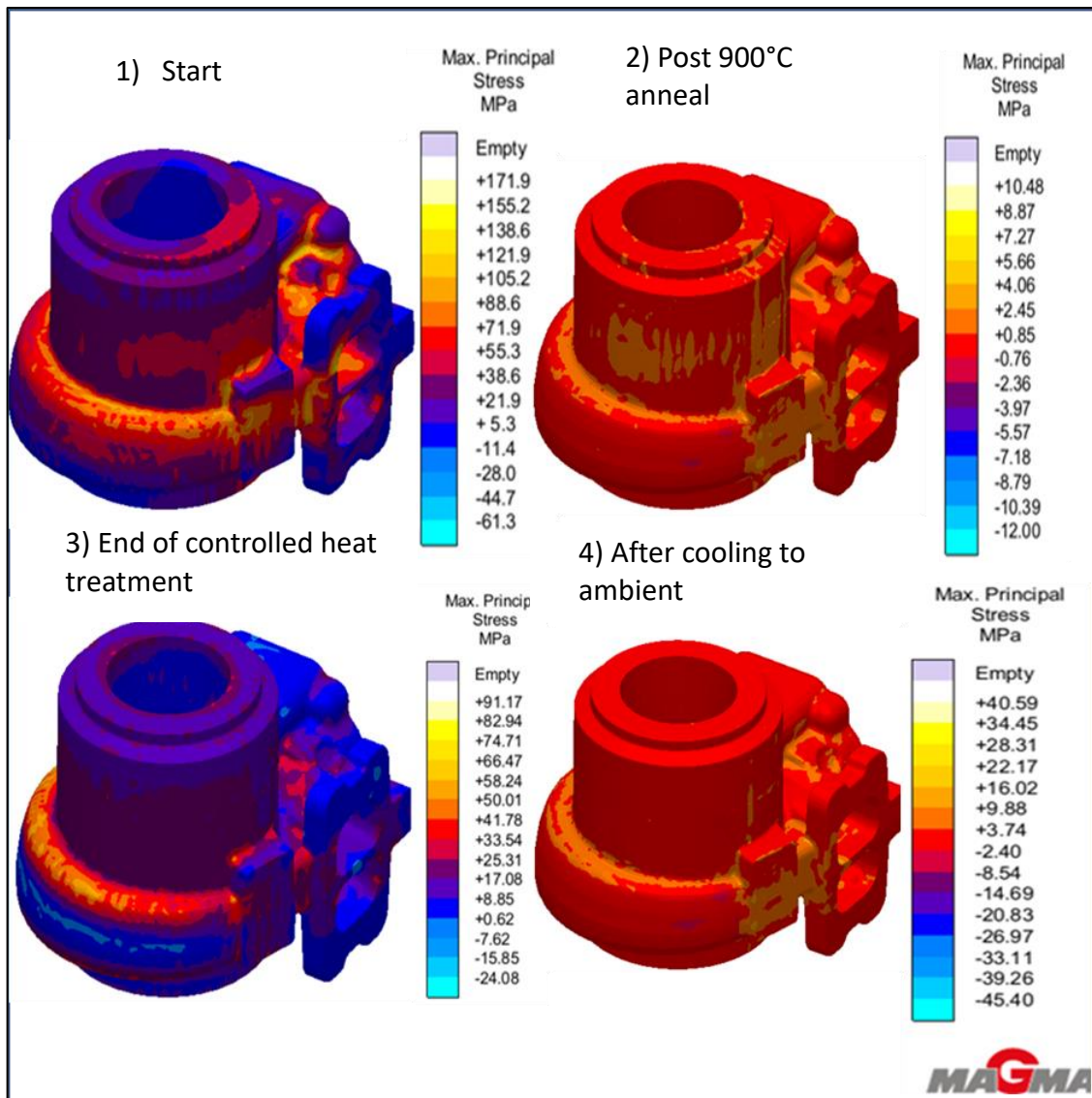


Figure 8.9 Maximum principal stress distribution during the heat treatment cycle.

Within the housing at position 9 in the tongue location the maximum principal and von Mises stress distributions are detailed in Figure 8.10 and Figure 8.11.

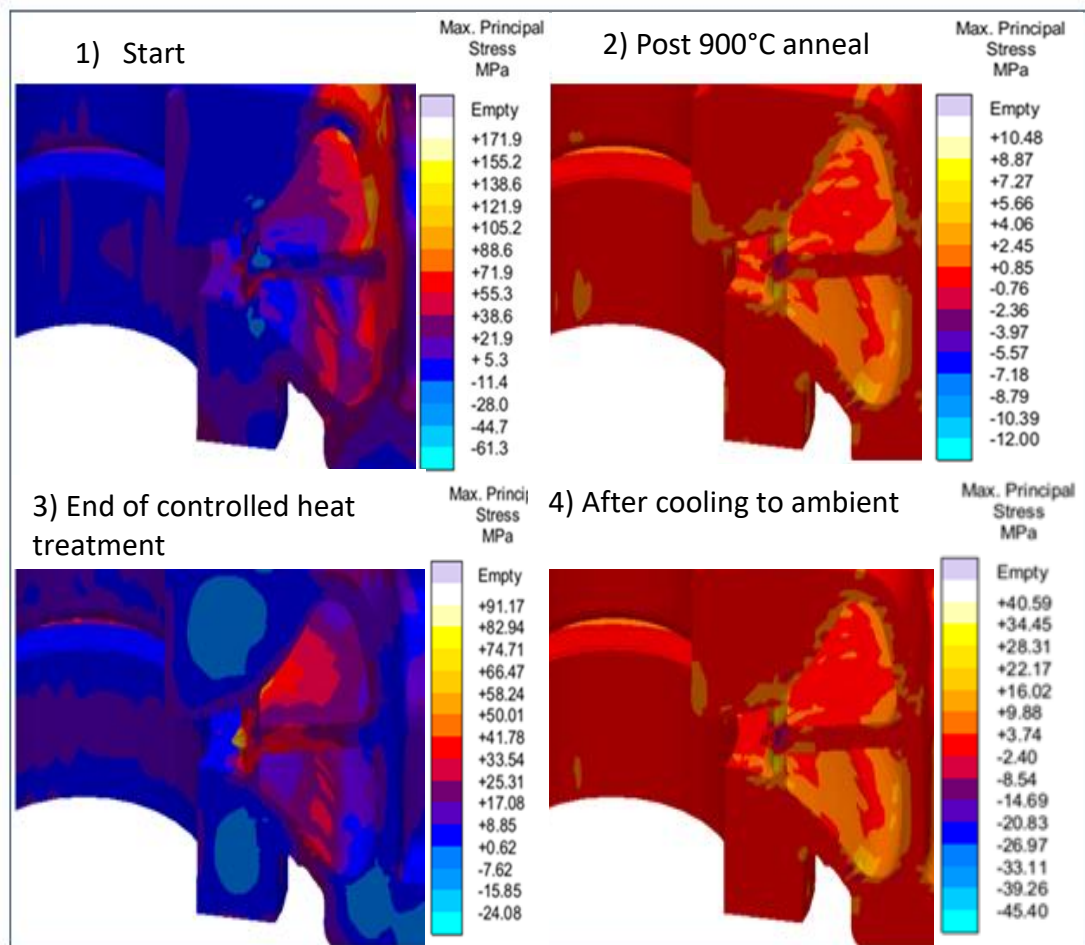


Figure 8.10 Simulated residual maximum principal stress distribution during the heat treatment cycle in the tongue/ divider wall region (position 9).

A point within the tongue location was selected and the change in stress throughout the simulated production process was tracked alongside temperature as is shown in Figure 8.12.

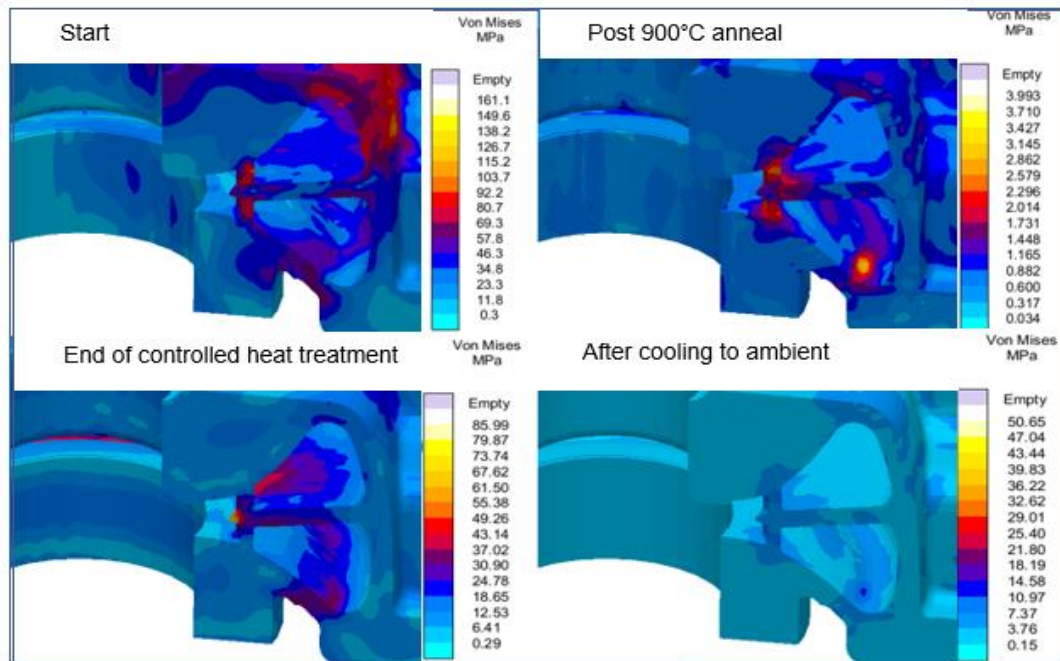


Figure 8.11 Simulated residual von Mises stress distribution over the course of the heat treatment cycle in the tongue/divider wall region (position 9).

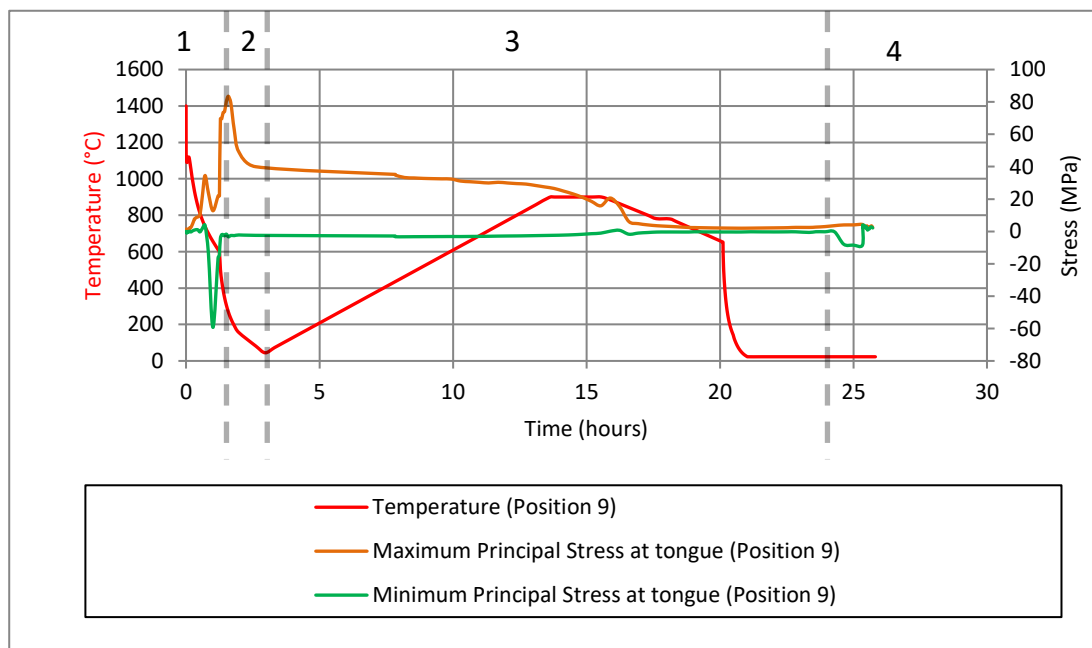


Figure 8.12 Plot of maximum and minimum principal stresses against temperature over the course of the entire manufacturing process, from a point in the divider wall close to the tongue at position 9. (1) Solidification and in-mould cooling, (2) mould removal and cooling to ambient, (3) heat treatment and (4) machining.

Figure 8.12 clearly shows that the shake out point of the production process introduces residual stress to the casting.

8.1.4. Machining

The machining was found to have little impact on the stress distribution and marginally increased maximum principal stresses by 5-10MPa locally on the machined faces. The maximum principal stress is compared between the casting and machined housing in Figure 8.13 and the von Mises stress is compared in Figure 8.14.

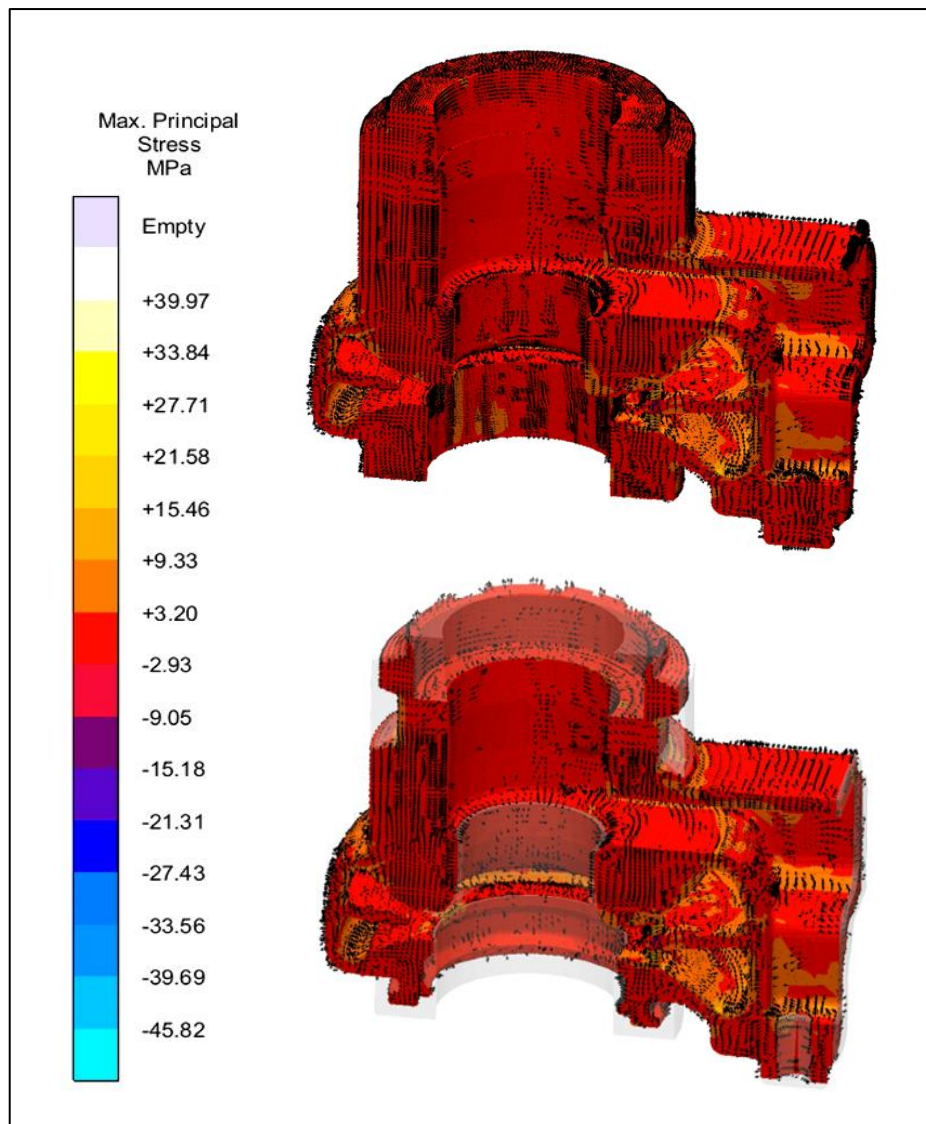


Figure 8.13 Maximum principal stress distribution before (top) and after (bottom).

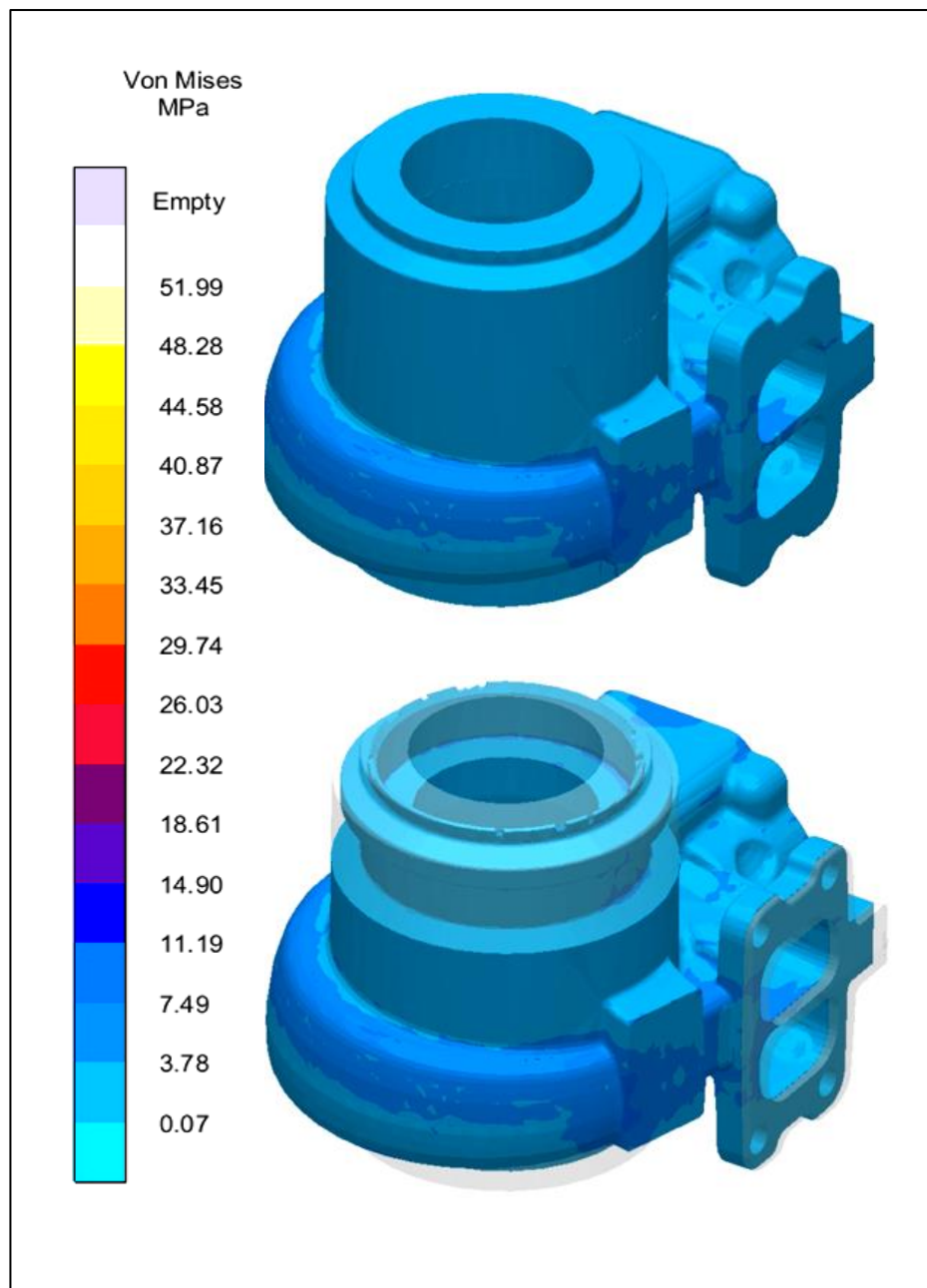


Figure 8.14 von Mises stress distribution before (top) and after (bottom) machining.

8.1.5. Residual stresses at the end of the production process

The final stress state at the end of manufacturing process across the horizontal 3-9 plane is shown in Figure 8.15. Simulated stresses are low in magnitude, with both compressive and tensile stress not exceeding $\pm 11\text{MPa}$ throughout the bulk of the housing. The 6-12 plane displayed the same result with no obvious trends in the distribution of stress. The tongue location however experiences higher magnitudes of both compression and tension, this is evident in the contour plots of maximum and minimum principal stress distributions as shown in Figure 8.16 and Figure 8.17, respectively.

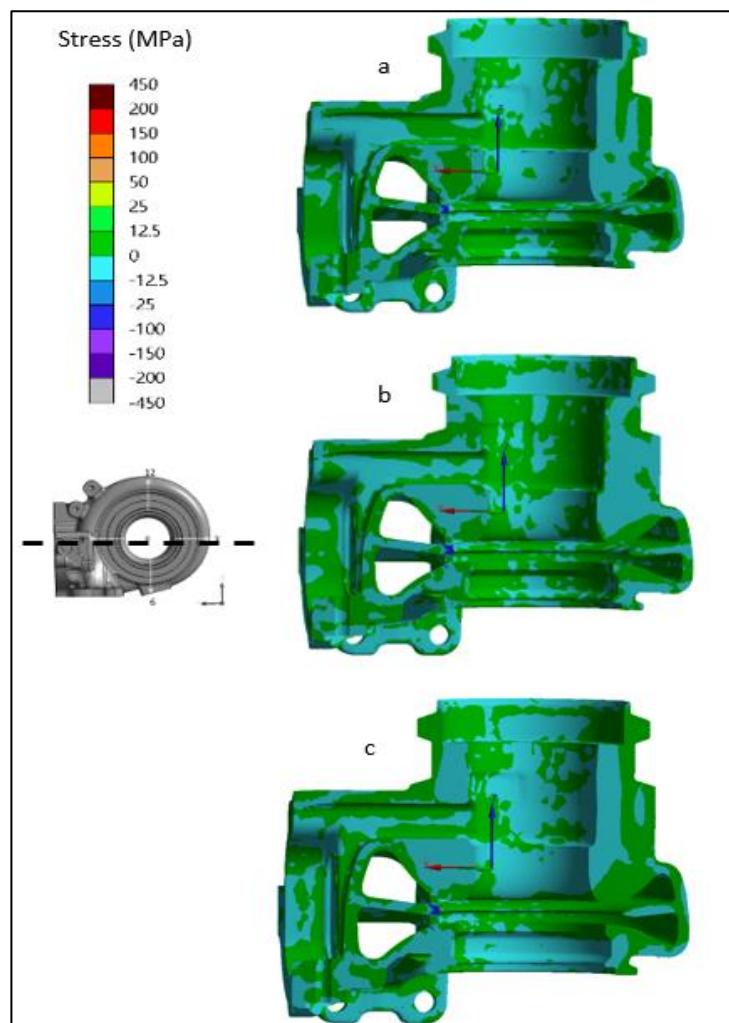


Figure 8.15 Stress distribution across the position 3-9 plane in a) axial b) radial and c) hoop directions.

A maximum tensile stress of $\sim 60\text{MPa}$ and a minimum compressive stress of $\sim 73\text{Pa}$ occur in the region of the tongue.

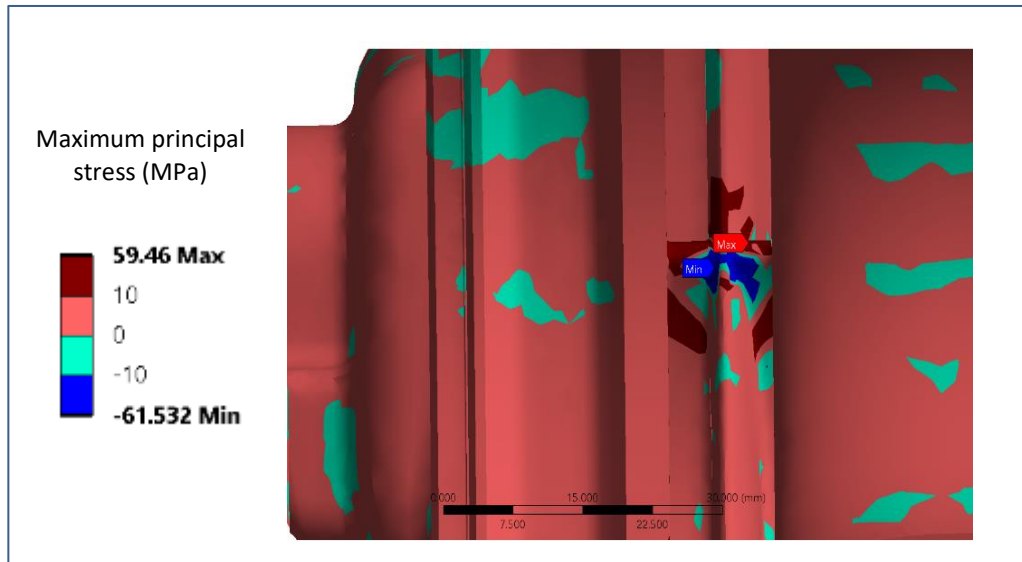


Figure 8.16 Simulated maximum principal stress in the tongue location at the end of the manufacturing process.

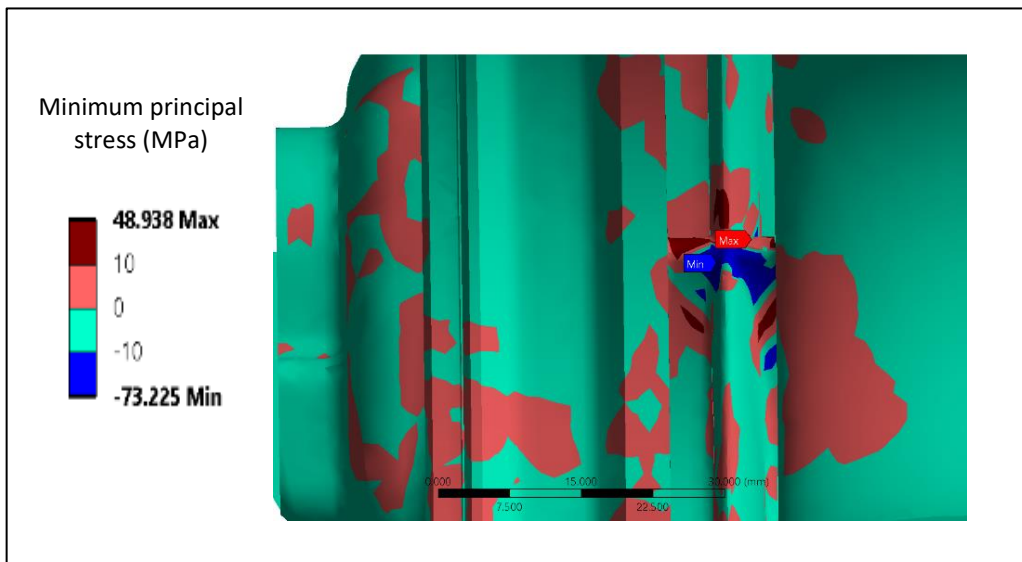


Figure 8.17 Simulated minimum principal stress in the tongue location at the end of the manufacturing process.

8.2. Thermomechanical fatigue simulation

8.2.1. Temperature

The global maximum and minimum temperatures within the turbine housing simulated over the course of the thermal cycle are shown in Figure 8.18. The maximum simulated temperature was 744.12°C in the divider wall region and occurred 550 seconds after the heat up phase commenced. The maximum metal temperature does not reach the peak gas temperature of 760°C due to heat loss through both natural convection and radiation. The minimum temperature, excluding the initial heating up from ambient was 109.81°C and occurred at the end of the cool down period on the bearing housing flange connection face.

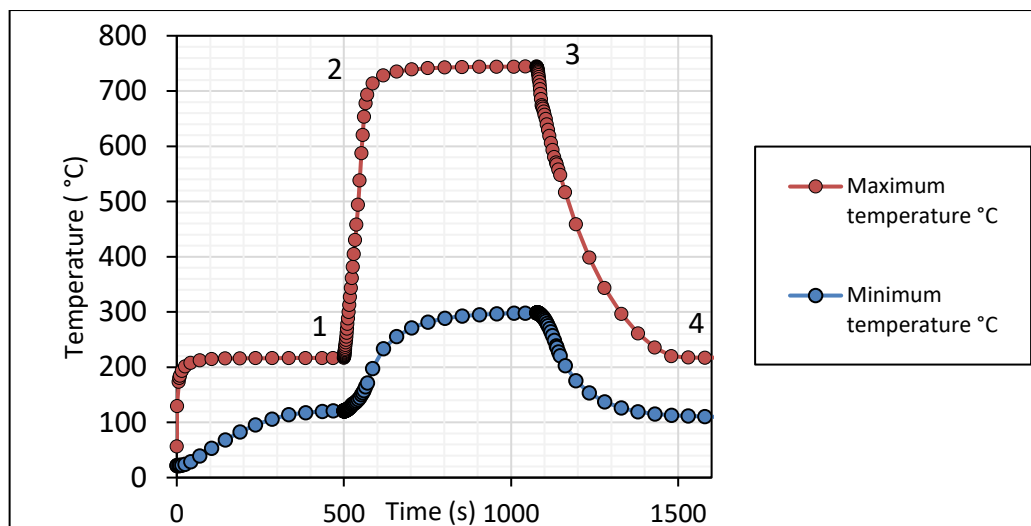


Figure 8.18 Simulated maximum and minimum temperatures in the turbine housing over the simulated thermal cycle with the (1) pre-heat up (2) post-heat up, (3) post soak and (4) post cool down periods identified.

Figure 8.19 and Figure 8.20 detail the simulated temperature distribution throughout various stages of the thermal cycle and correspond to the numbered points as shown in Figure 8.18. Clearly there is a temperature differential that exists across the housing, in particular the divider wall region due to its lower section thickness experiences higher temperatures than the surrounding thicker sections.

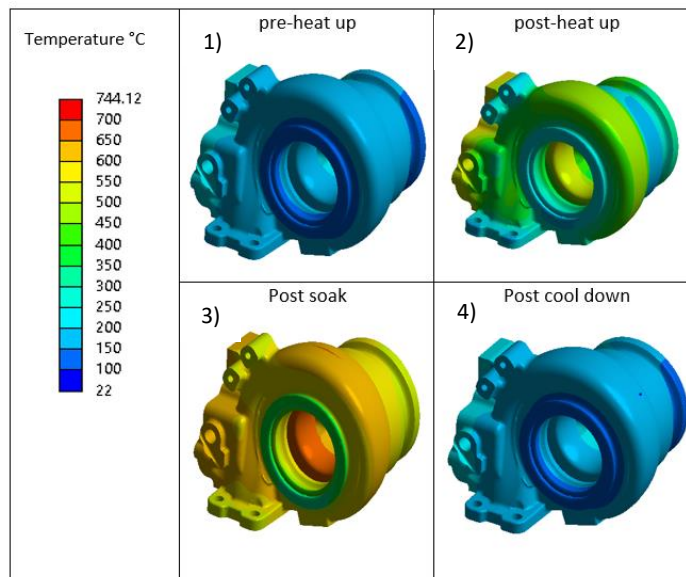


Figure 8.19 Simulated temperatures in the turbine housing at various stages in the thermal cycle as detailed in Figure 8.18, viewed from the bearing flange face.

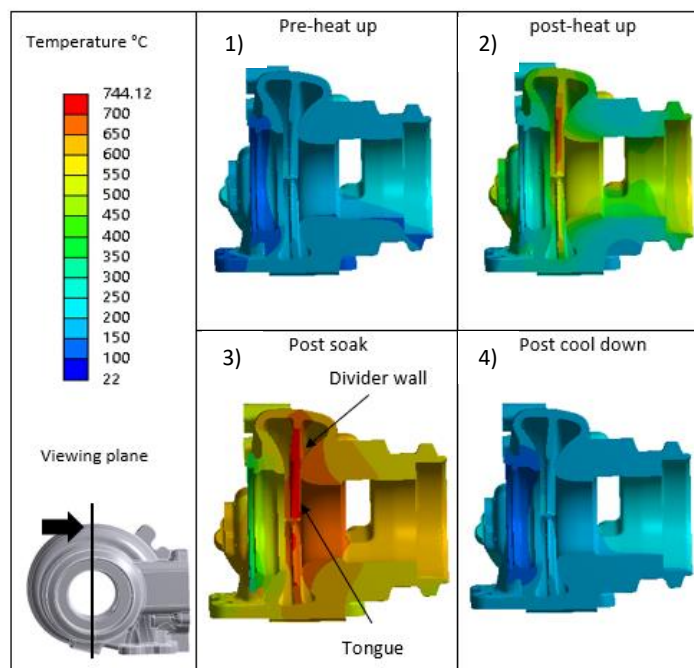


Figure 8.20 Cross-sectional view of the simulated temperature in the turbine housing at various stages of the thermal cycle as detailed in Figure 8.18.

8.2.2. Deformation during thermal cycling

The total deformation of the simulated assembly is shown in Figure 8.21. As detailed in section 7.2.5 to prevent rigid body motion displacement constraints were applied to the cylinder head block.

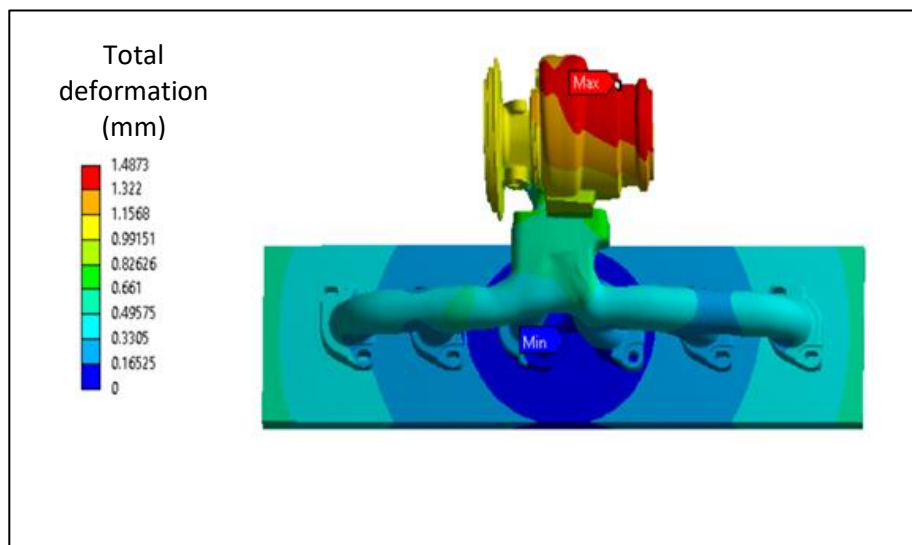


Figure 8.21 Total deformation during applied thermal load after the initial ramp up in temperature corresponding to point (2) as shown in Figure 8.18.

8.2.3. Stress/strain behaviour during thermal cycling

To visualise the stress distribution throughout the thermal cycle a vector plot of the maximum and minimum stresses is presented in Figure 8.22 showing the stress distribution during the heating up phase. Figure 8.23 details the stress distribution during the cooling down phase.

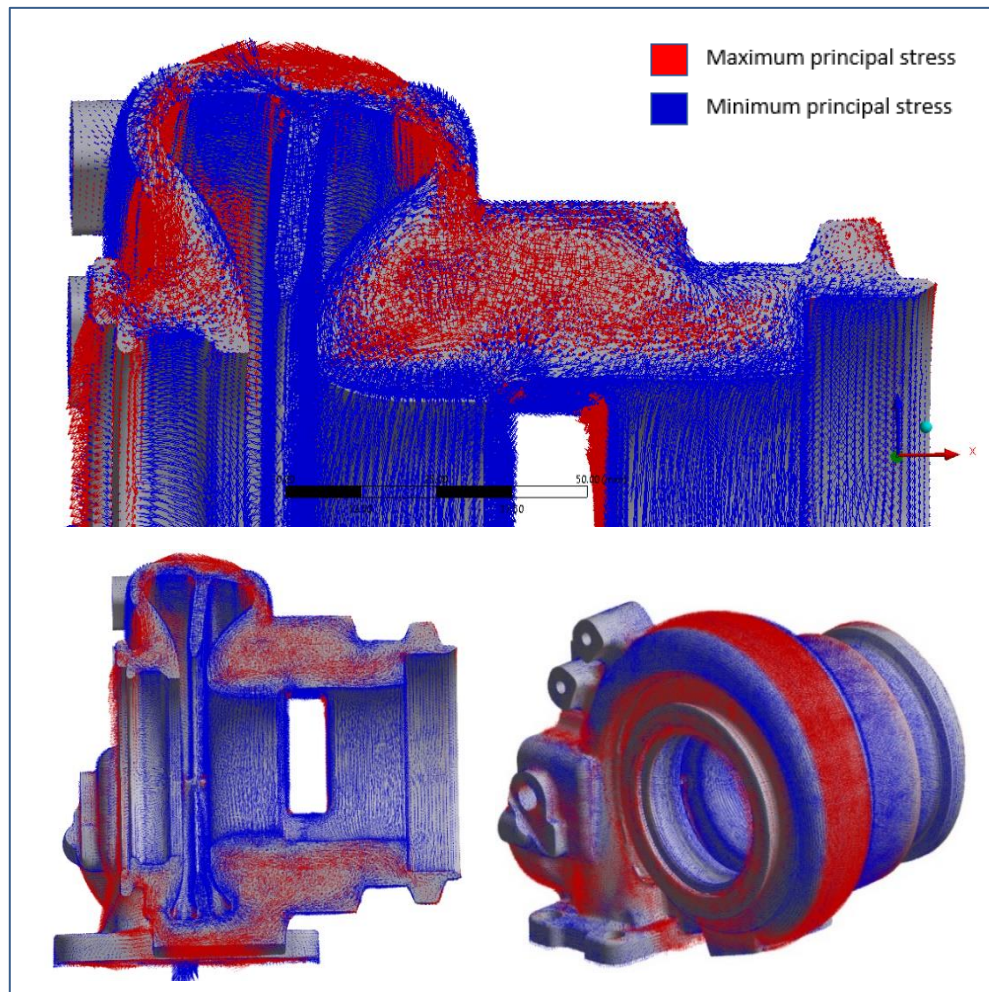


Figure 8.22 Vector plot of maximum (red) and minimum (blue) principal stresses during the heating phase of the thermal cycle corresponding to point (3) as shown in Figure 8.18.

During the heating phase the external surface of turbine housing volute wall is subjected to a combination of compressive and tensile stress. The tensile stresses are distributed within the middle of the external volute surface which corresponds to an interior compressive stress in the divider wall. During the cooling phase the opposite occurs with the external surface of the housing experiencing compression and the internal divider wall subjected to tension.

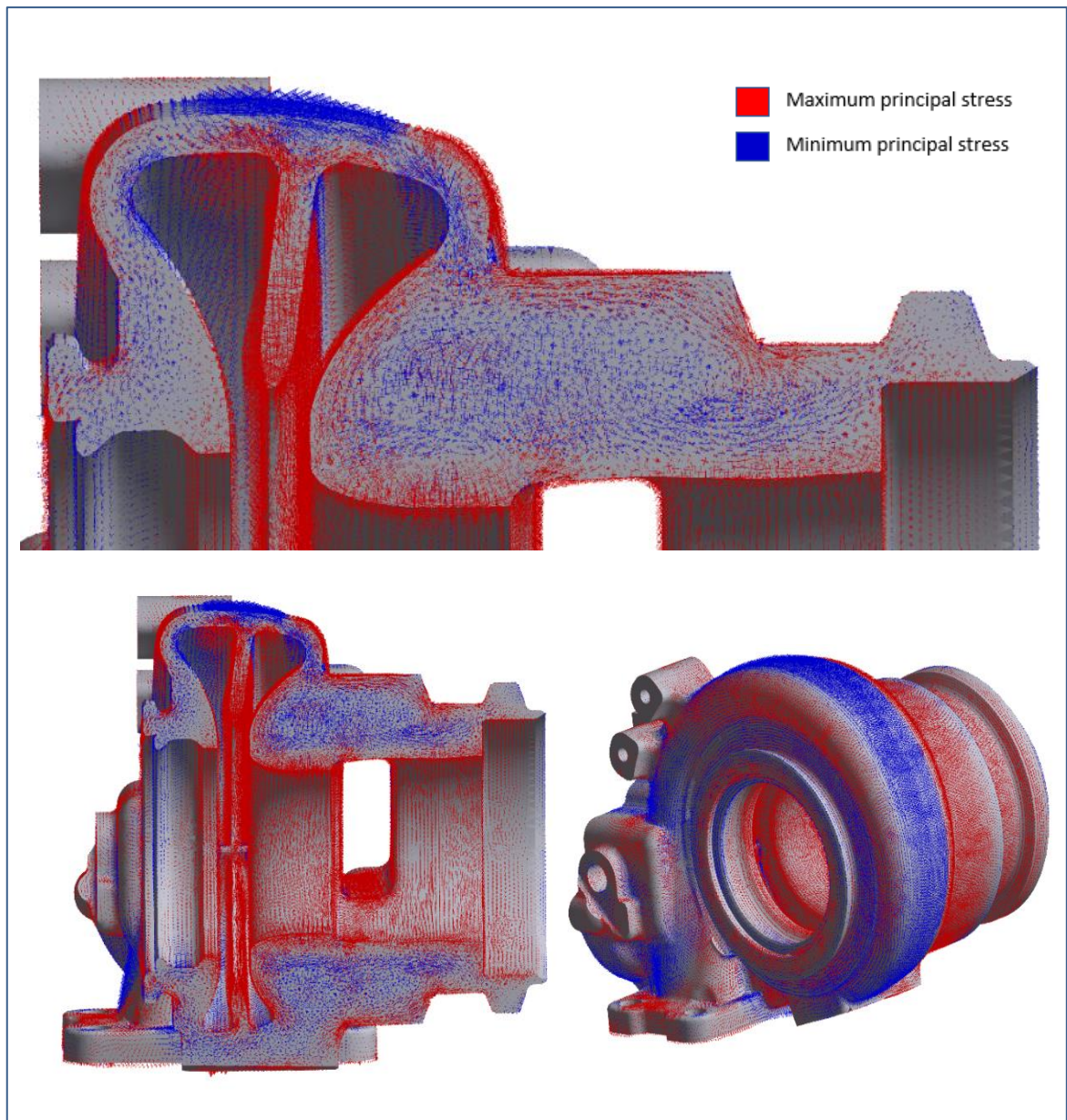


Figure 8.23 Vector plot of maximum (red) and minimum (blue) principal stresses during the cooling phase of the thermal cycle corresponding to point (4) as shown in Figure 8.18.

This behaviour results in localised yielding of the divider wall. The equivalent plastic strain is shown in Figure 8.24 at 70 seconds in to the ramp up in temperature at which point the divider wall experiences $\sim 700^{\circ}\text{C}$ in temperature

Figure 8.25 shows the equivalent plastic strain 60 seconds in to the ramp down in temperature. The maximum values of equivalent plastic strain occur in the tongue region.

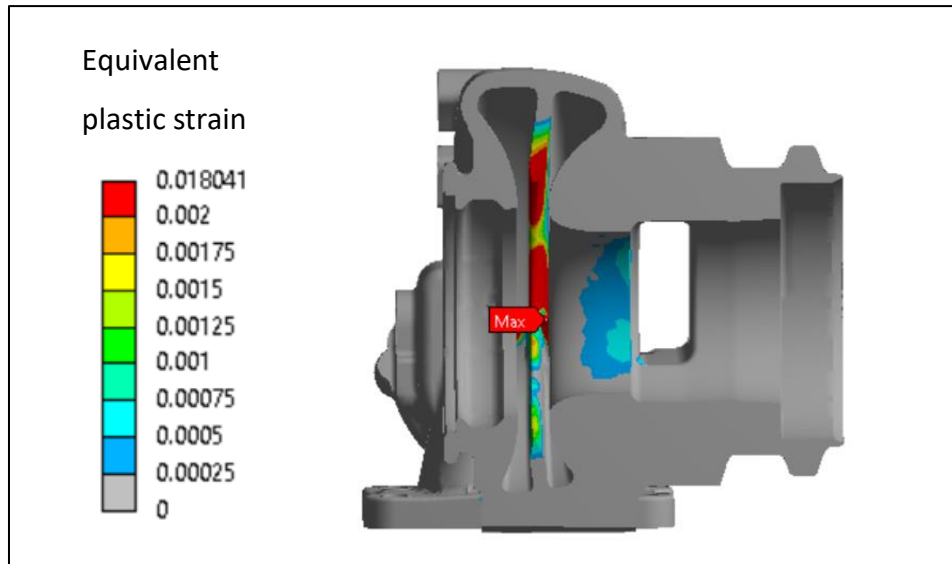


Figure 8.24 Equivalent plastic strain distribution in the tongue location 70 seconds into the ramp up in temperature.

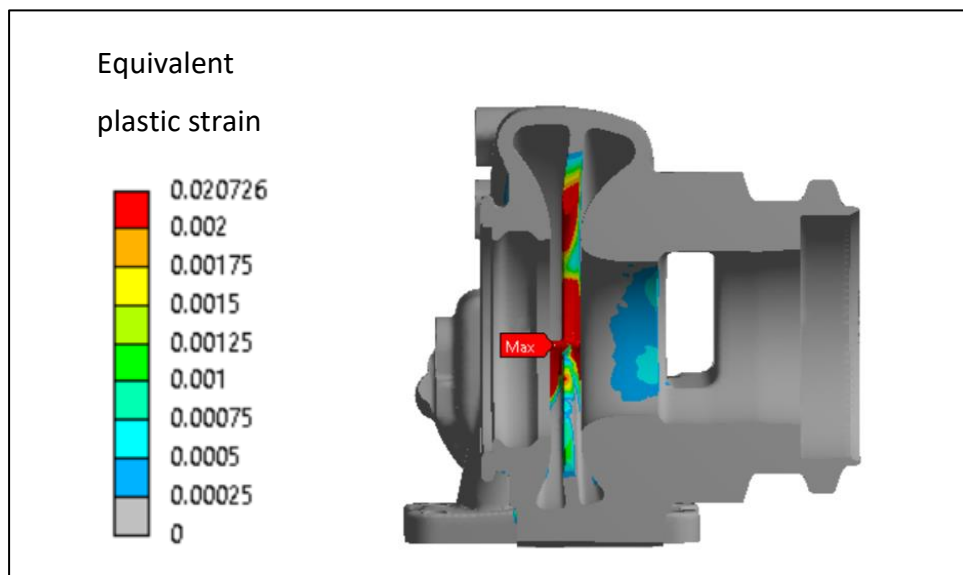


Figure 8.25 Equivalent plastic strain distribution in the tongue location 100 seconds into the ramp down in temperature.

The strain distribution during the heating up phase of the thermal cycle is shown in Figure 8.26 and during the cool down phase in Figure 8.27.

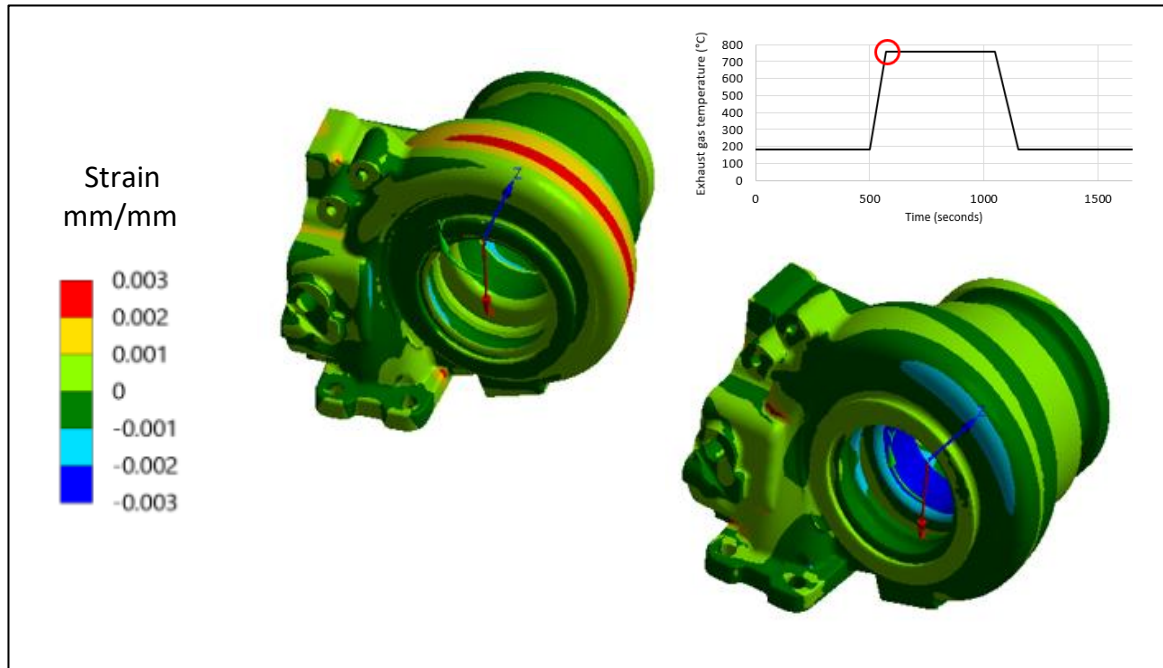


Figure 8.26 Axial strain (left) and hoop strain (right) distribution during the heat up phase of the thermal cycle.

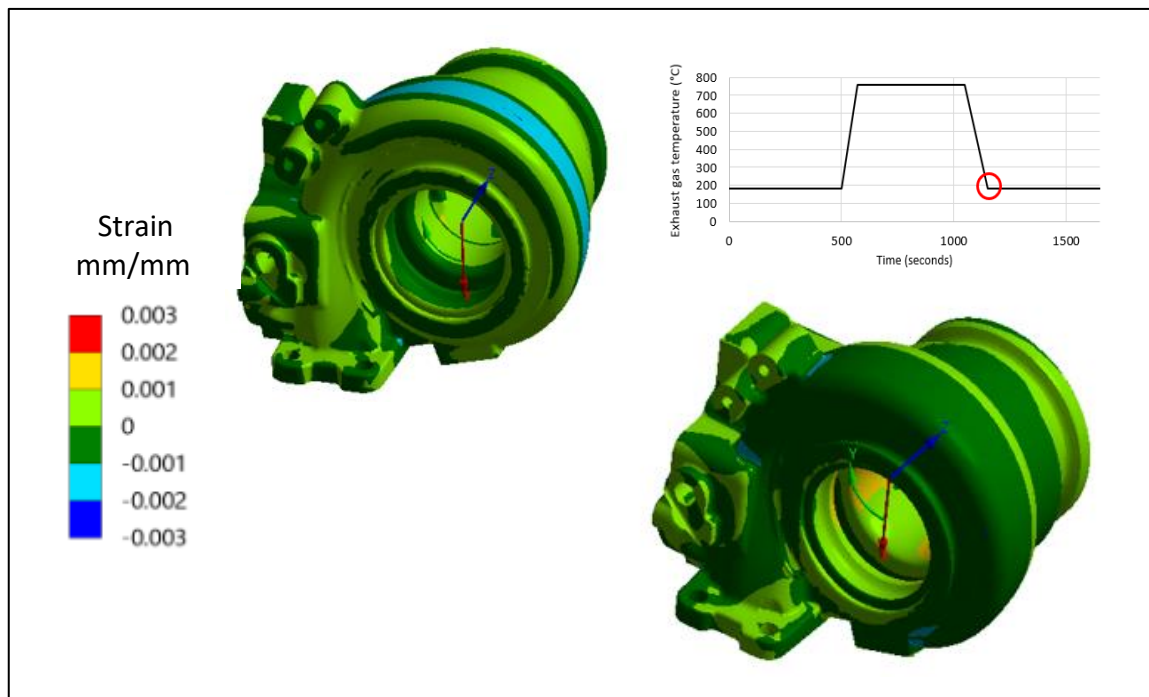


Figure 8.27 Axial strain (left) and hoop strain (right) distribution during the cool down phase of the thermal cycle.

The axial and hoop strain results have been extracted at the locations corresponding to the strain gauges and are given in Figure 8.28.

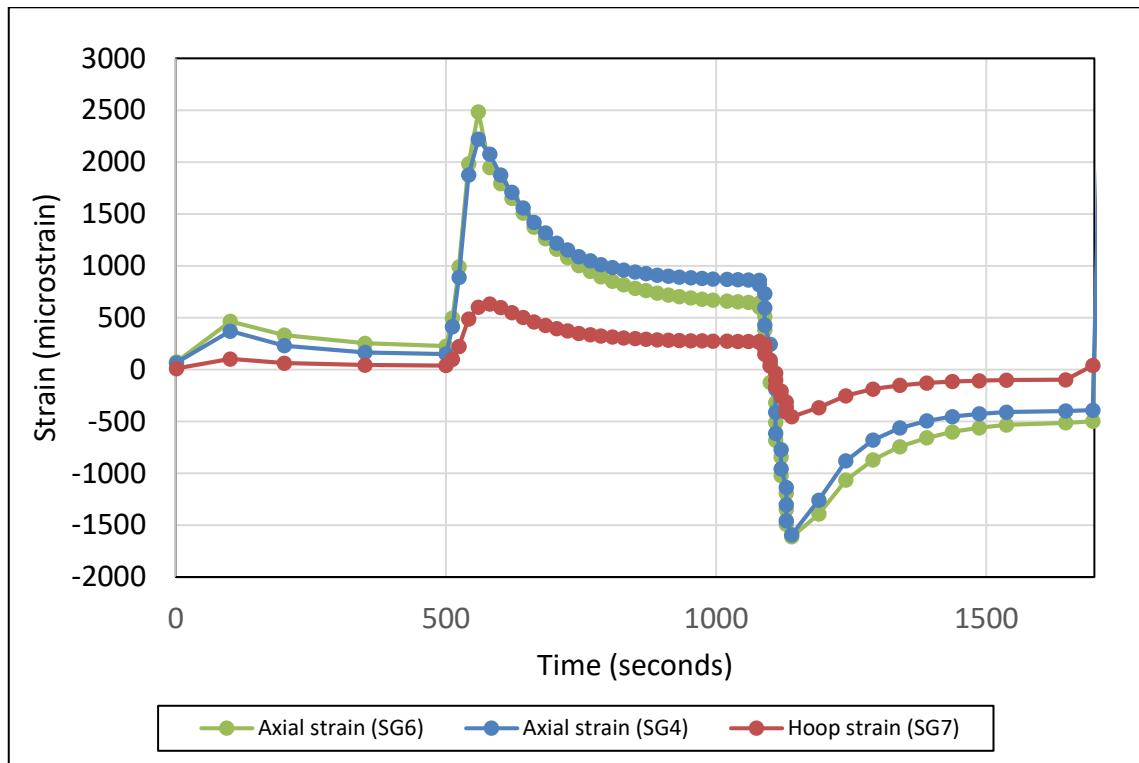


Figure 8.28 Strain measurements extracted from strain gauge locations

The extracted strain results show that the strain is in-phase with the applied temperature. Strain gauge 6 exhibits the highest levels of tensile strain at $\sim 2500\mu\epsilon$ with a similar level recorded for strain gauge 4. The strain gauge 7 (hoop) location shows levels just above $\pm 500\mu\epsilon$.

8.2.4. Residual stress distribution after thermal cycle

The residual stress distributions for the axial, radial and hoop stress components after a simulated thermal cycle are shown in Figure 8.29 for the position 3-9 plane and Figure 8.30 for the position 6-12 plane. The stress distribution on the exterior of the housing is given Figure 8.31.

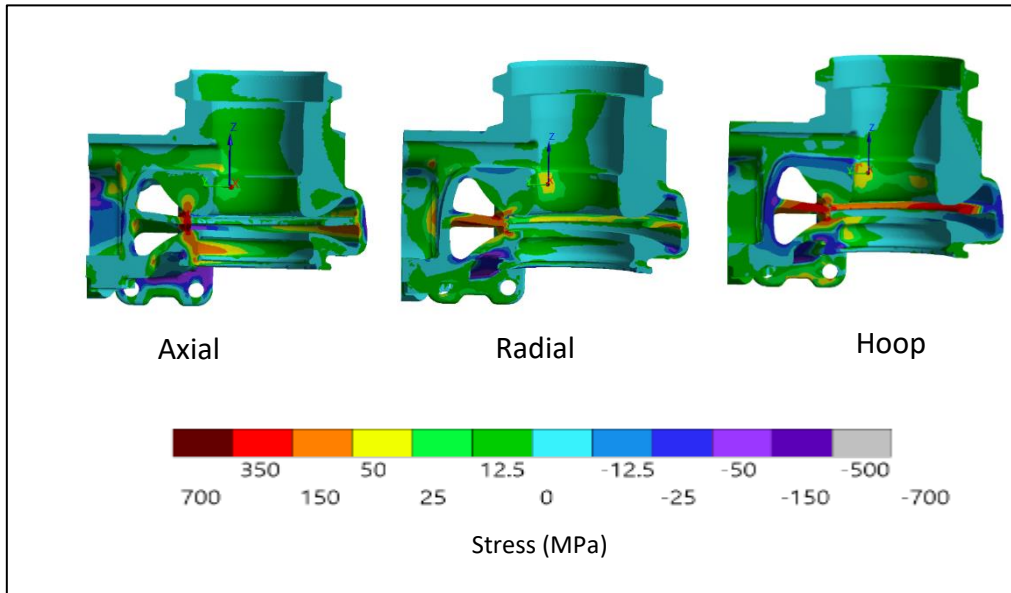


Figure 8.29 FEA contour plots of the residual stress in the three principal directions in the position 3-9 plane after thermal cycling of the turbine housing.

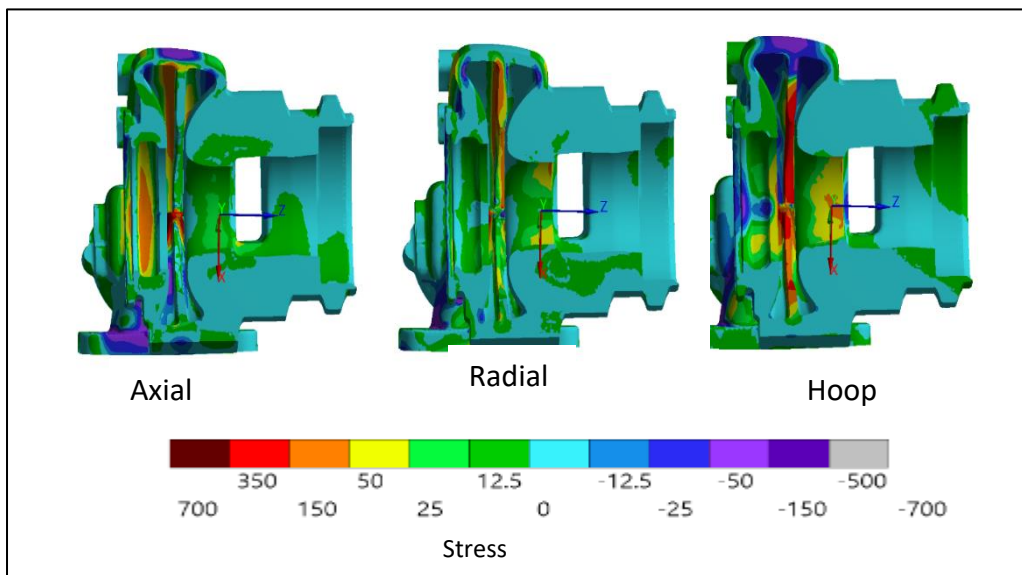


Figure 8.30 FEA contour plots of the residual stress in the three principal directions in the position 6-12 plane after thermal cycling of the turbine housing.

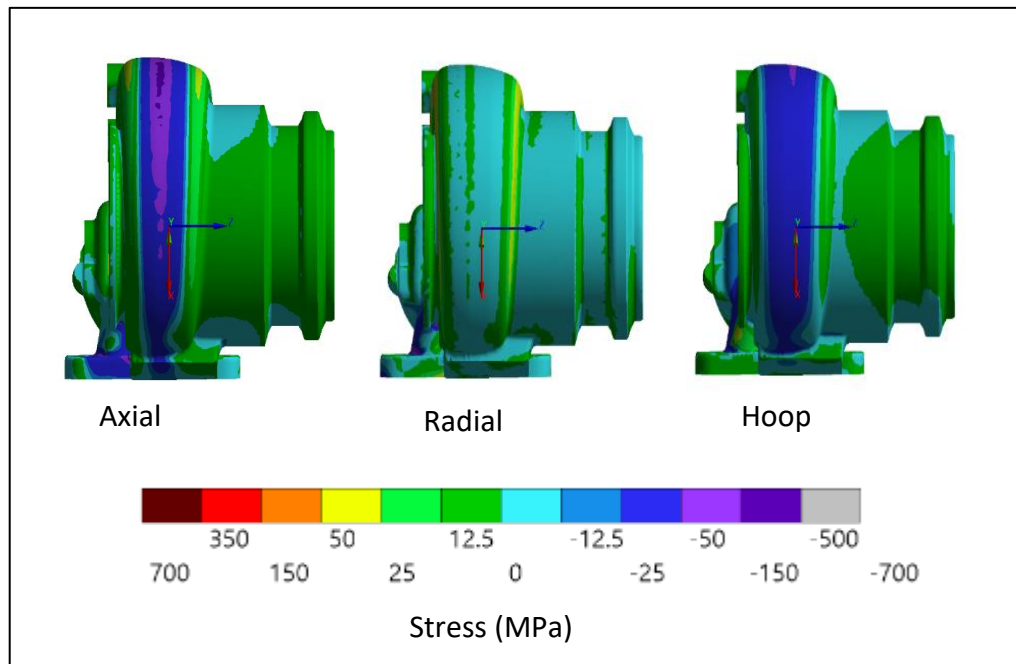


Figure 8.31 Residual stress distribution after a simulated thermal cycle a) axial stress b) radial stress and c) hoop stress.

External residual stresses were found to be compressive with the largest magnitudes observed in the axial direction, the internal divider wall was subjected to tensile stresses the largest of which occurred in the hoop direction at ~ 350 MPa.

9. Comparison of results and discussion

This chapter will compare the experimental measurements of both residual stress obtained before and after the turbine housing was subjected to a thermomechanical load, and applied strain obtained during thermal cycling against simulation models. A discussion as to the trends observed and the behaviour of stress/strain over the course of the turbine housings life time will also be provided.

9.1. Manufacturing induced residual stresses

MAGMA simulations considering the solidification sequence of the cast turbine housing indicated that rapid cooling takes place as the molten iron cools and solidifies. The temperature distribution throughout the casting was logical, with thin sections cooling and solidifying in advance of thicker sections. As the housing cools and solidifies a stabilisation in temperature occurs as the change from liquid to solid takes place. The onset of residual stress generation appears to take place rapidly and is low ($<\pm 10\text{MPa}$) in magnitude in the initial, in-mould cooling stages. At the point of shakeout, and exposure of the housing to ambient air cooling, tensile stress levels were observed to increase in magnitude around the outer volute wall, peak tensile stresses of $\sim 110\text{MPa}$ were observed, while the interior divider wall section was subjected to a similar level of compression with a combination of tensile and compressive stresses observed in the tongue region. The removal of the feeder and gating systems affected the local stress area only with a reduction in von-Mises stress of $\sim 50\text{MPa}$. Machining was included in the simulation for completeness using a simplified approach; during machining maximum principal tensile stresses were observed to increase slightly between $5\text{--}10\text{MPa}$ although an assessment of the von-Mises stress suggests overall a reduction in stress is expected locally on the machined faces. The simulation of the annealing heat treatment, suggests it is effective in relaxing residual stresses, with rapid relaxation of peak tensile stresses of 140MPa before heat treatment to levels less than $<15\text{MPa}$ within the bulk of the housing, after

heat treatment. Ultimately by the end of the simulation of the production process the residual stress levels were predominantly very low $<\pm 10\text{MPa}$ in both tension and compression. It was observed that a small region in the tongue, which is a highly constrained point of the casting, exhibited increased tensile stress in the region of 60MPa which was balanced somewhat by an area of increased compressive stress.

A comparison of the MAGMA simulations of the hoop stress distribution at the end of the manufacturing process against the experimental measurements obtained using the contour method (cut 3) is given in Figure 9.1. The residual stresses agree over most of the cross-section in that they are low in magnitude, with the contour results varying between approximately $\pm 30\text{MPa}$ within the bulk of the cross-section while the simulation results fall between $\pm 11\text{MPa}$. The contour method does however show higher magnitudes of stress around the perimeter of the cross-section. This is due to a larger uncertainty in the measurement which can be attributed to wire contact induced surface roughness (Olson, DeWald, Prime, & Hill, 2015).

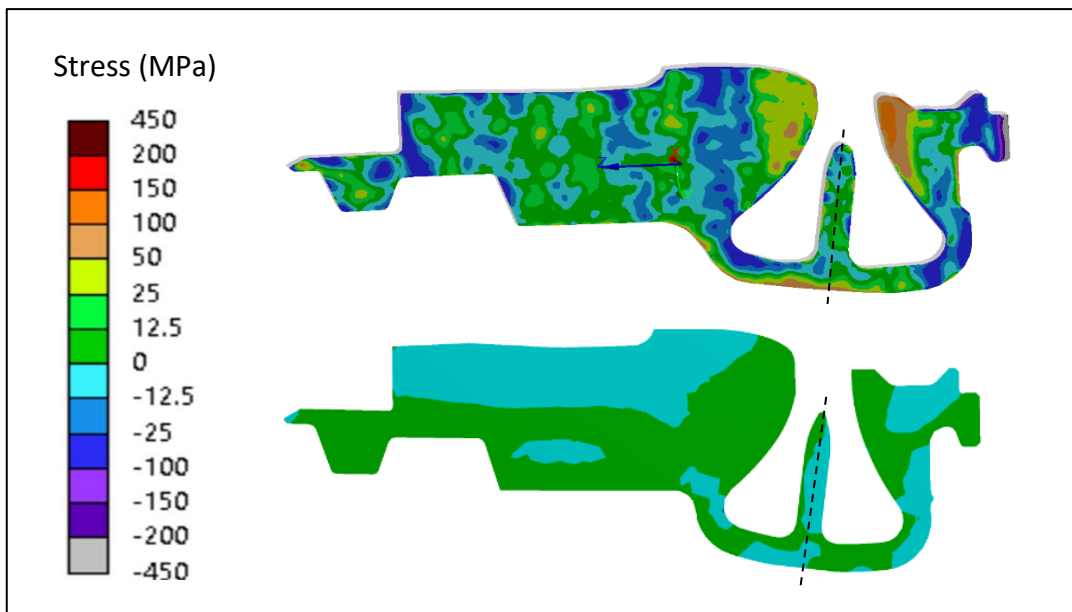


Figure 9.1 Comparison of the experimental measurement of hoop stress obtained using the contour method (top) and the result of the MAGMA simulation (bottom). Centre line used for comparison between the techniques is also shown.

The contour method proved particularly challenging to apply to the turbine housing. During the cutting process the surface quality was adversely affected. Additional steps were necessary to improve the accuracy of the result which unfortunately reduced the measurement area. The steps identified to improve the uncertainty in the measurement, which eventually required the use of a sacrificial layer, will assist in future measurements on complex geometries. The results from the centre line of the divider wall have been extracted for both the contour measurement and simulation and are compared in Figure 9.2 alongside the neutron diffraction measurement from position 3. As detailed in section 5.3.2, the final cut (cut 3) was obtained from a slightly different position compared to the neutron line scan, however this is unlikely to make a large difference in the results.

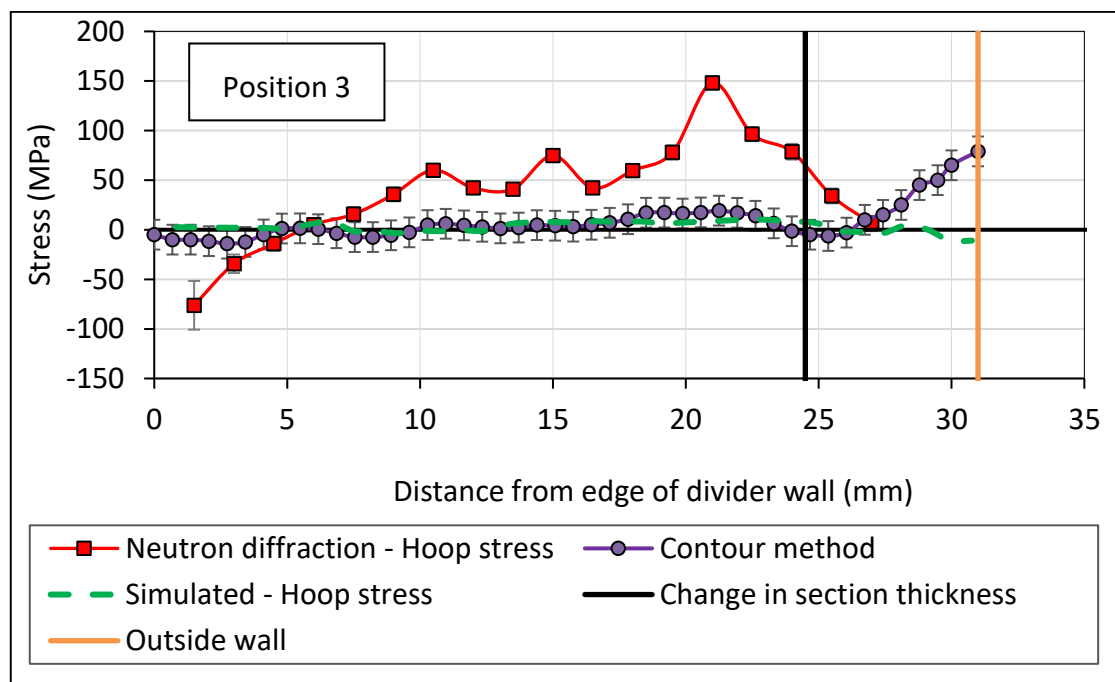


Figure 9.2 Comparison of the hoop stress component measurements using the contour method and neutron diffraction against simulated results in position 3.

Comparing the three profiles, the contour and simulation results show good correlation, with most of the simulated points falling within the error bars of the

contour measurement. The neutron diffraction line scan however shows larger magnitudes of stress reaching $150\text{MPa} \pm 10\text{MPa}$. The contour measurement and neutron diffraction data show a similar trend to begin with, starting in compression at the edge of the divider wall. There does appear to be an increase in tension in the contour measurement data around the 20mm point, in agreement with the neutron diffraction line scan, there is also a slight increase in tension in the simulated values at this point.

A comparison between each of the neutron diffraction line scans obtained within the divider wall in positions 3,6,9 and 12 is given against the simulated results in Figure 9.3 to 9.6.

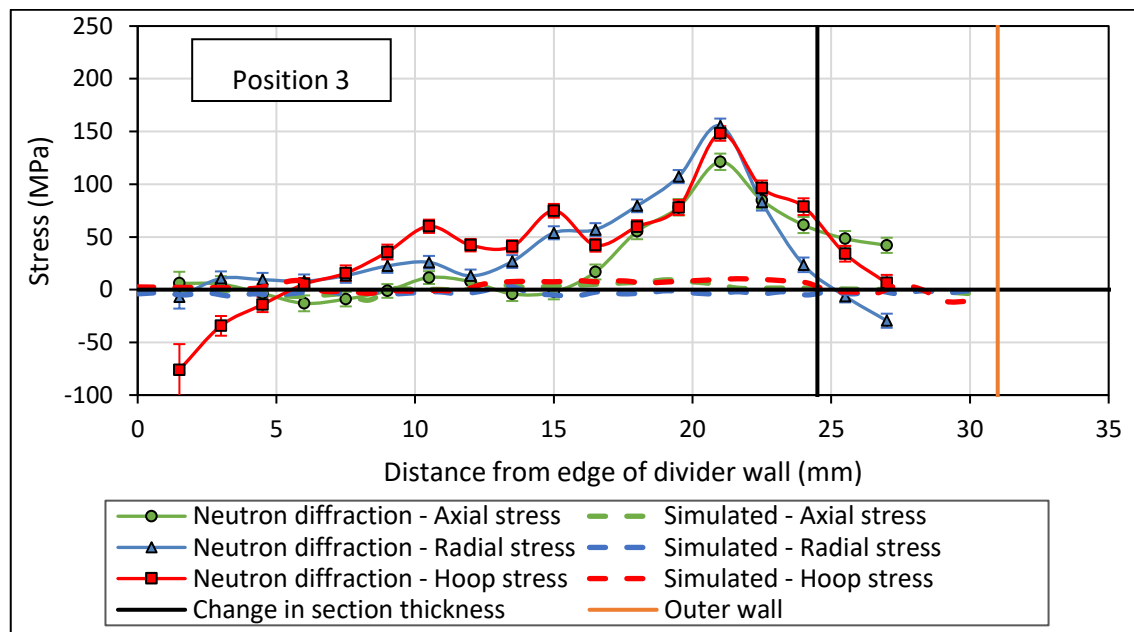


Figure 9.3 A comparison of neutron diffraction and simulated results along the centre line of position 3. Considering the residual stress state at the end of the manufacturing process.

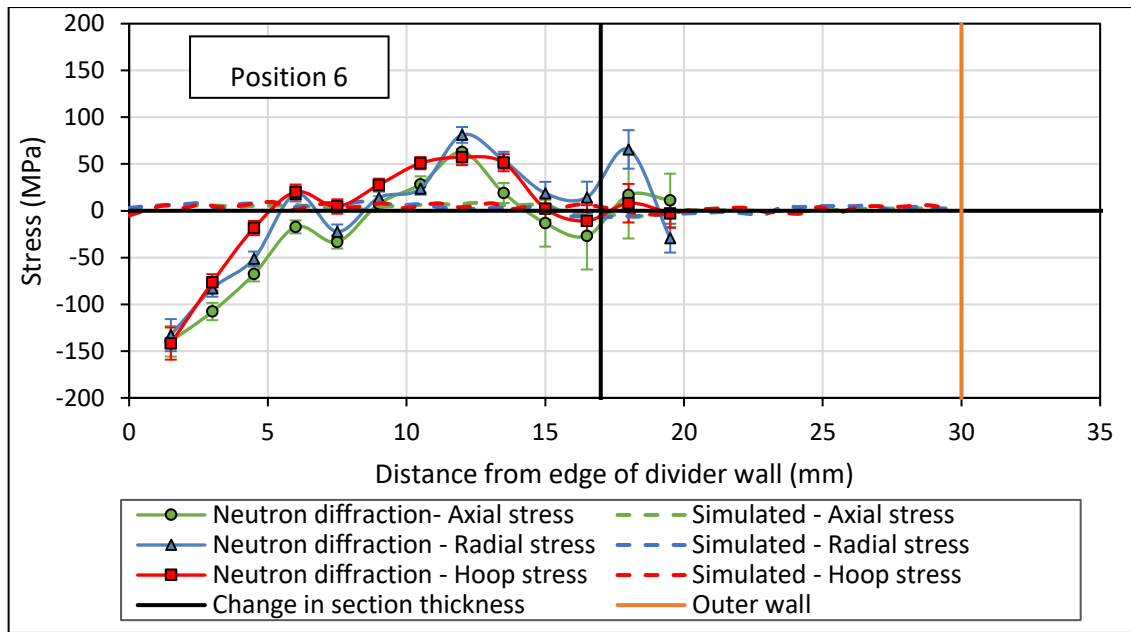


Figure 9.4 A comparison of neutron diffraction and simulated results along the centre line of position 6. Considering the residual stress state at the end of the manufacturing process.

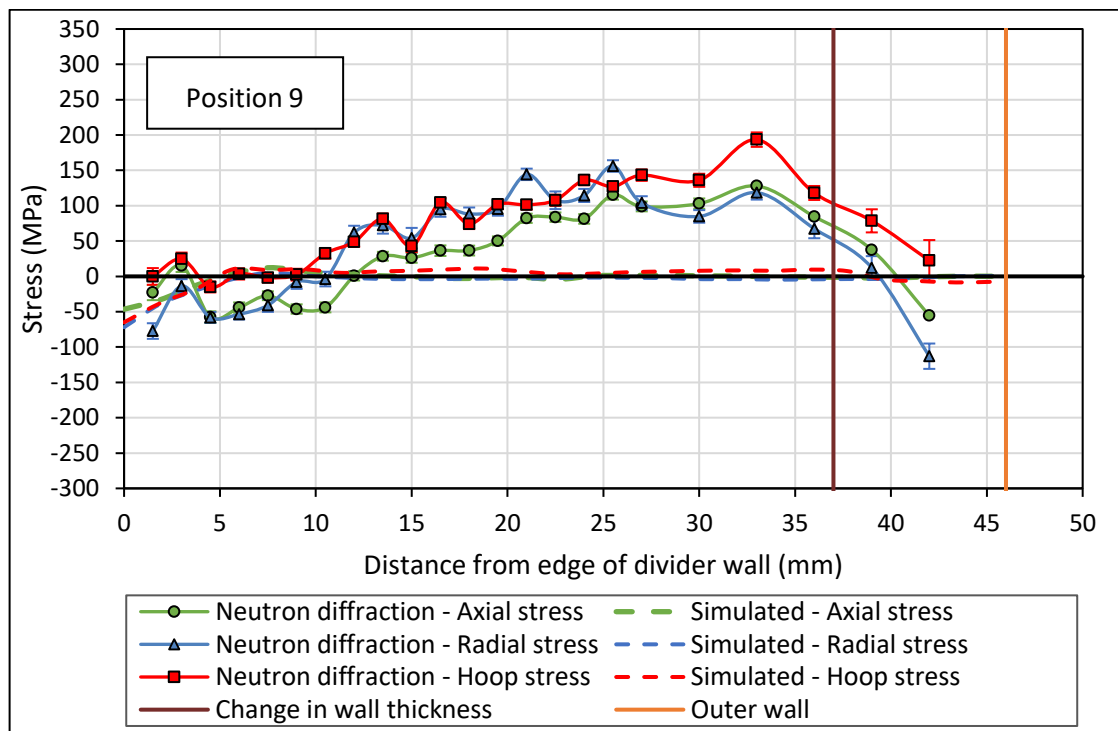


Figure 9.5 A comparison of neutron diffraction and simulated results along the centre line of position 9. Considering the residual stress state at the end of the manufacturing process.

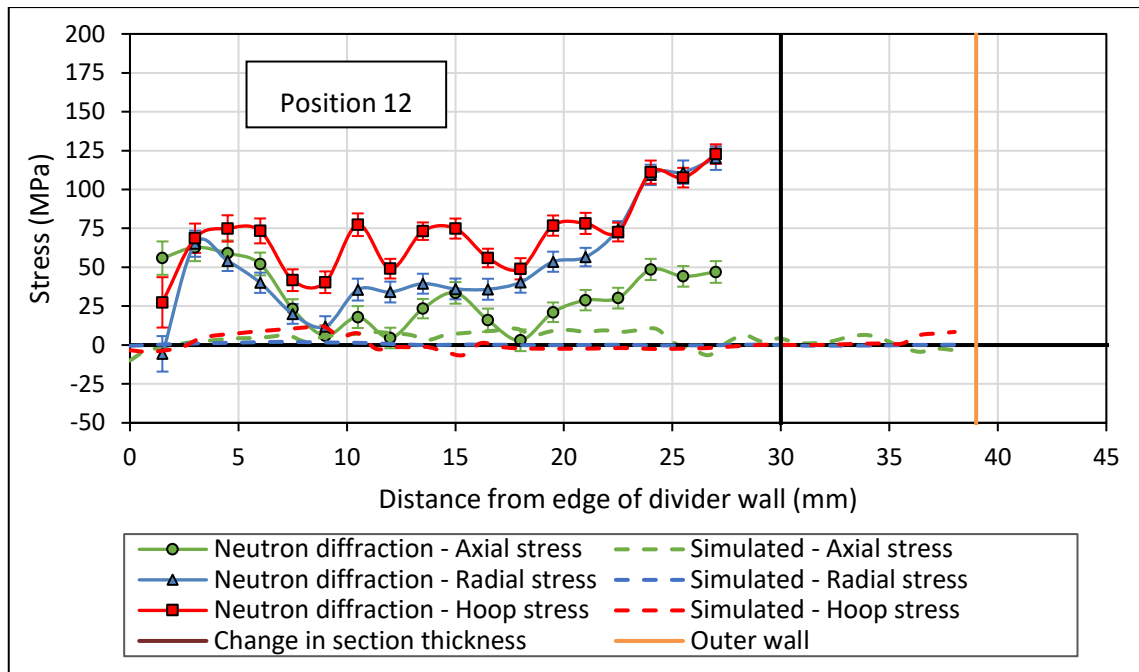


Figure 9.6 A comparison of neutron diffraction and simulated results along the centre line of position 12. Considering the residual stress state at the end of the manufacturing process.

There is a consistent trend across the simulation results of very low levels of stress. The stresses obtained from the neutron diffraction measurements are larger with a tendency for compressive stresses at the edge of the divider wall, which becomes tensile as they progress toward the change in section thickness, before once again shifting towards compression. The neutron diffraction line scans do show low levels of residual stress at the start of the line scans, particularly in the radial direction. However, the neutron diffraction results predominantly show considerably higher values than those from the simulation. The neutron diffraction results suggest that the heat treatment does not completely remove residual stresses, whereas the simulation model suggests the opposite. The simulation model was based on a few assumptions, firstly it was not known how the casting was orientated in the mould, this would influence the solidification of the housing, but is unlikely to make such large difference in the simulated result. The cooling profile considered in the simulation was also based on assumptions as it was not possible to obtain actual temperatures of the part during the solidifying and cooling stage. Ultimately the boundary conditions assumed in the simulation model require improvements to capture the correct stress behaviour. The

neutron diffraction measurements could also possess a degree of uncertainty since the technique relies on the measurement of strain in the exact same location for each of the strain directions. In addition, the use of the bulk elastic constant for the determination of stress may not be completely representative throughout the turbine housing, due to its non-uniform microstructure. The assumption has also been made that the stress-free d_0 reference sample is representative of the microstructure at the divider wall measurement location.

9.2. Applied stress during thermal cycling

A comparison of the observed temperatures recorded from the thermocouples after stabilisation (3rd cycle – as shown in Section 6.3) is compared against the FEA simulation for each thermocouple location in Figure 9.7 to Figure 9.9. The thermocouple locations are as detailed in Section 5.4.1. The simulated temperatures were found to be in good agreement with the experimental values, and within $\pm 15^\circ\text{C}$.

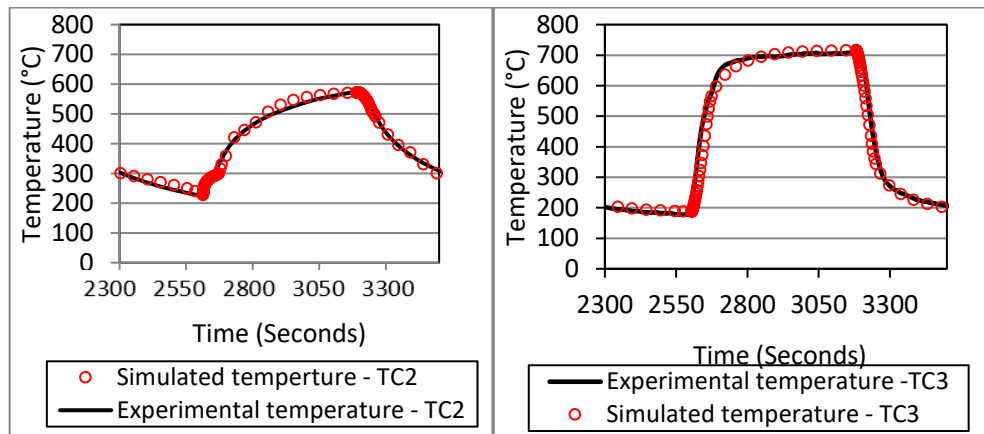


Figure 9.7 Comparison of simulated and experimental temperatures during a thermal cycle for thermocouple 2 (TC2) and thermocouple 3 (TC3).

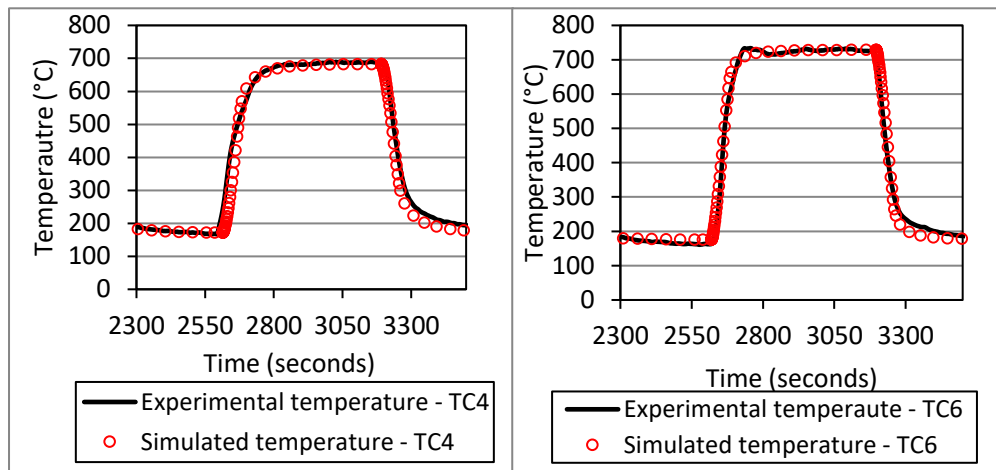


Figure 9.8 Comparison of simulated and experimental temperatures during a thermal cycle for thermocouple 4 (TC4) and thermocouple 6 (TC6).

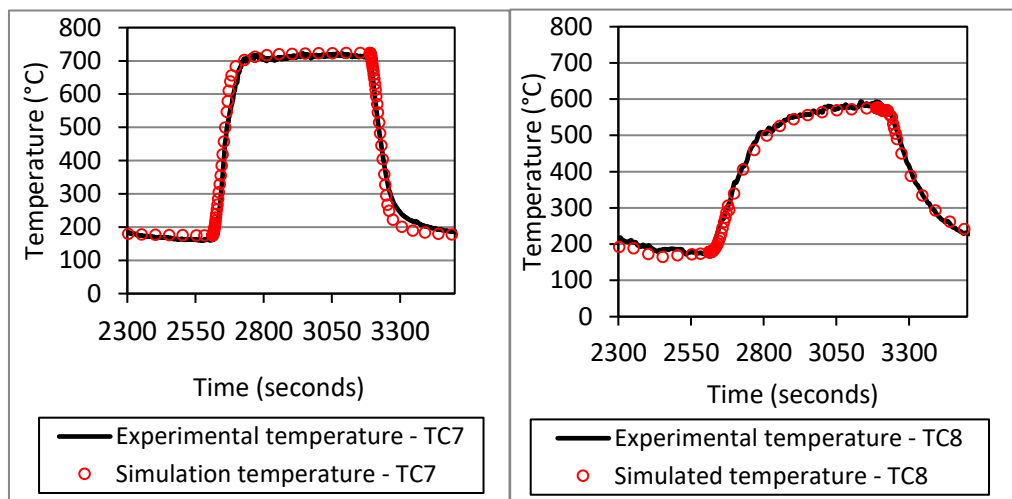


Figure 9.9 Comparison of simulated and experimental temperatures during a thermal cycle for thermocouple 7 (TC7) and thermocouple 8 (TC8).

The temperature distribution throughout the duration of the thermal cycle is as expected and there is clearly a differential in temperature that exists across the housing. With good correlation between experimental and simulated temperatures at a selection of locations throughout the turbine housing, the assumption is made that the temperatures throughout the remainder of the simulation model are representative and sufficient to capture the stress/strain behaviour during the TMF simulation. A comparison of the experimental strain gauge measurements over the course of the thermal cycle is compared against simulated results in Figure 9.8 to 9.12.

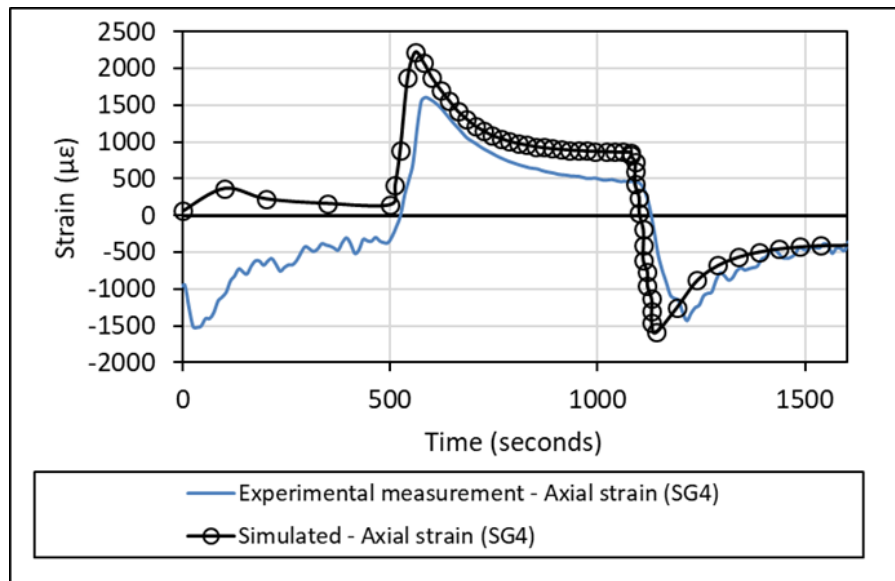


Figure 9.8 Comparison of the axial strain gauge measurement (SG4) during thermal cycle testing against simulated results.

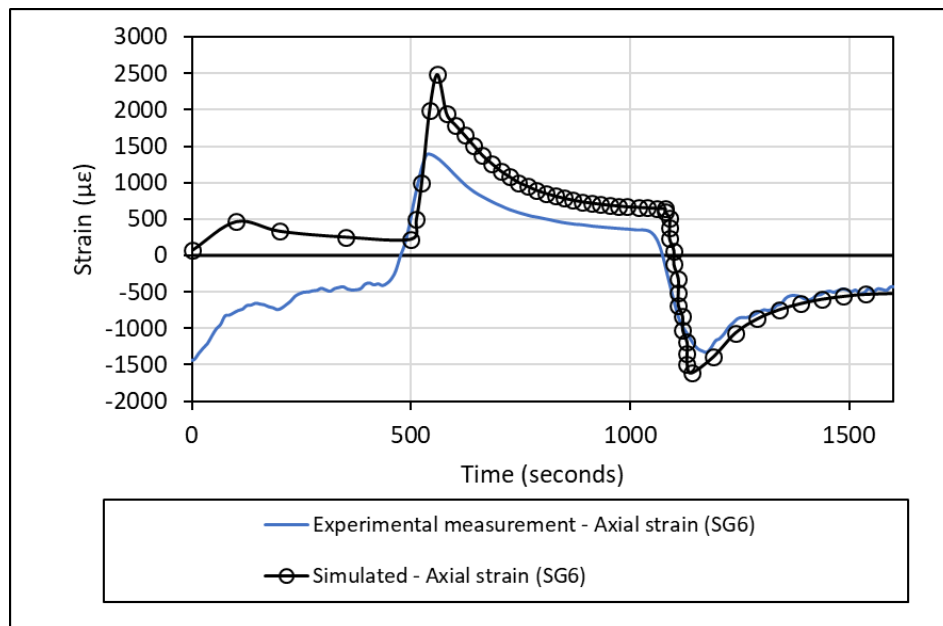


Figure 9.9 Comparison of the axial strain gauge measurement (SG6) during thermal cycle testing against simulated results.

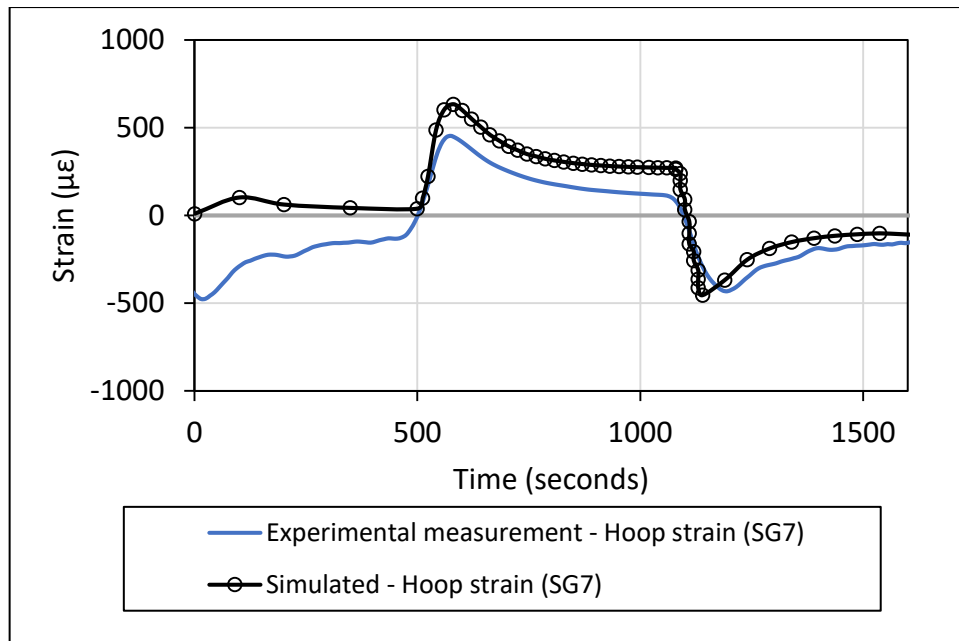


Figure 9.10 Comparison of the axial strain gauge measurement (SG7) during thermal cycle testing against simulated results.

The correlation between the observed experimental measurements of strain and the results of the simulation are comparable in that the trend is very similar exhibiting in-phase behaviour with the thermal cycle, with a rapid increase in strain during the heating phase followed by a gradual reduction as the temperature stabilises throughout the housing. On cooling, the tensile strain magnitude reduces becoming compressive. The largest magnitude of tensile strain was observed by strain gauge 4 of almost $\sim 1500\mu\epsilon$. The simulated values obtained of the maximum tensile strain were within 20-50% of experimental measurements across of the strain gauge locations. The simulated values of maximum compressive strain shortly after the ramp down in temperature showed a better correlation and were within $\sim 20\%$. However, the simulated strain values during the middle heat soak phase of the thermal cycle did show an improved correlation of 10% across some of the data points. Some of the potential reasons for the discrepancy in the results could be attributed to a variation in material properties in the actual part compared to the simulation model. In addition, the simulation model considered middle design tolerances, improved correlation may

be observed by obtaining a scanned model of the actual part. As discussed, the agreement between the observed and simulated temperature was very good, therefore this suggests the simulation model does not completely capture the correct mechanical behaviour. The simulated assembly of parts also did not represent the exact hardware used in the test as this was not practical.

The behaviour of the turbine housing, throughout the simulated thermomechanical load cycle can be summarised as follows:

- i. During the heating stage a differential in temperature develops across the housing.
- ii. The internal divider wall due to its lower section thickness rapidly increases in temperature.
- iii. The divider wall region experiences large, compressive hoop stresses resulting in yielding and plastic deformation, the external surface of the housing experiences tensile stress along the centre line of the geometry which balances those in the divider wall.
- iv. As the thermal load reaches steady state, the temperature throughout the housing stabilises, and with a reduction in the differential in temperature, a reduction in strain levels is observed.
- v. During the cooling phase of the thermal cycle the temperature of the divider wall cools rapidly, inducing large tensile hoop stresses in the divider wall while the external surface of the volute is subjected to a combination of compressive hoop and axial stresses.
- vi. The tongue is a position in which the divider wall is constrained from moving hence it experiences greater magnitudes of stress.

An assessment of the accumulated plastic strain (PEEQ) at the end of a single thermal load cycle using an isotropic hardening material model was used to examine the risk of fatigue crack initiation within the turbine housing. The simulated results of the PEEQ at the end of one load cycle are given in Figure 9.11 and are compared against actual fatigue crack locations, noting that the limit at which cracking is predicted to occur in the simulation model is when the level of PEEQ is $\geq 0.2\%$ for the SiMo 5.1 material. The simulation accurately predicted that the divider wall would crack with large regions of PEEQ observed in the fatigue crack locations. The maximum PEEQ was observed to be 4.6087% and located in the tongue location.

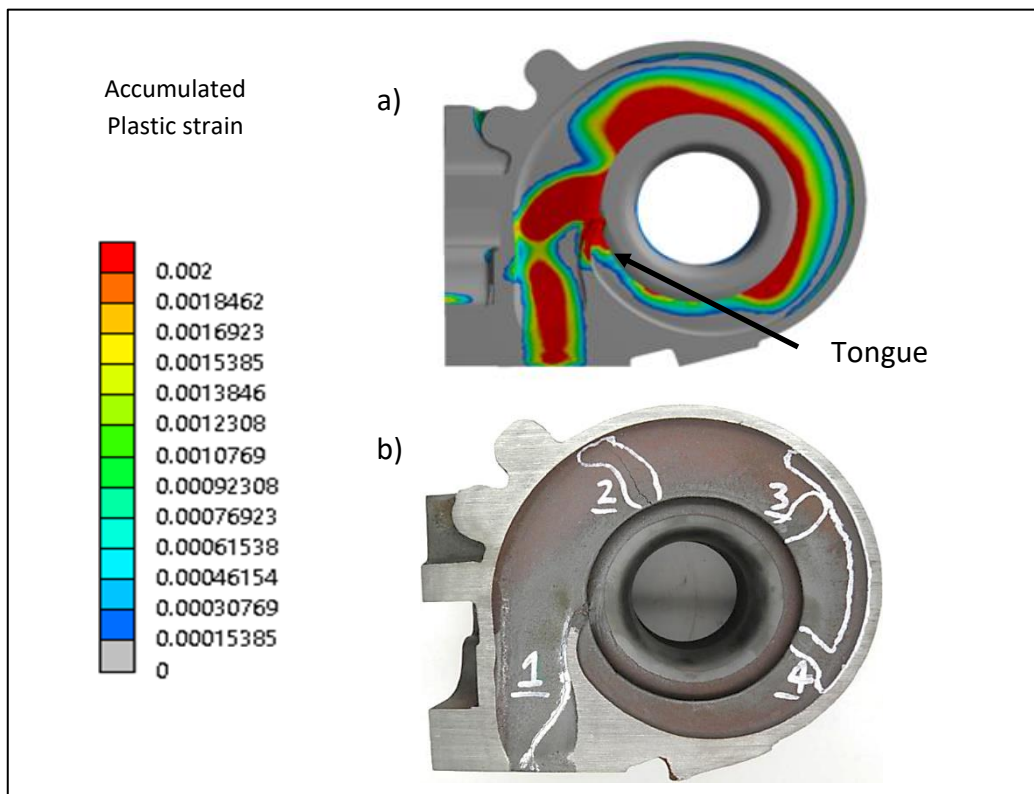


Figure 9.11 Comparison of a) Simulated accumulated plastic strain at the end of one load cycle and b) actual crack locations after 1200 cycles. PEEQ values $\geq 0.2\%$ (red contour) are at risk of fatigue crack initiation

Figure 9.12 shows the PEEQ at the end of the thermal cycle with the addition of the pre-stress condition of residual stress from the MAGMA simulations, the values PEEQ increased slightly in the tongue location to 4.611%. suggesting the pre-stress load has combined with the applied load.

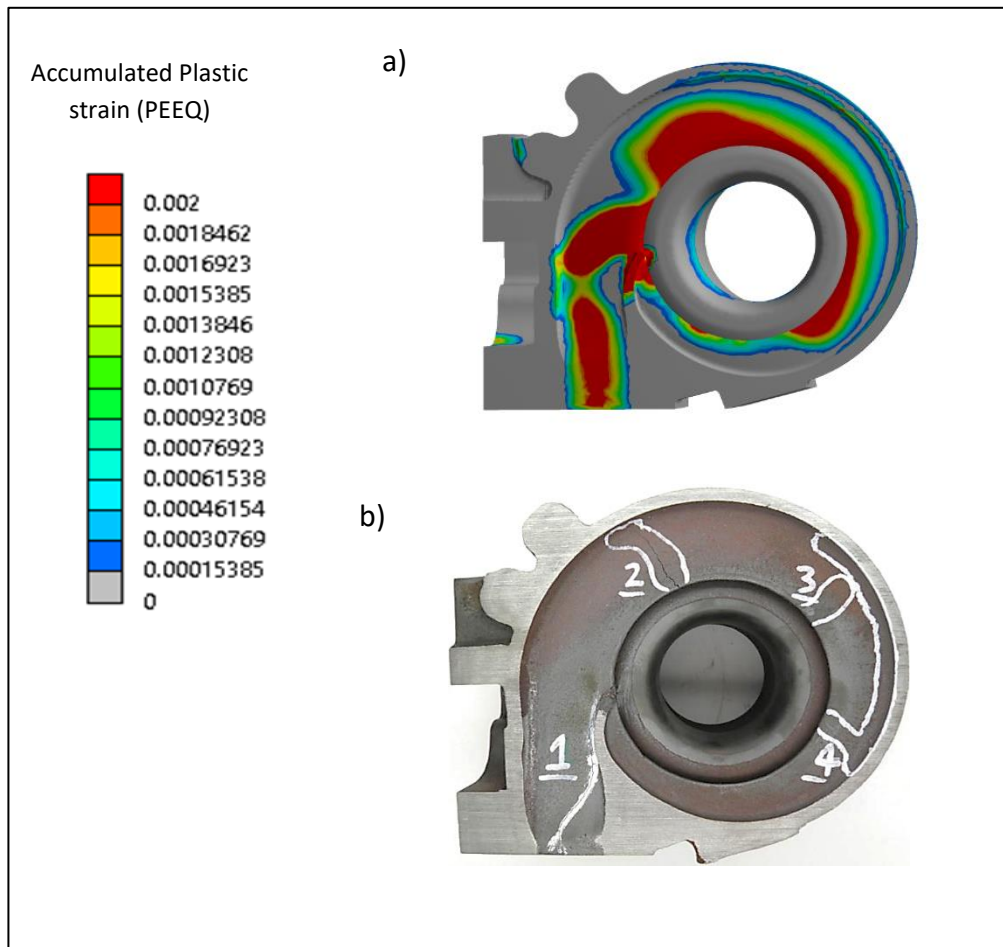


Figure 9.12 Comparison of a) Simulated accumulated plastic strain at the end of one load cycle and b) actual crack locations after 1200 cycles. PEEQ values $\geq 0.2\%$ (red contour) are at risk of fatigue crack initiation.

Fatigue cracks were observed to have initiated due to large hoop stresses on the edge of divider wall and propagated radially through the divider wall. They did not propagate completely to the exterior surface, due in part to an increase in section

thickness that occurs around the outer perimeter of the housing. In addition, the divider wall is subjected to the highest temperature, and as the yield strength reduces with temperature, is more susceptible to plastic deformations. The fact that good correlation between the TMF simulated crack locations and the actual crack locations was possible regardless of the addition of residual stress, could suggest that residual stresses may play a minimal role in improving the predictions of crack locations. However, to accurately determine this, it is necessary to consider viscoplastic material models and utilise fatigue life prediction software to assess the number of cycles to failure.

9.3. Residual stresses after thermal cycling

Neutron diffraction measurements were repeated after the turbine housing had been subjected to 10 thermal cycles. It was observed after testing that the tongue location (close to the start of position 9) exhibited a fatigue crack. A comparison of the neutron diffraction line scan measurements and the extracted stress result along the centre line of the divider wall from the simulations is detailed in Figure 9.13 to Figure 9.16.

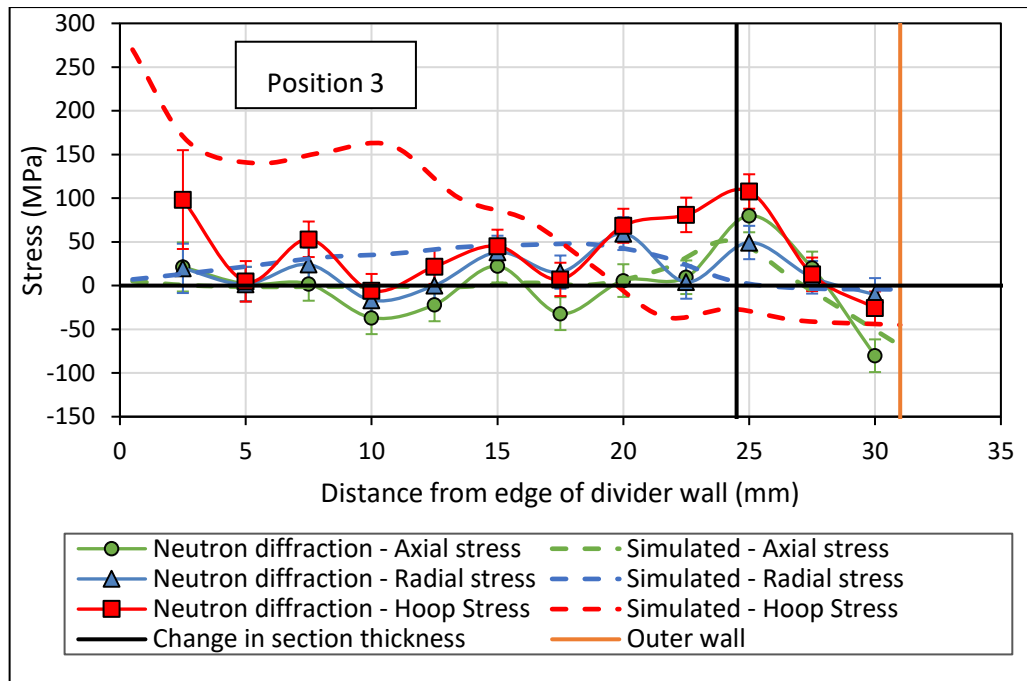


Figure 9.13 A comparison of neutron diffraction and simulated results along the centre line of position 3. Considering the residual stress state after thermal cycling.

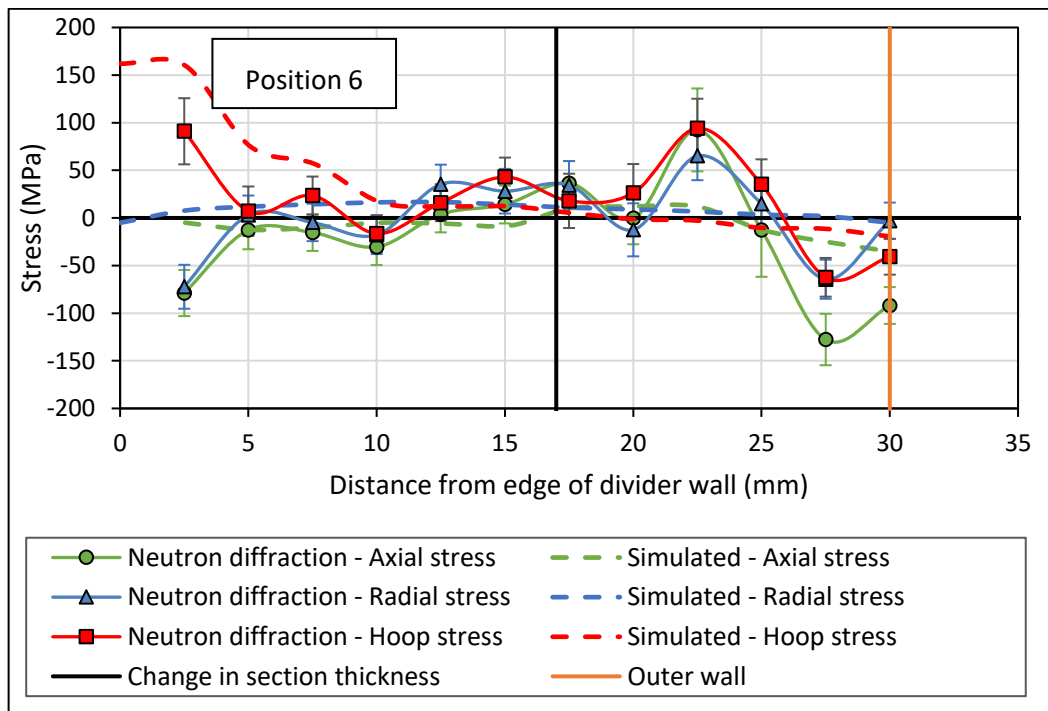


Figure 9.14 A comparison of neutron diffraction and simulated results along the centre line of position 6. Considering the residual stress state after thermal cycling.

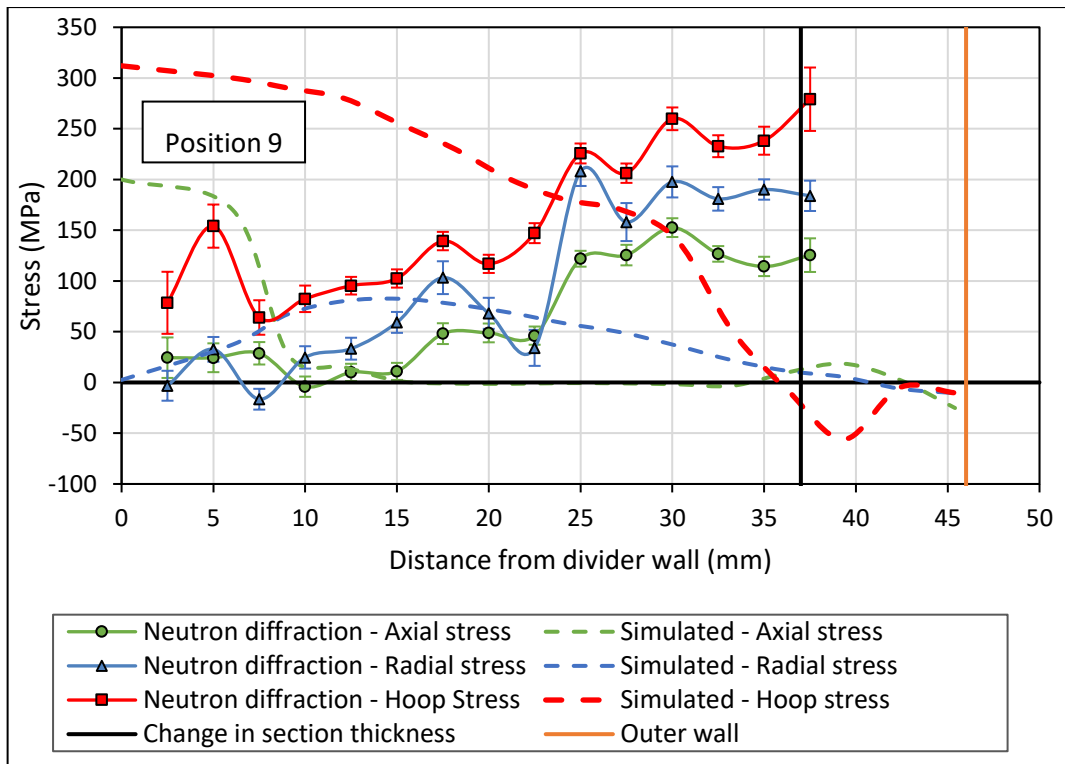


Figure 9.15 A comparison of neutron diffraction and simulated results along the centre line of position 9. Considering the residual stress state after thermal cycling.

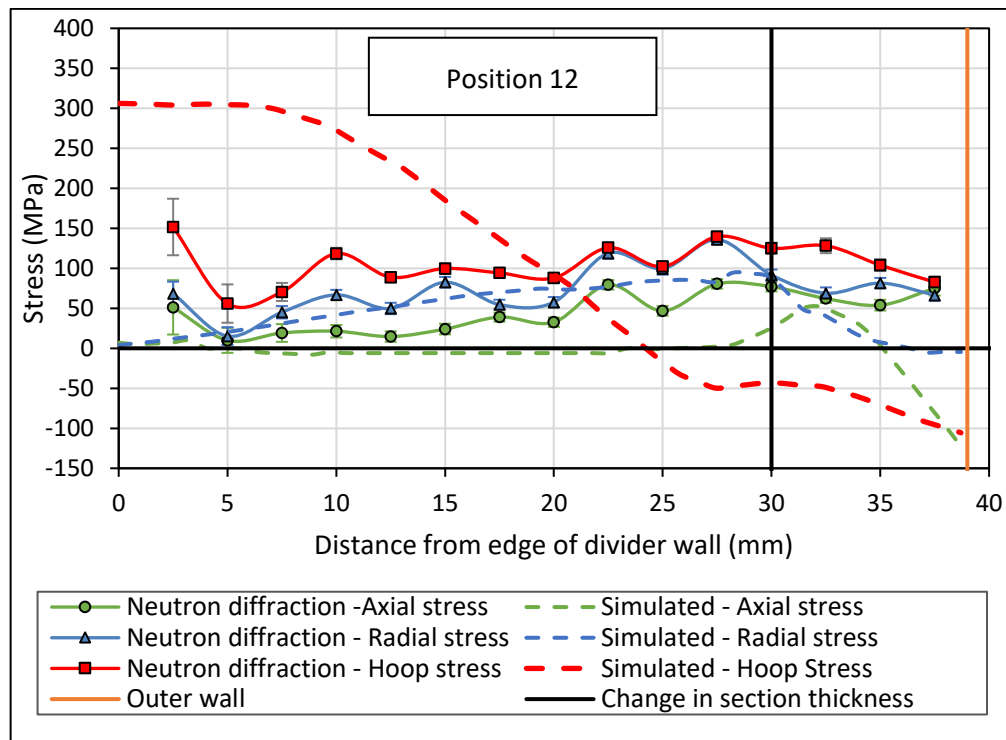


Figure 9.16 A comparison of neutron diffraction and simulated results along the centre line of position 12. Considering the residual stress state after thermal cycling.

In the neutron diffraction results there is a clear trend of large tensile hoop stresses at the start of the measurement line, with lower magnitudes of radial and axial stress. The simulation shows a similar trend with large hoop stresses at the start of the measurements. The neutron diffraction measurements of positions 3 and 6 appear to exhibit similar behavior and the simulated results agree reasonably well in that the axial and radial stresses are lower in magnitude and tend towards compression at the end of the measurement line with larger tensile hoop stresses at the first measurement point. The neutron diffraction measurements at position 9 and 12, show a slightly different behavior in that they exhibited an increased magnitude of tension along the line scan. This was especially evident in position 9, the location which experienced the largest load during the thermal cycling.

A comparison of the neutron measurements before and after thermal cycling was given in section Figure 6.23. A variation in the stress magnitude and direction was observed after the application of the thermal load, which suggests a combination of stress relaxation and strain hardening had taken place. There was evidence of an increase in hoop stress at the start of every position line scan, suggesting the onset of strain hardening because of plastic deformations taking place, this agrees with that observed in the TMF simulation which showed that the edge of the divider wall experiences plastic deformation.

10. Conclusion

This thesis presents the results of a novel combination of experimental and simulation techniques to measure stresses throughout the lifecycle of a typical turbine housing. The accurate validation of residual stress and thermomechanical fatigue (TMF) simulations can be challenging and measurements of internal residual stresses in areas prone to fatigue crack initiation are difficult to obtain.

To study the residual stresses that are introduced during the manufacturing stage of the turbine housing, and after thermomechanical loading, the following experimental and simulation work was undertaken:

- Contour measurements (before TMF loading)
- Neutron diffraction measurements (before and after TMF loading).
- Simulations using MAGMA casting software of the process induced residual stresses.

In addition, to investigate the behaviour of the turbine housing during operation due to TMF loading, the following experimental and simulation work was performed:

- High temperature strain gauge and thermocouple measurements during thermal cycling.
- ANSYS simulations using an isotropic material model to simulate one thermal cycle, assessing the accumulated plasticity at the end of the cycle and observing fatigue crack locations.
- Durability testing of the housing for 1200 cycles to observe fatigue crack initiation and propagation.

A review of existing literature suggested that the turbine housing was a complex geometry to study with the neutron diffraction technique. Previous studies focussed primarily on weld induced residual stresses with limited examples of measurements on complete engineering components. The turbine housing is a challenging geometry to obtain measurements from due in part to the limited neutron path length, as well as

the difficult positioning and alignment requirements, which are necessary to study key areas prone to fatigue, such as the divider wall and tongue regions. As a result, this research has pushed the boundaries of the neutron diffraction technique. The measurement of residual stress after the application of thermomechanical loading to an engineering component also appears to be novel, with existing research focussing on measurements in single crystals, simple bars or tensile test specimens.

The contour method is considered a relatively new residual stress measurement technique, and therefore its application to challenging structures which contribute to its development are important. A review of existing literature suggested that due to the large variation in section thicknesses found within the turbine housing, the contour method could be particularly challenging to apply. This research has set out a methodology through which successful measurements have been obtained and could assist future measurements of complex geometries.

The use of neutron diffraction and the contour method as well as strain gauges to study applied strain during thermal cycling, has been used to validate simulation models. This is important as it assists in improving the accuracy of simulation models, minimising the need for expensive testing and limiting potential turbine housing failures in the field. Prior to this research it was unknown what levels of residual stress could be present within the turbine housing. A summary of each of the techniques and the results will now be given:

10.1. Neutron Diffraction

A key advantage of using neutron diffraction to measure residual stress is that it offers the ability to obtain measurements of internal tri-axial stresses non-destructively. The technique can be applied to large engineering components due to the capabilities of the SALSA strain instrument.

The main disadvantages of the technique are that measurements are limited to line scans only, which can be time consuming especially as the path length limit for cast

iron of ~60mm is approached. This limits the amount of measurements that can be obtained. Neutron diffraction can also be very expensive to access and limited, specialised facilities are required. It is also sensitive to microstructure variations and relies upon the use of a stress-free reference sample for calibration.

The neutron diffraction measurements obtained in this research observed a substantial variation in stress levels. The measurements at the end of the manufacturing process were observed to be tensile in nature ranging from very low levels $<\pm 50\text{MPa}$ at the perimeter edge of the line scan and up to 150MPa in magnitude close to the divider wall. There appears to be a continual increase in tension which then reduces as a change in section thickness occurs.

The results of the neutron measurements show a variation in stress magnitude and direction after thermal cycling, suggesting both stress relaxation and strain hardening had occurred.

The uncertainty in the neutron measurements, associated with the fitting errors to the spectrum for the turbine housing and d_0 measurements was ~5-15MPa for most of the measurement points. However, the accuracy of these measurements also relies upon probing the exact same location for each of the strain directions. In addition, the use of the bulk elastic constant for the determination of stress may not be completely representative of the measurement locations due to variations in microstructure. The assumption has also been made that the stress-free d_0 reference sample is representative of the microstructure at the divider wall measurement location.

10.2. The Contour Method

The contour method, is a destructive technique and involves the sectioning of the turbine housing before measurement of the cut faces and the subsequent determination of stress. Its advantages are that it allows a stress map of the entire cut surface to be obtained and can be applied in a lab-based setting. It is based on different assumptions compared to neutron diffraction and is not sensitive to

microstructure, therefore, it is a good complementary technique. Compared to neutron diffraction the contour method does not require a stress-free reference sample. However, the accuracy of the method is very dependent on the quality of the sectioning cut and measurements are limited to uni-axial stresses.

The contour method was applied to investigation manufacturing induced residual stresses only. Difficulty was encountered due to the introduction of cutting artefacts to the cut surfaces resulting in unrealistically high stress levels. Numerous steps were required to improve the accuracy of the result, with the eventual use of sacrificial layers to improve the uncertainty in the measurement. Ultimately the contour method showed low residual stress levels of $\pm 30\text{MPa}$ within the bulk of the cross section. This was somewhat contradictory to the neutron diffraction measurements which showed levels up to $\sim 150\text{MPa}$, measurements closer to the outside edge of the housing did however show better correlation.

10.3. Strain Gauges

Strain gauges were applied in conjunction with thermocouples to a turbine housing which was subjected to several thermal cycles. Strain gauges were the only practical way to obtain in-situ strain measurements and their use, especially at temperature and with a complex strain field can be difficult. The strain gauges were observed to be in-phase with the strain, with the largest magnitude of $\sim \pm 1500\mu\epsilon$ being observed.

10.4. Numerical Simulations

Simulations of the stress distribution throughout the entire lifetime of the turbine housing were undertaken. The use of numerical simulation is easier, cheaper and less time consuming compared to measurement techniques, however the accuracy of any simulation is very dependent on both the chosen boundary conditions and material models.

A simulation using MAGMA software was performed to investigate the change in stress throughout the standard production process of the turbine housing. The stress distribution at the end of the production process, which encompasses solidifying, cooling (both in and out of the mould), heat treatment, feeding system removal and finally machining was then compared against the neutron diffraction and contour measurements. The simulation showed very low $<11\text{MPa}$ levels of stress within the bulk of the material at the end of the simulation, with a slight increase in both tension and compression near the tongue of $\sim \pm 60\text{MPa}$. The low levels of stress were due to the simulated annealing heat treatment which caused rapid relaxation of residual stress. In contrast the neutron diffraction results revealed higher levels of stress, tensile in nature and up to 150MPa in magnitude.

A comparison of the contour measurement and the simulation shows that the residual stresses agree over most of the cross-section in that they are low in magnitude, with the contour results varying between approximately $\pm 30\text{MPa}$ within the bulk of the cross-section while the simulation results fall between $\pm 11\text{MPa}$.

The TMF simulation was validated against strain gauge measurements, the observed trends were very similar, with high magnitude of tensile strain exhibited during heat up phase and compressive strain on the cool down. The correlation between the maximum and minimum strain values between the simulation and experimental measurements varied and was within 10-50%. The model was accurate enough to predict the fatigue crack locations when assessed against a housing subjected to an extended durability test. During the simulated thermal cycle, the turbine housing was observed to experience a continually changing stress distribution. The heating phase of the cycle was characterised by large compressive hoop stresses in the interior divider wall which on cool down move into tension; it is this continuous variation in stress that results in fatigue crack initiation.

After thermal cycling the neutron diffraction results showed an increase in tensile stress especially in locations 9 and 12, this is most likely due to a strain hardening effect induced by plastic deformation. The FEA simulation observed a similar trend

showing increased tensile residual stresses across the measurement line compared to the simulation prior to thermal cycling. The neutron diffraction measurements also revealed that the biggest increases in residual stress occur in the hoop direction after thermal cycling; simulation models show a similar behavior

It is clear from this work that there are several methods for characterising residual stresses in engineering materials, each with good and bad points. The measurements presented here have shown it is possible to characterise the stress/strain behaviour throughout a typical lifecycle of the turbine housing through both experimental measurements and simulations.

10.5. Recommendations for further work

This research did not consider the effect of stress relaxation and creep during the thermomechanical fatigue simulation. To simulate this a time-dependent viscoplastic material model is required; this would then allow an assessment of the impact that residual stresses may have on the fatigue life of the turbine housing to be determined, and the number of cycles to failure could be assessed both with and without the inclusion of the pre-stress condition due to residual stresses. Further work is required to understand the strain hardening and softening behaviour that takes place as a result of the thermomechanical loading.

Additionally, to improve the correlation between simulation models of residual stress, experimental measurements at each stage in the production process, such as before and after heat treatment should be considered as well as the generation of high transient stresses during the final cooling phase of the thermal cycle during the simulation. Further measurements would be required to determine the apparent difference between the contour, and neutron diffraction results particularly around the change in section thickness along the line scan.

In order to better assess the contour method of measuring residual stress, it is necessary to investigate the effect that incremental cuts may have on the final stress

state in the turbine housing, due to the possibility that stresses are partially relieved throughout the cutting stages.

Finally, to assist in further measurements the limitation in the path length of the neutrons could be improved through a combined use of hole drilling to reduce the amount of material the neutron beam must pass through as shown by Labbe, Poisson, Prioul, & Lodini (2001).

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[illegible]

BEFORE TESTING (A)

THICKNESS (ENG), 1 [mm]

Measurement on 16/11/2016 07:34

Operator	Admin	Measurement on	16/11/2016 07:34
Part Number		Batch Number	
Supplier		Batch Quantity	
Operator		Date Received	

FEATURE 1 Drawing.

Diameter	NV=	83.88000	AV=	83.91150	---
(2) DATUM B	UT=	0.05000	DV=	0.03150	
	LT=	0.00000			

Actual.

FEATURE 2

Flatness					
(1) DATUM A	TO=	0.05000	DV=	0.00305	*

FEATURE 3

Position Z	NV=	135.75000	AV=	135.81969	---
(6) EO FACE	UT=	0.20000	DV=	0.06969	
	LT=	-0.20000			

FEATURE 4

Distance	NV=	11.60000	AV=	11.56072	-----
(12) FEATURE 4	UT=	0.05000	DV=	-0.03928	
	LT=	-0.05000			

FEATURE 5

Distance Z	NV=	113.96000	AV=	114.14725	-----
(8) FEATURE 5 TOP	UT=	0.30000	DV=	0.18725	
	LT=	-0.30000			

Distance Z	NV=	113.96000	AV=	114.22925	-----
(9) FEATURE 5 RIGHT	UT=	0.30000	DV=	0.26925	
	LT=	-0.30000			

Distance Z	NV=	113.96000	AV=	114.26130		ER=	0.00130
(10) FEATURE 5 BOTTOM	UT=	0.30000	DV=	0.30130			
	LT=	-0.30000					

Distance Z	NV=	113.96000	AV=	114.27990		ER=	0.01990
(11) FEATURE 5 LEFT	UT=	0.30000	DV=	0.31990			
	LT=	-0.30000					

FEATURE 7

Contour comparison	UT=	0.04000	UD=	0.01399
Nom.:2, Act.:1	LT=	-0.04000	LD=	-0.05246
In nom.pnts / Nom.rectang			MD=	0.03967

270 (ENG), 1 [mm]

Measurement on 16/11/2016 07:34

Operator	Admin	Measurement on
		16/11/2016 07:34

Contour comparison	UT=	0.04000	UD=	0.01521
Nom.:4, Act.:3	LT=	-0.04000	LD=	-0.05105
In nom.pnts / Nom.rectang			MD=	0.03829

Contour comparison	UT=	0.04000	UD=	0.01550
Nom.:6, Act.:5	LT=	-0.04000	LD=	-0.03761
In nom.pnts / Nom.rectang			MD=	0.02616

Contour comparison	UT=	0.04000	UD=	0.01560
Nom.:8, Act.:7	LT=	-0.04000	LD=	-0.03798
In nom.pnts / Nom.rectang			MD=	0.02575

FEATURE 8

Diameter	NV=	115.90000	AV=	115.90052	---
(20) FEATURE 8	UT=	0.30000	DV=	0.00052	
	LT=	-0.30000			

FEATURE 9

Diameter	NV=	90.55000	AV=	90.57031	---
(19) FEATURE 9	UT=	0.05000	DV=	0.02031	
	LT=	-0.05000			

FEATURE 10

Distance Z	NV=	113.05000	AV=	113.26760	---
(1) FEATURE 10	UT=	0.80000	DV=	0.21760	
	LT=	-0.80000			

FEATURE 11

Flatness					---
(2) DATUM D	TO=	0.10000	DV=	0.00899	

FEATURE 12

Angle	NV=	20.00000	AV=	20.01524	---
(2) FEATURE 12	UT=	1.00000	DV=	0.01524	
	LT=	-1.00000			

FEATURE 13

Angle	NV=	45.00000	AV=	45.01866	---
(1) FEATURE 13	UT=	1.00000	DV=	0.01866	
	LT=	-1.00000			

FEATURE 14

(ENG). 1 .[mm]

Measurement on 16/11/2016 07:34

Admin Measurement on 16/11/2016 07:34

Cyl. radius XY
(4) FEATURE 12 NV= 100.30000 AV= 100.19559
UT= 0.30000 DV= -0.10441
LT= -0.30000

Cyl. radius XY
(3) FEATURE 13 NV= 100.30000 AV= 100.10833
UT= 0.30000 DV= -0.19167
LT= -0.30000

FEATURE 15

Position X
(10) DRILL PATTERN CENTRE NV= 66.55000 AV= 66.49110
UT= 1.00000 DV= -0.05890
LT= -1.00000

FEATURE 16

Position Y PO
(4) FEATURE 16 NV= 74.60000 AV= 74.61607
UT= 2.20000 DV= 0.01607
LT= -2.20000

FEATURE 17

Position Z
(17) FEATURE 17/26 NV= 53.20000 AV= 53.13078
UT= 0.30000 DV= -0.06922
LT= -0.30000

FEATURE 18

Position Z
(16) FEATURE 20/18 NV= 21.70000 AV= 21.78250
UT= 0.20000 DV= 0.08250
LT= -0.20000

FEATURE 19

Position Y PO
(1) DATUM D NV= 77.85000 AV= 77.80168
UT= 0.50000 DV= -0.04832
LT= -0.50000

FEATURE 20

Position X
(16) FEATURE 20/18 NV= 93.90000 AV= 93.86257
UT= 0.30000 DV= -0.03743
LT= -0.30000

FEATURE 21

Position X
(15) FEATURE 21/24 NV= 47.10000 AV= 47.11441
UT= 0.30000 DV= 0.01441
LT= -0.30000

(ENG). 1 (mm)

Admin

Measurement on

Measurement on 16/11/2016 07:34

16/11/2016 07:34

Position X
(18) FEATURE 25 21

NV= 47.10000 AV= 47.12151
UT= 0.30000 DV= 0.02151
LT= -0.30000

FEATURE 22

Position X
(14) FEATURE 22/23

NV= 2.90000 AV= 2.94297
UT= 0.10000 DV= 0.04297
LT= -0.10000

FEATURE 23

Position Z
(14) FEATURE 22/23

NV= 16.60000 AV= 16.65780
UT= 0.20000 DV= 0.05780
LT= -0.20000

FEATURE 24

Position Z
(15) FEATURE 21/24

NV= 46.80000 AV= 46.87109
UT= 0.30000 DV= 0.07109
LT= -0.30000

FEATURE 25

Position Z
(18) FEATURE 25 21

NV= 22.20000 AV= 22.10977
UT= 0.20000 DV= -0.09023
LT= -0.20000

FEATURE 26

Position X
(17) FEATURE 17/26

NV= 84.80000 AV= 84.79843
UT= 0.30000 DV= -0.00157
LT= -0.30000

FEATURE 27

Diameter
(10) FEATURE 27

NV= 105.50000 AV= 105.54656
UT= 0.30000 DV= 0.04656
LT= -0.30000

FEATURE 28

Distance Z
(7) FEATURE 28

NV= 3.80000 AV= 3.80189
UT= 0.10000 DV= 0.00189
LT= -0.10000

FEATURE 29

Distance X
(2) FEATURE 29 TOP

NV= 44.50000 AV= 44.47575
UT= 0.20000 DV= -0.02425
LT= -0.20000

(ENG). 1 [mm]

Measurement on 16/11/2016 07:34

Operator

Admin

Measurement on

16/11/2016 07:34

Distance X	NV=	44.50000	AV=	44.48539	---
(3) FEATURE 29 BOTTOM	UT=	0.20000	DV=	-0.01461	---
	LT=	-0.20000			---
FEATURE 30					
Distance Z	NV=	86.00000	AV=	85.95383	---
(4) FEATURE 30 LEFT	UT=	0.20000	DV=	-0.04617	---
	LT=	-0.20000			---
Distance Z	NV=	86.00000	AV=	85.95727	---
(5) FEATURE 30 RIGHT	UT=	0.20000	DV=	-0.04273	---
	LT=	-0.20000			---
FEATURE 31					
Diameter	NV=	11.00000	AV=	11.01816	---
(6) FLANGE HOLE 1	UT=	0.20000	DV=	0.01816	---
	LT=	-0.20000			---
Diameter	NV=	11.00000	AV=	11.02763	---
(7) FLANGE HOLE 2	UT=	0.20000	DV=	0.02763	---
	LT=	-0.20000			---
Diameter	NV=	11.00000	AV=	11.02620	---
(8) FLANGE HOLE 3	UT=	0.20000	DV=	0.02620	---
	LT=	-0.20000			---
Diameter	NV=	11.00000	AV=	11.01960	---
(9) FLANGE HOLE 4	UT=	0.20000	DV=	0.01960	---
	LT=	-0.20000			---
FEATURE 32					
Flatness					---
(4) FEATURE 32	TO=	0.10000	DV=	0.00768	---

(B) After Gas Standtest

Part Number: : ~~XXXXXXXXXX~~
 Batch Number: :
 Supplier: :
 Batch Quantity: :
 Operator: :
 Date Received: :

Drawing				Actual			
**FEATURE 1							
12	N0293 Diameter	83.88000	0.05000 -0.05000	83.82303	-0.05697 -0.00697	<<----+-----	
**FEATURE 2							
1	N0295 Flatness		0.05000	0.00626		**-----	
**FEATURE 3							
2	N0297 Pos. Z	135.75000	0.20000 -0.20000	135.91613	0.16613	-----*****-	
**FEATURE 4							
11	N0300 Distance	11.60000	0.05000 -0.05000	11.42767	-0.17233 -0.12233	<<----+-----	
**FEATURE 5							
10	N0303 Distance	113.96000	0.30000 -0.30000	114.81878	0.85878 0.55878	-----+---->>	
7	N0304 Distance	113.96000	0.30000 -0.30000	114.35798	0.39798 0.09798	-----+---->>	
8	N0305 Distance	113.96000	0.30000 -0.30000	114.37831	0.41831 0.11831	-----+---->>	
9	N0306 Distance	113.96000	0.30000 -0.30000	114.31744	0.35744 0.05744	-----+---->>	
**FEATURE 7							
**FEATURE 8							
13	N0337 Diameter	115.90000	0.30000 -0.30000	115.94190	0.04190	-----**-----	
**FEATURE 9							
14	N0339 Diameter	90.55000	0.05000 -0.05000	90.53057	-0.01943	---***-----	
**FEATURE 10							
1	N0341 Distance	113.05000	0.80000 -0.80000	113.44064	0.39064	-----****-----	
**FEATURE 11							
2	N0343 Flatness		0.10000	0.08680		*****-	
**FEATURE 12							
1	N0348 Angle	20.00000	1.00000 -1.00000	20.01855	0.01855	-----*------	
**FEATURE 13							
2	N0351 Angle	45.00000	1.00000 -1.00000	45.10129	0.10129	-----**-----	
**FEATURE 14							
5	N0353 Cyl.Rad.	100.30000	0.30000 -0.30000	100.18179	-0.11821	---***-----	
Page 1							

Before

After

Feature	Before	After
4 N0354 Cyl.Rad.	100.30000	100.12023
**FEATURE 15		-0.17977
6 N0356 Pos. X	66.55000	66.55263
**FEATURE 16		0.00263
5 N0359 Pos. Y	74.60000	74.64164
**FEATURE 17		0.04164
15 N0362 Pos. Z	53.20000	53.16926
**FEATURE 18		-0.03074
19 N0364 Pos. Z	21.70000	21.67827
**FEATURE 19		-0.02173
1 N0367 Pos. Y	77.85000	77.74163
**FEATURE 20		-0.10837
19 N0370 Pos. X	93.90000	93.67473
**FEATURE 21		-0.22527
18 N0372 Pos. X	47.10000	47.02502
16 N0373 Pos. X	47.10000	47.10709
**FEATURE 22		0.00709
17 N0375 Pos. X	2.90000	2.87787
**FEATURE 23		-0.02213
17 N0377 Pos. Z	16.80000	16.69734
**FEATURE 24		-0.10266
18 N0379 Pos. Z	46.80000	46.69264
**FEATURE 25		-0.10736
16 N0381 Pos. Z	22.20000	22.18652
**FEATURE 26		-0.01348
15 N0383 Pos. X	84.80000	84.76818
**FEATURE 27		-0.03182
3 N0386 Diameter	105.50000	105.54682
**FEATURE 28		0.04682
6 N0388 Distance	3.80000	3.79872
**FEATURE 29		-0.00128
2 N0390 Distance	44.50000	44.47080
3 N0391 Distance	44.50000	44.46010
**FEATURE 30		-0.02920

4 N0393 Distance	86.00000	12701009004 0.20000	85.92839	-0.07161	-----***-----
5 N0394 Distance	86.00000	-0.20000 0.20000	85.91991	-0.08009	-----***-----
**FEATURE 31					
7 N0396 Diameter	11.00000	0.20000	10.98267	-0.01733	-----*-----
8 N0397 Diameter	11.00000	-0.20000 0.20000	10.98294	-0.01706	-----*-----
9 N0398 Diameter	11.00000	-0.20000 0.20000	10.93642	-0.06358	-----***-----
10 N0399 Diameter	11.00000	-0.20000 0.20000	10.93522	-0.06478	-----***-----
**FEATURE 32					
4 N0401 Flatness		0.10000	0.03679		***---

Appendix 2 – Importing MAGMA stress results into ANSYS mechanical

The following commands were applied in the setup of the static structural analysis

```
/SOLU
```

```
inistate,set,dtyp,stress
```

```
inis,read,StressG,ans,C:\FILELOCATION
```

```
inistate,set,dtyp,,
```

```
D,ALL,ALL
```

```
!INISTATE,READ,Fname,Ext,Path
```

!Read initial state data from a standalone initial state file of the specified

!name (Fname) and file name extension (Ext), located in the specified path

!(Path). The initial state file must be in a comma-delimited ASCII file format,

!consisting of individual rows for each stress/strain item, with each row

!consisting of columns separated by commas.

In the solution settings:

```
/POST1  
SET, LAST  
NMAX=NDINQR(0,13)  
nodo=0  
*CFOPEN, Reactions, mac  
*DO, I, 1, NMAX  
nodo=NDNEXT(nodo)  
*GET, ReactX, NODE, nodo, RF, FX  
*GET, ReactY, NODE, nodo, RF, FY  
*GET, ReactZ, NODE, nodo, RF, FZ  
*VWRITE, nodo, ReactX  
( 'F, ', F10.0, ', ', FX, ', ', F20.12)  
*VWRITE, nodo, ReactY  
( 'F, ', F10.0, ', ', FY, ', ', F20.12)  
*VWRITE, nodo, ReactZ  
( 'F, ', F10.0, ', ', FZ, ', ', F20.12)  
*ENDDO  
*CFCLOS
```

To apply further loads to the imported stress results another static structural module must be created, then the below command is applied to the first analysis time step only.

```
/SOLU
inistate,set,dtyp,stress
INISTATE, READ, StressG, ans
inistate,set,dtyp,,
/INPUT,'Reactions','mac','D:\users\30_Assistenza\inistate2\inistate2_files\dp0\SYS\MECH\'

!INISTATE, READ, Fname, Ext, Path
!Read initial state data from a standalone initial state file of the specified
!name (Fname) and file name extension (Ext), located in the specified path
!(Path). The initial state file must be in a comma-delimited ASCII file format,
!consisting of individual rows for each stress/strain item, with each row
!consisting of columns separated by commas.

!/INPUT, Fname, Ext, Dir
!Fname: File name and directory path (248 characters maximum, including the
!characters needed for the directory path). An unspecified directory path
!defaults to the working directory; in this case, you can use all 248 characters
!for the file name. The file name defaults to the current Jobname if Ext is
!specified.

!Ext: Filename extension (eight-character maximum).
!Dir: Directory path (64 characters maximum). Defaults to current directory.
```

Appendix 3 – Creep curves

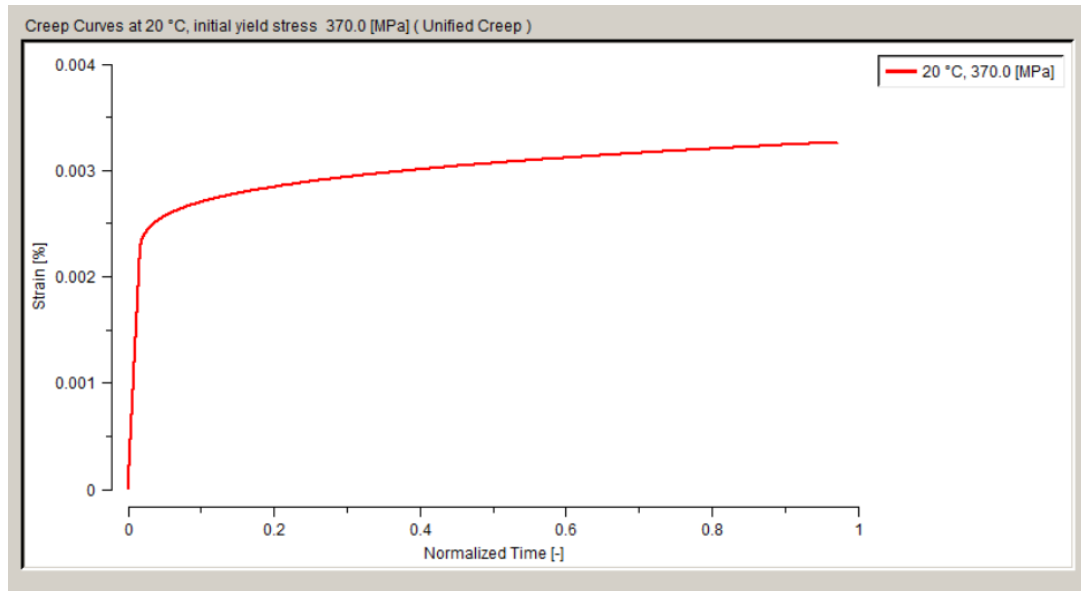


Figure A3.1 Creep curve defined for SiMo 5.1 20°C

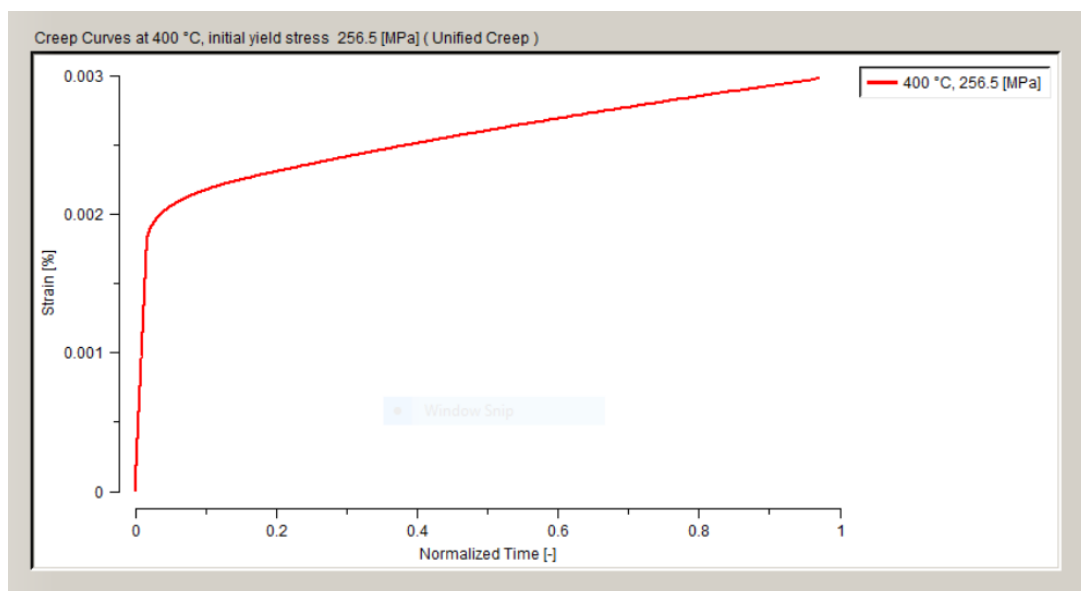


Figure A3.2 Creep curve defined for SiMo 5.1 at 400°C

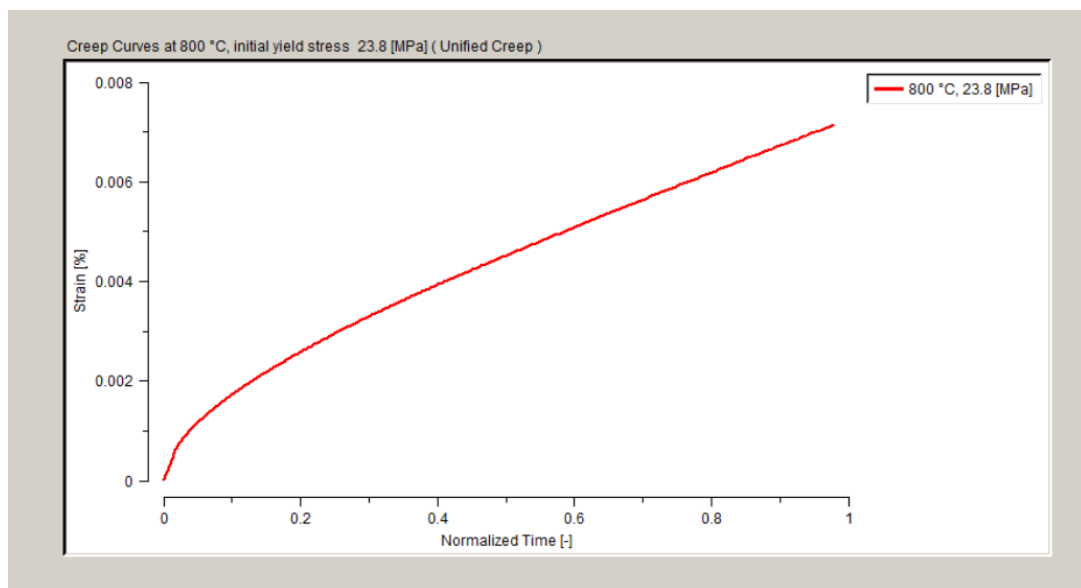


Figure A3.3 Creep curve defined for SiMo 5.1 at 800°C

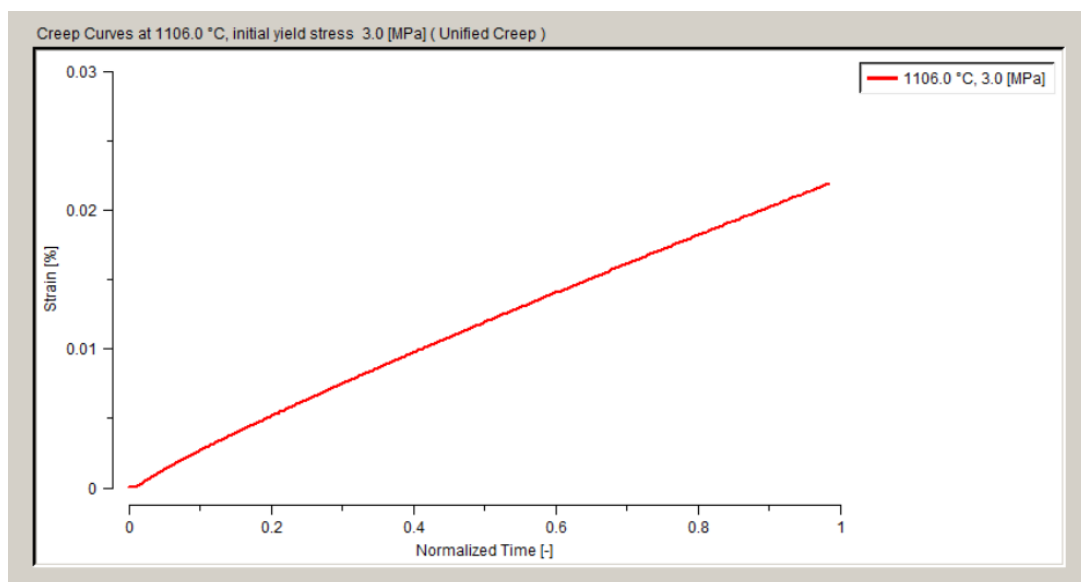


Figure A3.4 Creep curve defined for 20°C for SiMo 5.1 at 1106°C