A Novel Process for Forming T-Section Components with Low Residual Stresses in Aluminium Alloys

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I, Ran Pan, declare that the work presented in this thesis has been undertaken by myself, except where explicit reference and acknowledgement is made to the contribution of others. No portion of the work in this thesis has been submitted in support of an application for another degree or qualification in this or any other university or other institute of learning.

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ABSTRACT

The overall aim of this study is to develop a process for manufacturing extra-large aviation components with low residual stresses to minimise their subsequent distortion during the final machining process. The research concentrates on characterising and modelling the residual stress distribution at different stages of the manufacturing process, i.e. solution heat treatment (SHT), quenching and the subsequent residual stress relief. In addition, an effective method has been developed for manufacturing T-shaped components with low residual stresses.

Compression tests were conducted to determine the temperature dependent mechanical properties of AA 7050 aluminium alloy. Based on the experimental results, the visco-plastic material behaviour was characterised for a range of test temperatures and strain rates. A dislocation-based constitutive model, describing the thermo-mechanical response of AA 7050 Al alloy in a condition corresponding to the manufacturing process, was developed. Values of material constants in the model were calibrated from isothermal compression test results.

To quantify the residual stress evolution in the material during the manufacturing process, residual stress measurements were performed using neutron diffraction, X-ray diffraction and contour techniques on quenched and quenched & cold rolled specimens. The experimental data was used to verify finite element simulations of the residual stress mitigation processes, enabling residual stress predictions to be made. To study the effect of cold rolling on laboratory scale T-section components, a roll assembly was designed and manufactured. According to the measurement results, after cold rolling, the compressive stresses due to quenching at the surfaces of material were removed, though tensile residual stresses were induced near the surface. Generally, the stress predictions from the FE models were in close agreement with the measurement results which show that the FE results are reliable.

Feasibility studies were performed to mitigate quench-induced residual stresses in extra-large T-section component. An integrated model was built to predict the effectiveness of cold rolling on residual stresses in heat-treated samples. The
optimal parameters (incl. rolling deformation ratio and roll diameter) to minimise residual stresses in scaled-down T-section panels were predicted. Based on these simulation results, the integrated numerical models of cold compression and rolling on residual stress distribution in large-sized T-section components were developed.

To reduce the residual stress in large-sized heat-treated T-section components, this study showed that, although cold compression without overlap can effectively relax residual stress in the material, for the case of multiple compressions with overlap regions, the residual stresses distribution post-compression are still sufficient to lead to component distortion. The cold rolling technique is a more cost-effective cold working technique to improve the residual stress distribution in T-profile large-sized components, compared with multiple cold compressions technique. For the cases considered, for reducing residual stresses in extra-large T-section components the FE models also indicate that the optimised parameters to relax residual stresses in the component is a deformation ratio of 1.5 % via a set of rolls with 400 mm radius. These results provide a valuable guideline for future industrial production activities.
Nomenclature

\( \sigma_y \) yield stress of AA 7050

\( K \) drag stress

\( E \) Young’s modulus

\( H \) isotropic hardening parameter

\( H_{\text{plate}}, H_{\text{rib}} \) thickness of the plate and ribbed region in the T-Section component

\( \sigma \) total stress

\( \alpha_k, \alpha_B \) Expansion coefficients in the constitutive model

\( \sigma_v \) Mises equivalent stress

\( \bar{\rho} \) dislocation density

\( \varepsilon_p \) plastic strain

\( \varepsilon_T \) total strain

\( \Delta \varepsilon_p \) visco-plastic strain increment

\( \Delta \varepsilon_e \) elastic strain increment

\( \Delta \varepsilon_t \) thermal strain increment

\( \Delta \varepsilon_T \) total strain increment

\( A, B, C, n_1, n_2 \) material constants in the constitutive model

\( Q_K, Q_{C_1} \) Constants for the Arrhenius laws in the constitutive model

\( Q_E, Q_{n_1} \) Constants for the Arrhenius laws in the constitutive model

\( \Delta t \) Time increment
Three consecutive positions at the heat transfer path, \( i \) the serial number. \( \Delta y \) temp spatial increment

\( y_{s,1}, y_{s,2} \) is the nearest to the surface (along the heat transfer path), the position \( y_{s,2} \) next to \( y_{s,1} \) towards the core of block

\( T_{y_i}^t, T_{y_{i-1}}^t \) the temperatures along the heat transfer path at different positions \( y_i, y_{i-1} \) and \( y_{i+1} \) at time \( t \)

\( T_{y_i}^{t-\Delta t}, T_{y_i}^{t+\Delta t} \) the temperatures of a point along heat transfer path at time \( t - \Delta t \) and \( t + \Delta t \)

\( T_{y_{s,1}}^t, T_{y_{s,2}}^t \) \( T_{y_{s,1}}^t \) represents the temperature of \( y_{s,1} \) at time \( t \), \( T_{y_{s,2}}^t \) is the temperature of \( y_{s,2} \) at time \( t \).

\( T_{\text{surface}}^t, T_\infty^t \) \( T_{\text{surface}}^t \) is the surface temperature at a time \( t \), and \( T_\infty^t \) is the temperature of quenchant.

\( k_{tc} \) The temperature dependent thermal conductivity of 7050 aluminum alloy.

\( \rho_d \) The temperature dependent density of 7050 aluminum alloy

\( c_p \) The temperature dependent specific heat of 7050 aluminum alloy

\( \alpha_d \) The temperature dependent thermal diffusivity of 7050 aluminum alloy.

\( q_c', q_h' \) \( q_c' \) represents the heat convection flux per unit area, \( q_h' \) represents the heat transfer flux per unit area

\( h \) Heat transfer coefficient between 7050 aluminum alloy and water

\( \theta_{hkl}^0, a_{hkl}^0 \) \( \theta_{hkl}^0 \) and \( a_{hkl}^0 \) are the values of reference Bragg angle and lattice spacing.

\( \Delta \theta_{hkl}, \Delta d_{hkl} \) \( \Delta \theta_{hkl} \) and \( \Delta d_{hkl} \) are the values of Bragg angle and lattice spacing increment
\( \varepsilon_{hkl} \) Elastic strain of the \{hkl\} crystallographic plane

\( E_{hkl} \) and \( \nu_{hkl} \) are the elastic modulus and Poisson’s ratio of the \{hkl\} crystallographic plane

\( \sigma_{ii} \) and \( \varepsilon_{ii} \) are the direct stress and strain, \( ii \) (11,22,33) is the related index
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Chapter 1  Introduction

1.1 Background

In order to make commercial airline larger and safer, remarkable progress in the strength to weight ratio of aluminium alloys and improvements in manufacturing methods e.g. extrusion, stamping, forging thick plates, and heat treatments, such as over-aging and retrogression and re-aging, have been made. In the 1930s, experiments with various levels of alloying elements in aluminium and copper led to some 2000 series aluminium alloys [1]. Since the 1940s, due to the requirement of stronger transport aircraft, the integral technology uses larger mechanical structure to minimize the number of components and joints [2]. Since in the form of a thick sheet, the early 2xxx series alloys were susceptible to exfoliation corrosion, the material investigators found that if both zinc and magnesium elements could be added into aluminium alloys, they will have significantly higher strength than the early 2xxx alloys. As a material used in early stage transport aircraft, AA7075 is one typical representative of this kind of materials [1, 3, 4].

Typically, residual stresses are statically self-equilibrium within a body without any external load. For heat-treated components, during the machining process, the surface layers that compressive stresses exist are removed, leading to the redistribution of residual stresses and achieving a new stress equilibrium condition. At this stage, stress release during machining can lead to at least partial distortion of a large-sized component. For most aviation components, machining is usually the final step of the manufacturing process which decides the profile of a final component. Given the cost and safety factors, for large-sized aviation components, part distortion during machining process or service life is a very expensive problem. According to European collaborative project’s estimation, tens of millions of Euro are spent on investigating how to avoid or remove distortion in aerospace aluminium alloy components [5, 6].

To protect potential distortion caused by residual stresses, over more than 80 years, a series of stress relaxation techniques have been performed. Additionally, developed 2000, 7000 series aluminium alloys which have good performance and higher SCC resistance and novel aluminium alloys e.g. Al-Li alloys have made high strength aluminium alloys remain important in airframe construction despite the increase in the application of composite materials in aviation components, as shown in Figure 1.1.
The percentage of material used in large-sized passenger aircrafts. The data is taken from [7-10].

However, the problem of part distortion caused by large residual stresses in aviation components is still not fully solved. According to aircraft manufacturer requirements, the acceptable distortion in the final product should not be beyond 0.025 mm [11]. For heat-treated extra-large aluminium alloy panel component with more than several meter length, unacceptable distortion still appeared due to the remaining sufficient residual stress after cold working levelling technique and the subsequent over-aging treatment [12].

For large-size aviation components, quite a few have the T-profile cross-section, as shown in Figure 1.2. Aviation Industry Corporation of China Ltd is now fabricating T-type large-sized AA7050 wing spar caps which are 5600 mm long. Extra-large aviation spar caps manufactured by AVIC Ltd have not been stress relieved. During the final machining period, the relaxation of the thermal residual stresses leads to the occurrence of part distortion [13].
The schematic of several T-section components a) Fuselage stringer number 16 [14] b) Spar cap of wing [15]. Since the aviation component with complicated cross section shown in Figure 1.3 cannot be cold compressed directly for reducing thermal-induced residual stresses. The current fabrication method involving cold compression is to firstly produce a T-section panel. The simple T-section configuration means the material can be easily uniform plastically deformed during cold compression so that the thermal induced residual stresses could be effectively removed. The final step of the whole forming process is to machine the T-section into the target shape. (The reason to use a T-section panel but a rectangular block is that in this way less material will be machined during the final machining step) To obtain the aviation component, about 60 - 70% material needs to be removed by machining [16].
To relieve the residual stresses in extra-large components, multiple cold compression has been adopted by AVIC Ltd. However, after multiple cold compression, residual stresses in heat treated components did not fully relax under localized compressive stresses. Therefore, an investigation into the reasons behind the distortion phenomena and the corresponding solution for this problem is required. After comparing several stress relief methods, cold rolling, as a kind of cold working method, has been studied to replace the current manufacturing method. The results can provide a guideline for the future industrial production.

1.2 Aims and objectives

The overall aim is to systematically study a novel process to manufacture extra-large T-section aluminium alloy components with low residual stresses, using experimental and numerical techniques. To realise these aims, the following objectives were set:

1. Review and evaluate the current residual stress relief methods, compare their advantages and disadvantages to judge if they are appropriate to reduce residual stresses in extra-large components.
2. Improve a constitutive material model, for incorporation into a finite element analysis, to describe the material deformation behaviour during the manufacturing process.
3. Develop and experimentally validate a finite element model to simulate a components manufacturing process and predict the residual stress distribution.
4. Investigate the influence of cold working techniques on residual stress evolution in heat-treated aluminium specimens. Quantify the magnitude and distribution of residual stresses in these samples by finite element predictions validated by experimental measurements.
5. Provide guidelines to industry on the optimal methodology and parameters to manufacture T-Section components with low-residual stresses.
1.3 Structure of the thesis

The background of AA 7050 aluminium alloy and the mechanism of the material model used in this study are demonstrated in Chapter 2. This chapter also contains an overview of previous research involving investigations about the influence of quite a few stress relief methods on residual stresses in materials. The principle of the used destructive and non-destructive measurement methods are also illustrated in Chapter 2.

To accurately describe the material behaviour of 7050 aluminium alloy throughout the manufacturing process, fundamental experiments were performed. Chapter 3 presents the corresponding Gleeble tests for the strain-stress curve of 7050 Al alloy under various conditions. Chapter 4 describes the numerical and experimental processes of reducing thermal-induced residual stresses and the related experimental assessment of residual stresses in Aluminium specimens. It contains the comparison between stress measurements through neutron, X-ray, contour methods and FE predicted results.

As it is not possible to conduct experiments for extra-large T-section components in the laboratory, the scaled down specimens were used. Numerical models for the quenched & cold rolled T-section specimen were built and the surface stress distributions of these specimens were obtained in Chapter 5. In addition, it also details the residual stresses distribution in extra-large T-section components after multiple compression via FE prediction and predicts the cold rolling effect on residual stress in real-size T-section components. The conclusion and future work are detailed in Chapter 6.

Appendix A includes the experimental detail used in calculating heat transfer coefficient between solution heat treated 7050 Aluminium alloy and water. Appendix B contains the details about the design of the specific rolls for the scale down T section components.
Chapter 2  Literature review

2.1 Introduction

The exploitation of heat-treated aluminium alloy is one of the main technical breakthroughs that provided an incentive for the rapid development of the aviation industry. Therefore aircraft designers are provided with a low-density and relatively high strength-to-weight ratio material that is available to be fabricated into complicated shapes and different sizes. As large-sized aircraft have become popular, problems resulting from macroscopic residual stresses begin to appear. This chapter contains the origin and character of thermal-induced residual stresses and their relationship with the manufacturing process of aviation components.

Although in past decades quite a few of residual stress relaxation methods have been created and applied in industry, with the development of large aircraft, it is increasingly difficult to relieve residual stresses in extra-large aviation components. This chapter also includes a review of published residual stress relief techniques for aviation materials, where residual stresses have been generated due to thermal or mechanical loading. In this study, cold rolling and cold compression, as the residual stress relaxation methods, are thoroughly investigated. Furthermore, an introduction to key residual stress measurement methods has been described.

As most residual stresses evolution in materials are related to external loadings, it is important to study the material deformation behaviour under loading during service period and moreover predict the residual stresses redistribution in materials. For most metallic materials, their physical performance in response to external loading can be described by a set of constitutive equations. The constitutive equations and the corresponding material parameters are used to illustrate the deformation behaviour of a sample under operation conditions. In this chapter a review of deformation mechanics for AA 7050 material, relevant to this study, is detailed. A material model that defines elastic and plastic deformations of AA 7050 Aluminium alloy is also presented in this study.
2.2 Type AA7xxx series Aluminium alloy and its Material Properties

2.2.1 Test Material Description

AA 7050 aluminium alloy has been widely employed in aviation industries because of its attractive physical properties, e.g. excellent corrosion resistance and good formability. This kind of material can be formed into fuselages and wing ribs of the most existing Airbus aircraft. 7050 aluminium alloy is a kind of Al-Zn-Mg-Cu heat treatable alloy. High zinc and magnesium content are the two main additive elements which can improve the tensile strength and corrosion resistance of the material and a high copper content can help the supersaturated solid solution phase to be retained at room temperature after quenching. The chemical composition of the aluminium alloy 7050 considered in this work is given in Table 2.1.

<table>
<thead>
<tr>
<th>Element</th>
<th>Cu</th>
<th>Zn</th>
<th>Si</th>
<th>Fe</th>
<th>Mg</th>
<th>Ti</th>
<th>Zr</th>
<th>Al</th>
</tr>
</thead>
<tbody>
<tr>
<td>wt.%</td>
<td>2.25</td>
<td>6.43</td>
<td>0.04</td>
<td>0.07</td>
<td>2.11</td>
<td>0.04</td>
<td>0.10</td>
<td>Bal.</td>
</tr>
</tbody>
</table>

2.2.2 General flow features

It is generally accepted that there are two typical styles of stress-strain curves corresponding to various deformation mechanisms of Al alloy, and the schematic is as shown in Figure 2.1 [17]. Curve A (in Figure 2.1) represents the dynamic recovery phenomenon of the material during deformation which reveals that dynamic recovery is the only softening mechanism. In this case, the stress magnitude will gradually increase to a critical value which is considered as the saturation flow stress $\sigma_s$. Curve B demonstrates that the dynamic recrystallization phenomenon appears during high-temperature deformation. If dynamic recrystallization occurs, the flow stress of material will exceed the peak flow stress $\sigma_p$ and then become steady to achieve a constant value $\sigma_{ss}$. 
2.2.3 Constitutive material model

Constitutive models are usually applied to describe material behaviour. Such models originate from simple power-law relationships, such as the Estrins et al.’s [18] isotropic hardening model. They belong to mixed isotropic-kinematic hardening models where the deformation is dependent on the material microstructure e.g. grain size, recrystallization, and even loading history. In this case, to investigate the stress evolution of T-section components during the whole forming process, an accurate thermo-mechanical material model should be built. The aim is to build a material model to simulate the metal flow of the thermal-induced residual stress evolution during quenching and the subsequent cold working processes. In general, the constitutive equations build the relationship between stress-strain behaviour, strain rates and deformation temperatures.

A review of the material laws to define the aforementioned plastic deformation is presented in this section. In the 1920s, a flow law was created [19, 20] to express the relationship between the stress to plastic strain rate $\dot{\varepsilon}_p$ [19]. This equation can be transferred in another type as shown in Equation (2.1)

$$
\sigma = K(\dot{\varepsilon}_p)^{1/n}
$$

$$
\dot{\varepsilon}_p = \left(\frac{\sigma}{K}\right)^n
$$

where $\sigma$ is the equivalent stress, $E$ is Young’s modulus, $K$ and $n$ are constants that are fitted to experimental stress strain curves. However, this equation does not include the softening phenomena.
To accurately describe material behaviour during plastic deformation, in the 1975s, Sandström and Lagneborg et al [21] related the hardening stress to the square root of dislocation density which can be seen as a mis-orientation on the cell walls.

They define the dislocation density $\rho$ fluctuates between the fully annealed state $\rho_0$, the initial dislocation density $\rho$, and $\rho_{\text{max}}$ a state fully saturated. The normalised dislocation density $\bar{\rho}$ fluctuates between 0 and 1, for the fully annealed and fully saturated states, respectively, as illustrated in Equation (2.2).

$$\bar{\rho} = \frac{\rho - \rho_0}{\rho_{\text{max}} - \rho_0} \quad (2.2)$$

$$H = m \tau_w = m \alpha_1 G b \sqrt{\bar{\rho}} = \alpha G b \sqrt{\bar{\rho}} \quad (2.3)$$

In Equation (2.3), $H$ is hardening stress, $m$ is the Taylor factor i.e. empirical value, $\tau_w$ is a stress which is related to the dislocation density $\bar{\rho}$ on the cell walls [18], $\alpha_1$ is a material constant, $\alpha$, a constant around 0.25 which is the product of $m \alpha_1 G$ is the shear modulus and $b$ is the magnitude of the dislocation burgers vector.

Provided the material deformation mechanisms, static and dynamic recovery, a normalized dislocation density based constitutive equation introduced by Lin et al [22] could be described as,

$$\dot{\bar{\rho}} = \left( \frac{d}{d_0} \right)^{Y_d} Y_d (1.0 - \bar{\rho}) |\dot{\varepsilon}^P| - C_1 \bar{\rho}^{C_2} \quad (2.4)$$

where $d_0, Y_d, C_1$ and $C_2$ represent material constants. The first term of this equation illustrates the growth of dislocation density caused by plastic strain and the dynamic of the dislocation density. In this case, the coefficient $\left( \frac{d}{d_0} \right)^{Y_d}$ represents the influence of average grain size $d$ on normalized dislocation. As the average grain size $d$ in the material increases, the dislocation density would also increase as well during visco-plastic deformation because less grain rotation and grain boundary sliding occur. In other words, the term $\left( \frac{d}{d_0} \right)^{Y_d}$ describes the relationship between the deformation mechanism of grain rotation and grain boundary sliding on the growth rate of dislocations in material, as shown in Equation (2.4). Additionally, the term $(1.0 - \bar{\rho})$ means the dynamic recovery of material ensures the normalized dislocation density could be limited to the saturated state of a dislocation map.
According to Ashby et al [3], if two dislocations have opposite signals and share the same slip plane, they will annihilate each other when they meet. Especially when a material is exposed at high temperature, the influence of this softening mechanism on the structure of the material is remarkable. The second term of Equation(2.4), $C_1\bar{\rho}C_2$, describes the influence of static recovery on the normalized dislocation density. It originates from Sandstorm and Lagneborg et al’ s [21] equation $\dot{\rho} = \dot{\varepsilon}^p/(bl) - 2M\tau\rho^2$ in which the $\dot{\varepsilon}^p$ is the plastic strain rate, $l$ is the dislocation mean free path, $M$ is the mobility of grain boundaries and $\tau$ is the average energy per unit length of a dislocation.

In this case, the effect of both $M$ and $\tau$ on the normalized dislocation density is simplified as $C_1$. To improve the flexibility of the equation so as to make it describe various materials and deformation conditions, the exponent constant $C_2$, a variable, is introduced into the Lagneborg et al’ s equation and derive it to $\dot{\rho} = \dot{\varepsilon}^p/(bl) - 2M\tau\rho^{C_2}$.

To describe the material behaviour under elastic-viscoplastic deformation, Cao and Lin et al [23] introduced a set of the constitutive equations as follows:

$$\dot{\varepsilon}_p = \left[\frac{\sigma_v - H - \sigma_y}{K}\right]^n$$  \hspace{1cm} (2.5)

$$\dot{\rho} = A(1.0 - \rho)\dot{\varepsilon}_p - C_1\rho^{C_2}$$  \hspace{1cm} (2.6)

$$\sigma = E(\varepsilon_T - \varepsilon_p)$$  \hspace{1cm} (2.7)

where $H = B\sqrt{\rho}$ originating from Equation (2.3), which is related to the increment of dislocation density, represents the material hardening. $E$ is Young’s modulus and $\sigma_y$ is the yield stress of the material. As mentioned in Equation (2.5), $K$ and $n$ are material constants to describe the hardening behaviour, amoung them, $K$ represents drag stress and $n$ is the power law of hardening exponent. $A$ is a material constant which has a relationship with the average grain size of a material. As above, $C_1$ and $C_2$ are material constants.

For the cold working processes of manufacturing T-section component, e.g. cold rolling/compression, the 7050 aluminium alloy material could be deformed elastically and visco-plastically. Hence, the dislocation-based visco-plastic constitutive material model, i.e. Equations (2.5) - (2.7), has been employed in this study.
2.3 Review of techniques to relieve residual stress in large-sized heat-treated specimens

Many structural components are fabricated from heat-treated aluminium alloys, which continue to be a leading aviation material since the 1940s. Through solution heat treatment and quenching, precipitation hardening can significantly strengthen alloys’ performance. Quenching can however introduce large residual stresses into components, especially large components (> 50 mm) leading to component distortion and possibly fracture [9, 10, 24]. Hence, residual stress mitigation techniques have been widely examined over recent decades.

This section summarizes the origin of residual stress and the effectiveness of various methods to minimize residual stresses in large-sized heat-treated components. Several approaches were included.

2.3.1 Overview of residual stress

The stresses that exist in a material or component in the absence of external loadings or thermal gradients can be defined as residual stress. The residual stress cannot be measured directly. The corresponding residual strain is measured by residual stress measurement techniques and the residual stresses can be calculated using the material properties, e.g. Young’s modulus and Poisson’s ratio.

Residual stresses are generated in most of the forming processes including heat treatment, material deformation, machining, chemical action or other operations that could change the shape or the properties of a material. From a variety of sources, it can exist in the un-deformed raw material, develop during manufacturing or can be induced by in-service loading. In some cases, the combination of residual stresses and external loading could even be large enough to induce local yielding and plastic deformation, both on a microscopic and macroscopic scale, and consequently influence the performance of the component. Therefore, it is critical to investigate the internal stress distribution of material through measurements or finite element predictions.

When designing a forming process of a component, it is necessary to consider the magnitude and distribution of residual stress as they are vital to the component performance. Generally, stress equilibrium is always kept in any independent
component. In other words, a tensile residual stress in a component must be balanced by a compressive residual stress in the material.

Although lots of sources can lead to residual stresses in material, they mainly could be classified into two groups. Among them, mechanical-induced residual stresses are caused by non-uniform plastic deformation due to various manufacturing processes. Thermal-induced residual stresses are usually the result of non-uniform heating or cooling operations. For the later, with the increase of component size, during heating or cooling, the temperature difference in the material could be increasingly significant and the large residual stresses are generated. This kind of residual stress mentioned above is TYPE I residual stress which is on a macroscopic level. The macro residual stresses vary continuously over large distances in the body of the component. TYPE II RS extends over grain level and TYPE III RS exists at the atomic level at interfaces and dislocations [25].

2.3.2 Effect of residual stress

In general, tensile residual stress is undesirable in the material as they can accelerate fatigue failure, stress corrosion cracking, etc.,. Quenching of aluminium alloys always leads to a compressive residual stress around the material surface and tensile residual stress at the core part of the material. Hence, it is inevitable that distortion occurs during the machining process after quenching. Another example is welding, and it is often found that cracking occurs around the weld region. Since significant plastic deformation occurs in the fusion zone and surrounding heat affected zones (HAZ), therefore the residual stress introduced by strain misfit is created between such regions and the parent material [26]. The measured result shows that in the weld region the tensile principle residual stress magnitude is much larger than the uniaxial yield stress of material at room temperature, proof of the complexity of stress redistribution in a material after welding. Additionally, research also illustrates that tensile residual stresses induced by quite a few manufacturing processes, such as wire drawing, surface machining (milling, tuning) [27], can adversely affect the fatigue life of components.

With the same stress magnitude, the tensile residual stress close to the surface region could be more detrimental to components. In most cases, large tensile surface residual stress means a large sufficient mean stress which could strongly influence the crack growth rate regimes during fatigue [28]. Hence, the existence of tensile surface residual stress combined with the service stress could seriously reduce the component life.
Although the tensile residual stress in the core part of material has limited effect with the crack growth, e.g. quenching, once a crack or external effect does penetrate the inner part which is under tension, the crack or material distortion could develop rapidly and catastrophically.

However, residual stresses can also make a contribution on prolonging components’ in-service life if they are introduced deliberately. Significant advantages can be achieved if compressive residual stress could be applied to the surface region of a material. Researches have proven that compressive residual stresses at material surfaces are generally beneficial as they not only increase fatigue strength but resistance to stress-corrosion cracking. Furthermore, compressive surface residual stress could effectively protect a material from the cracks caused by low amplitude high cycle fatigue. For example, shot peening is widely adopted in exerting compressive stress at the component surfaces [29].

2.3.3 Cold working techniques of relaxing residual stresses in large-sized component

In recent decades, cold working techniques, such as cold compression or stretching, were adopted to make as-quenched components uniformly plastically deformed so as to relax residual stress effectively. Hence, for plates, bars, extrusions, simple symmetrical shapes, the plastic deformation for stress relief is performed in tension or compression for reasons of practical convenience and attainable uniformity [1, 30-32].

Mechanical methods, such as cold compression and cold stretching, are based on the theory below. The same deformation ratio is imposed on the areas suffering from tensile and compressive residual stresses which make the material yield. As a result, the strain differential induced by quenching in the elastic region can be significantly reduced. This symbolizes a smaller residual stress differential in the plastic zone of the stress-strain curve. After unloading elastically, the stress difference between both tensile and compressive residual stresses is still kept as identical as they were during the loading process. Hence, the differential between both the largest residual tension and compression is significantly relieved.

For this case, although the as-quenched 0.2 % proof stress of the surface and core locations should be the same, the work-hardening behaviour of both differs [33]. The work hardening at core location has a faster rate than that of the surface. It is simply because during the quenching, the phase precipitation in the core part is slower than
the surface. In reality, it could differ from case to case as work-hardening behaviour varies through the thickness [34].

For relaxing residual stresses in aviation components, cold compression and cold stretching have been adopted by many researchers and industries. 2% cold compression/stretching were applied by Koc et al [35] to remove residual stresses in an AA 7075 aluminium alloy block with size 1026×406×127 mm$^3$. Figure 2.2 illustrates the effect of mechanical stress levelling techniques on heat-treated blocks. It can be seen that more than 90% residual stresses were reduced when the deformation ratio is 2%. When 1% compression ratio was applied, the whole material was not fully plastically deformed.

![Graphs showing the effect of mechanical stress levelling techniques on heat-treated blocks.](image)

Figure 2.2 The geometry of specimen and x-, y- and z-component residual stresses of samples along y-axis after quenching, 1% compression ratio (without lubricant), 2% compression ratio (with lubricant) and 2% stretching ratio (data from [35]).

In addition, the forging/stretching methods were also adopted by Younger et al [36] to mitigate the residual stresses and the corresponding machined distortion in heat treated aluminium alloy satellite box. They found over 84% of stress magnitude can be reduced after mechanical stress levelling.
Baburamani et al [37] tried to relieve residual stresses in an AA7085 aluminium alloy block with size $1750 \times 1200 \times 152 \text{ mm}^3$ via the cold compression technique. The stresses' results measured via X-ray diffraction and slitting methods show that thermal-induced residual stresses of over 200 MPa was reduced to tens of MPa. In addition, for the relatively thick component, the authors found the increase of hardness may be in direct proportional to the change of residual stress magnitude. For thick forged specimens, the stress magnitude can be affected by the temperature gradient, material inhomogeneity, the extent of material plastically deformed during cold working and the strain hardening behaviour.

Although residual stresses in heat-treated components can be significantly relaxed by cold stretching or compression with plastic deformation ranging from 1 – 5 %, these techniques are limited by configuration, shape and size [38].

For instance, although for aluminium alloy components with relatively small volume (length < 200 mm), the residual stress magnitude in a material can be reduced by a factor of about 10 via cold stretching [26], for a large-sized component which is more than 5000 mm long and >120 mm thick, it is hard to apply cold stretching to relieve the residual stresses in this kind of component due to handling difficulty and asymmetric loading [35].

Zheng et al [12] studied the residual stress distribution in a heat-treated curved extra-large T-section component and the influence of the cold forging process after quenching, as shown in Figure 2.3. For this kind of component, it is difficult to make the whole component uniformly plastically deformed via cold stretching technique. Even for cold compression method, preparing a set of specific extra-large die is also expensive and impractical. Although the residual stress magnitude of the heat-treated component is unavailable, it is mentioned the relaxation of residual stress is ranging from 43 % - 79 %.
As illustrated in Figure 2.4, multi-step compression was applied during the stress relief process and the overlap region is 500×760 mm². It can be seen in Figure 2.5 that after cold compression, the relatively large $\sigma_{zz}$ tensile stress still exist in the core part of the material and at the bottom surface of the specimen tensile residual stress (up to 95 MPa) began to appear.
In addition, their contour result also reveals the phenomena that the remaining residual stresses in quenched & cold compressed material are still possible to lead to partial distortion of aviation components during machining processes. Tanner et al, Prime et al and Pan et al [39-41] believe that non-uniform plastic deformation due to multiple compression induced the complicated stress distribution in the material. Hence, given the disadvantages of the current cold working techniques, to minimize residual stress magnitude in an extra-large component, cold rolling is considered to be a suitable alternative studied as is discussed in the following chapters.

2.3.4 Thermal stress relief techniques for mitigating residual stresses in large-sized components

For large-sized aviation components, it has been concluded in Zhang et al [12] that extra-large components with relatively complicated configuration cannot be stress relieved economically via multiple cold compression or stretching. In contrast, the non-uniform plastic deformation will not be a problem when thermal stress relaxation methods are applied in reducing residual stresses in these large-sized products.

2.3.4.1 Precipitate heat treating

The influence of aging treatment on residual stresses magnitude and distribution has been studied by many researches [2, 36, 42-44]. Remarkable residual stress reduction is not detected during long-term natural aging. The residual stress evolution of quenched AA 7010 specimen was tracked by Tanner et al [45] and as shown in Figure 2.6 that, the stress magnitude did not decrease significantly.

![Figure 2.6 surface residual stresses at room temperature for cold water quenched AA 7010 aluminium alloy characterised by X-ray diffraction (Taken from [46]).](image-url)
The effectiveness of artificial aging on alleviating residual stress magnitude relies on quite a few factors e.g. alloy type, aging temperature, time, etc. Provided the extent of precipitation required to generate necessary mechanical properties. The aging temperature that applies to aviation alloys usually ranges from 100 - 190 °C. For quenched AA 7075 aluminium alloy, Robinson et al [47] found that a two-step over-aging treatment (105 °C for 7 h and 175 °C for up to 128 h) can lead to the thermal-induced residual stress relaxation in AA 7075 aluminium alloy from 25 % to 40 %. They also found that for cold water quenched AA 7050 aluminium alloy, after the over-aging treatment (120 °C for 6 h and 175 °C for 8 h), the surface compressive residual stress magnitude decreases from 210 MPa to 160 MPa. The effect of aging process parameters, e.g. aging time and aging temperature on residual stresses in post-quench E319 cast aluminium alloy has been studied extensively by Larry et al [48]. It can be concluded from the research that although aging temperatures less than 200 °C are sufficient to induce some dislocation motion so that residual strain can be alleviated to some extent, the extent of relaxation is nominal and cannot be quantified.

Although some researchers have proven that over-aging can typically relax residual stresses by up to 40 % [47, 49], the temperature and the related aging period needed to induce significant residual stress relief also promote the precipitation of coarse and fragile phases. Younger et al [36] found that increasing of aging temperature at the second stage of the over-aging process can lead to the drop of the yield stress and ultimate strength of AA 7075 aluminium alloy, as shown in Figure 2.7. The applied over-aging treatment is to keep the quenched specimen at 107 °C for 6 - 8 h and then the temperatures below for 8 - 10 h.

In addition, the influence of retrogression and re-aging (RRA) on residual stresses was also investigated [42, 49-52]. It is well-known that the aim of the RRA treatment is to improve the strength and stress corrosion resistance. It can also be seen in Figure 2.8 that for quenched AA 7449 aluminium alloy specimen, the residual stresses reduction due to RRA treatment ranges from 19 % - 25 %. Similar conclusions have also been made for applying RRA treatment to AA 7010 aluminium alloy [50]. However, for AA 7050 aluminium alloy, the RRA treatment only has a nominal effect on residual stress relaxation [2]. Hence, for a quenched component, the longer period of exposure at aging temperature will lead to a relatively small additional amount of stress relaxation, besides cold working stress relief techniques, but the amount is hard to quantify and it is of little practical meaning.
Figure 2.7 Effect of second step over-aging temperature on yield strength and ultimate tensile strength of AA 7075 aluminium alloy (Data from [36]).

Therefore, even if the increasing activated energy due to aging can lead to some dislocation motion and climb to make residual stress relaxed, the amount is relatively limited compared to mechanical stress levelling. However, over-aging and RRA treatments are adopted in manufacturing large-sized aviation components, the main
target of applying these treatments is improving the physical performance of final products, apart from causing an approximately 20 % stress relaxation [12, 36, 49].

2.3.4.2 Uphill quenching

As aging type heat treatment can only reduce no more than 40% residual stresses in a material, the effect of other thermal treatment methods on residual stresses has been investigated. Since non-uniform plastic deformation due to thermal gradient during quenching can induce large residual stresses in a material, hence significant effort has focused on reversing the procedure of the temperature differential imposed during SHT and rapid cooling so as to relax the thermal-induced residual stresses. This kind of methods is called uphill quenching/cryogenic treatment. As shown in Figure 2.9, the as-quenched material is further cooled to -180 - -210 °C via immersing in liquid nitrogen or dry ice. After the temperature equilibrium is achieved, the specimen should be rapidly transferred to an atmosphere with the elevated temperature medium (aging temperature). Then the sample should be aged for a specific period. Researchers [53-56] have found that at the beginning of the second heating stage of Figure 2.9, maximising the temperature variation between the surface and core parts of material and improving the surface temperature as soon as possible are the key to effectively mitigate the residual stresses in material, as shown in Figure 2.10. This is because the non-uniform volume change during rapid heating can cause plastic deformation which leads to the relaxation of quenching-induced residual strain.

![Figure 2.9 Heat treatment cycle including uphill quenching for relaxing residual stresses in AA 2024 aluminium alloy (Taken from [57]).](image-url)
Figure 2.10 Relationship between the residual stress reduction and the maximum temperature variation between the surface and the core of the test specimen during uphill quenching. These data are about the quenching of AA 2014 plate specimens with 50.8 mm thick cooled to -195 °C within 1.5 h after SHT and cold water quenching (data from [53]).

Apart from the investigation of residual stresses reduction due to uphill quenching, the effect of the treatment on physical properties of materials has also been studied. It has been proven that unlike the annealing type thermal treatment, the influence of uphill quenching on material properties can be negligible [57-59]. Figure 2.11 illustrates the similar microstructure, e.g. size and shape of precipitates, between two different treatments.

![Figure 2.11](image)

Figure 2.11 The comparison of TEM micrographs of A 6061 alloy between a) typical T6 treatment b) Cryogenic treatment (Taken from [59]).

In the laboratory scale, it has been found that the exploitation of uphill quenching is very successful and in some cases, more than 91 % residual stress reduction has been observed [53, 58]. But since this kind of technique was patented by Aloca in the 1960s [60], so far it is still difficult to apply widely in large-sized aviation components as the
related cost, e.g. dry ice/liquid nitrogen and specific steam nozzle fixtures, is no less
than that of manufacturing a set of large dies. Hence, for the cases which mechanical
stress levelling cannot be applied, uphill quenching is still a good candidate.

2.3.4.3 Stress mitigation via lowering the temperature gradient during quenching
Apart from reversing the pattern of thermal gradient during quenching, moderately
reducing the temperature difference through component thickness during quenching is
another way to generate less residual stresses in a quenched material. To lower the
temperature gradients, several methods, such as choosing different cooling media,
changing the coolant temperature, have been adopted and various extent of stress
reduction has been achieved, as illustrated in Figure 2.8.

Although currently, water is the most popular quenchant, lots of quenching mediums
have been tried for rapid cooling aluminium alloys, as cold water quenching can induce
large residual stress in aviation components. These mediums are carbonated water,
water with dissolved nitrogen under pressure, oil, nitrate salt, Polyethylene glycol
(PAG), etc [49]. For medium temperature, water with elevated temperature, boiling
water and salt bath around 190 °C have been used. In fact, for the methods including
changing quenching media and increasing quenchant temperature to relax residual
stresses, there are two different mechanisms.

For warm/hot water, water with dissolved gas and nitrate salt, etc, at high temperature
(> 60 °C) these medium will release plenty of air and form a persistent vapour jacket at
the surface of the component. As the heat transfer coefficient between air and material
is much smaller than that between water and aluminium alloy, at the start of quenching
the thermal gradient in the material can be lowered and accordingly less residual stress
can be generated. In contrast, for cold water quenching (< 60 °C), Robinson et al [2]
found there is no gas film surrounding AA 2618 aluminium alloy block.

For other quenching media e.g. PAG and oil, during quenching these quenchant can
form a blanket around the material surface [61]. Since the heat transfer coefficient
between these coolant and Aluminium alloys are also relatively lower than that
between aluminium alloy and water, these quenching media can also lower the cooling
rate of material and the thermal gradient within the body.

As shown in Figure 2.8, at least 30 % residual stresses magnitude can be relaxed
through changing cooling medium or medium temperature and in some cases, a more
than 90 % residual stress reduction has been detected. However, for extra-large
components, lowering the cooling rate means the occurrence of excessive precipitation. Accordingly, a remarkable drop of mechanical properties is inevitable because of the lack of supersaturated solid solution. To improve physical properties of material while getting acceptable residual stress magnitude, Robinson et al [62] tried to modify material surface via coating it with black copper oxide (CuO) and found although the extent of stress reduction in coated material under boiling water quenching is less than that in material without coating under the same treatment, the mechanical properties have been elevated. But for a large-sized component, the practicability of this technique is still doubtable in terms of cost.

2.3.5 Other techniques of relaxing residual stress

As an alternative to typical thermal stress relief and conventional mechanical stress relief for mitigating residual stresses, vibratory stress relief (VSR), defined as a method to relax residual stresses via cyclic loading treatment, has been used widely for several decades. Quite a few companies have applied the VSR techniques to a wide variety of mechanical structures ranging in size from thin plates with few of kilograms [63] to large welded assemblies of several hundred tonnes [64], as illustrated in Figure 2.12. For different metallic material with various configurations, the VSR treatment has achieved various extent of stress relaxation ranging from 20 - 70 %. Although there is still no consensus for the mechanism of VSR, many researchers have found that the increase of process parameters, e.g. cyclic stress magnitude and number of cycles, can help reduce residual stresses in material and vibrational frequency variation has an only nominal effect on the residual stress relief [65, 66].

Provided the mechanism of cold working stress relief techniques, Klotzbucher et al [67] put forward an explanation about the VSR stress relief mechanism that during VSR treatment, plastic deformation occurs, relaxing the combined stress magnitude and make it below the material yield stress characterised by the relationship:

\[
\text{residual stress} + \text{imposed loading} \leq \text{material yield strength}
\]
After external stresses removal, the residual stresses are back to a relative low level. However, this theory cannot explain why additional residual stress will be released over the remaining cycles (up to $10^4$) after first several cycles. Walker et al [66] gave a revised explanation. There is indeed a loading amplitude threshold, e.g. 250 MPa, which is even less than half of the yield stress of aluminium alloy under T6/7 conditions. If the external cyclic stress is below the threshold, there will be no residual stress relief. But once the external loading is beyond the specific stress amplitude, with the increase of loading magnitude, for a given number of cycles, the combined effect of the residual stresses and lattice vibration due to VSR will weaken the pinning point defects so as to let more dislocation motion occur. As a result, a more stable dislocation system with minimized energy will be gradually formed because of the micro-plastic process that is responsible for the residual stress relaxation. Hence, at the initial few of cycles, significant stress relaxation occurs. Then, the more the number of cycles, the more stress relief occurs but with a gradually lesser extent, as shown in Figure 2.13.
In addition, VSR treatment has little effect on the mechanical properties of a material, but the concern about VSR involving with the negative effect on fatigue life of components still exist. The research conclusion also varies. Ankirskii et al [69] and Lutes et al [70] believe if VSR is conducted with large cyclic stress (at least more than half uniaxial yield strength of metallic material to cause it locally yield) and a limited number of cycles, there is no effect on fatigue life. However, in Sonsino et al’s study [71], for any stress mitigation during vibration, fatigue damage must occur as the fatigue limit of the material is exceeded during VSR. Just the influence of vibration on fatigue life differs from case to case. Hence, so far the lack of quantitative understanding of the relation between vibration treatment and fatigue resistance of material still limits the application of VSR technique.

2.3.6 Stress relief techniques for this project

Considering the current stress relief techniques mentioned above, it can be seen that for relaxing residual stresses in large-sized aluminium alloy aviation components, in terms of cost and final product quality, there is no ideal technique that can effectively mitigate residual stress.

For annealing type heat treatments, e.g. natural aging, artificial aging and RRA, the main target of these techniques is to improve the material properties. As the aging time
required by these techniques to reduce significant residual stresses is too long to constrain the coarse grain generation and consequently lower the material strength. Treatments, e.g. over-aging, can only play auxiliary roles in reducing residual stresses if the corresponding expense about mechanical properties can be tolerated.

Uphill quenching has been widely used in reducing components with complex configurations. But its expensive cost severely constrains its application in large-sized components. Although Araghchi et al [57] found that 180 °C hot oil pool can be used to replace the relatively expensive steam nozzle apparatus, the extra-large geometry makes it difficult to maximize the temperature difference between material surfaces and core during the reheating period and consequently it is hard to quantify the extent of the stress reduction in the whole part and how the residual stresses distribution change during the cryogenic treatment.

VSR can lead to stress relief in large-sized aluminium alloy components to some extent but there is no quantitative understanding about VSR, especially its effect on fatigue cracking. As some researchers have concluded fatigue failure could be activated by VSR, for relatively valuable large-sized aviation components, it needs more research before widespread application.

For stress relief extra-large aviation component, compared to the techniques above, cold compression is a good candidate as there is no drop of mechanical properties after this stress levelling method and the cost is relatively low [2]. But as mentioned in Zheng et al [12], longitudinal stress variation in quenched extra-large components is induced due to multiple compression and the remaining residual stresses are sufficient to cause unacceptable distortion during the final machining process.

Therefore, to minimize residual stresses in a large-sized component, it is necessary to explore a new method. Cold rolling, as one kind of mechanical stress levelling techniques, has several advantages over the multiple cold compression technique. In terms of economic factors, the current equipment of cold multiple compression technique is made up of a bottom die 6 meters long and a top die 1.2 meters long. Their cross section can be seen in Figure 2.14. The material of die is stainless steel.
In contrast, the equipment that required by the cold rolling technique is only a set of rolls with ~400 mm radius. The cost is much lower than that of building the compression dies. In addition, compared with the huge dies, a rolling machine can save a lot of space. Although figures about the equipment of cold stretching to relieve residual stresses in extra-large component are unavailable, it believes the space that occupied by rolling machine is also much less than that of cold stretching equipment [16]. Apart from the cost and space factors, since the rolling velocity has no effect on the residual stress distribution in quenched and cold rolled material, the processing time of cold rolling on large-size T-section component could be less than that of multiple cold compression if a high rolling velocity is used.

In terms of residual stress, the cold rolling technique can make the component avoid the overlap region effect caused by multiple compression. Although Giorgi et al and Zhao et al [72, 73] have found after cold rolling tensile residual stresses will appear at the both contacting surfaces of the material, for as-quenched large-sized components, there is a lack of research. If the cold rolling method can combine with an auxiliary stress relaxation technique like over-aging, it is highly possible to get a satisfying result to reduce residual stresses in large-sized aviation component with acceptable mechanical properties.
2.4 Methods to experimentally measure residual stress

2.4.1 Overview

To evaluate the effectiveness of stress relief techniques on residual stress reduction in components, many residual stress measurement methods may be used. These methods can be classified into two categories: non-destructive measurement technique and destructive measurement technique. Neutron diffraction, synchrotron X-ray diffraction and X-ray diffraction belong to the non-destructive methods which are based on measuring the angular distribution of the radiation diffracted from the material. Destructive methods include contour method and hole drilling method where the original stress distribution can be reflected by the distortion due to stress relaxation after specimen cutting. Since the mechanism differs from technique to technique, the amount of stress components that can be quantified also varies. Additionally, limitations also exist about the spatial resolution of the detected zone for each technique.

Generally, for measuring the residual stresses in extra-large aviation components, non-destructive residual stresses measurement methods are more appropriate than destructive measurement methods in terms of the economic factors. Among these non-destructive measurement methods, neutron diffraction method and synchrotron X-ray diffraction method play similar roles. They all can measure stresses distribution in some mm into materials. Although the less measurement time is required by synchrotron X-ray diffraction measurement, neutrons have the greatest penetration depth [74]. In addition, for the synchrotron X-ray measurement, the lower scattering angle due to high X-ray energy can aggravate the difficulties in capturing the incident and out beam and consequently could lead to the larger uncertainty of the experimental result than that of neutrons [75].

For relatively small aviation components or a case-study, it is useful to adopt destructive measurement methods to investigate the RS distribution in a material. In this case, to investigate the effect of cold rolling on RS evolution in components, compared with other destructive measurement methods, e.g. deep hole drilling method, the contour method is a better option as it can give a detailed stress map of a component along the cold rolling direction.

Therefore, in this project, the neutron diffraction, X-ray diffraction technique (low energy) and contour method were applied which are described in detail in this section.
Measurement points and the corresponding three stress components along analysis path have been measured.

2.4.2 Neutron diffraction technique

In neutron diffraction, residual stresses are characterized through measurements the spacing variation between lattice planes. Neutron measurements are carried out via diffractometer apparatus located at neutron generating facilities. Figure 2.15 illustrates schematically the instrument SALSA at the reactor source in the Institut Laue-Langevin (ILL), Grenoble.

![Figure 2.15 The SALSA equipment in ILL: The layout of instrument [76].](image)

As shown in Figure 2.15, at reactor sources, a continuous monochromatic beam of neutrons is produced by using a monochromator to select a given neutron wavelength from a polychromatic neutron beam. If a sample is placed in a monochromatic beam of neutrons, then its lattice spacing can be determined if the incident wavelength of the diffracting neutrons is known. The neutron beam directed into a sample is scattered at different angles by the material's crystallographic planes. For each measurement position, the number of neutrons found by the detector along a range of scattering angles is fitted to a Gaussian distribution and every point is measured until enough neutrons are captured to calculate $\theta$. The scattering angle is determined by Bragg's Equation (2.8) [77]

$$n\lambda = 2d_{hkl}\sin\theta_{hkl} \tag{2.8}$$

where $\lambda$ is the wavelength of the incident neutron beam, $\theta$ is the scattering angle of the diffraction peak, $d_{hkl}$ is the lattice spacing and $hkl$ refers to the crystallographic plane.
Due to residual stresses, the elastic strain will be apparent as a shift in the value of $2\theta_{hkl}$ for a particular crystallographic plane. This technique relies on the fact that elastic strain depends on the change in lattice spacing $d_{hkl}$. The strain can be calculated by differentiating Equation (2.8) constant wavelength [78]

$$\varepsilon_{i,hkl} = \frac{\Delta d_{i,hkl}}{d_{0,hkl}^0} = -\cot \theta_{hkl}^0 \Delta \theta_{i,hkl}$$

(2.9)

where $\Delta \theta_{hkl}$ is in radians and $\theta_{hkl}^0$ and $d_{hkl}^0$ are the values of reference Bragg angle and lattice spacing. The values must be obtained from measurements on stress-free reference samples [74]. As shown in Figure 2.15, the strain is measured along the Q direction. The related gauge volume is characterized by the intersection of the incident and diffracted beams. The size of gauge volume can be changed by adjusting the apertures for the incident beam and the detector.

To minimize the systematic errors that are not found in a single peak measurement, $e_{\varepsilon_{hkl}}$, the uncertainty of the calculated strain, could be achieved from Equation (2.10) [74]

$$e_{\varepsilon_{i,hkl}} = \sqrt{\frac{(\Delta d_{i,hkl})^2 + (\Delta d_{0,hkl})^2}{(d_{hkl}^0)^2}} = \frac{\sqrt{\left(\Delta \theta_{i,hkl}\right)^2 + (\Delta \theta_{hkl}^0)^2}}{\tan(\theta_{i,hkl})}$$

(2.10)

when strains have been measured in three mutually orthogonal directions the direct stresses, $\sigma_1$, $\sigma_2$ and $\sigma_3$ can be determined from Equation (2.11)

$$\sigma_i = \frac{E_{hkl}}{1 + \nu_{hkl}} \varepsilon_i + \frac{\nu_{hkl} E_{hkl}}{(1 + \nu_{hkl})(1 - 2\nu_{hkl})} \left(\varepsilon_1 + \varepsilon_2 + \varepsilon_3\right)$$

(2.11)

where $i$ is the direct stress index, $E_{hkl}$ and $\nu_{hkl}$ are the Young’s modulus and Poisson’s ratio of the $\{hkl\}$ crystallographic plane, respectively. The uncertainty $\pm \sigma_{ii}$ ($ii = 11, 22, 33$) is given by:

$$e_{\sigma_{ii,hkl}} = \frac{E_{hkl}}{(1 + \nu_{hkl})(1 - 2\nu_{hkl})} \sqrt{\left(1 - 2\nu_{hkl}\right)^2 (e_{\varepsilon_{11,hkl}})^2 + \nu_{hkl}^2 (e_{\varepsilon_{22,hkl}})^2}$$

(2.12)

When measurements are associated with a specific crystallographic plane, then plane specific values of the elastic modulus and Poisson ratio are used in Equation (2.11) and Equation (2.12). For AA 7050 Aluminium Alloy, the material behaviour of $\{311\}$ lattice plane is recommended as its relatively high peak intensity and low $\varepsilon_p$. Therefore
the peak specific elastic modulus, $E_{311} = 70.2$ GPa, and Poisson ratio, $\nu_{311} = 0.35$ [74], have been employed.

### 2.4.3 X-ray diffraction method

For metallic components with residual stresses, compressive and tensile stresses distributed over the grain structure. Applying elastic tractions, the ensemble of atoms could change the system energy and the atoms move to new positions to form a more equilibrium system, leading to a shift of the diffraction peaks. Since the position change of the diffraction peaks could reflect the related varied atomic spacing, the diffraction peak change can be adopted in calculating the target strain and further stress components via the theory of solid mechanics. Additionally, the resultant strain relies not only on atomic spacing but also on the orientation of grains with respect to the related stress [74]. These phenomena make it possible to conduct an X-ray stress measurement via an X-ray source and the related diffraction detected by an X-ray detector. Since the penetration depth of the X-rays is no more than hundreds of microns, only surface residual stresses are measured using an X-ray diffractometer adopting the $cos\alpha$ technique.

Although neutron diffraction method can give accurate results about three stress components through the thickness of a material because of its relatively larger penetration depth, the corresponding equipment is not portable and cannot be used in-situ.

To investigate the residual stress distribution in large-sized aviation components, a portable Pulstec X360 X-ray stress analyser which utilizes an area detector adopting a novel Debye-ring fitting method was used in this study, as shown Figure 2.16. The methodology that the technique based on is described in detail below.

The X-ray diffraction technique is adopted to determine the surface stress map about two stress components of specimens or components. Debye rings are used for characterising the stress through the related change of $\varepsilon_\alpha$ due to diffraction data variation. If an area detector is placed perpendicular to the incident X-ray beam, Debye ring shows a rough circle. As illustrated in Figure 2.16(b), the radius of the Debye ring can be measured and the $\theta$ in Figure 2.16(b) can be determined via the Equation (2.13)

$$\theta = \frac{\pi}{2} - \frac{1}{2} \tan^{-1}\left(\frac{R}{\xi_L}\right)$$  \hspace{1cm} (2.13)
For 2D stress map (plane stress condition), $\varepsilon_\alpha$ is achieved from the strain projected along a direction with angle $\alpha$ as shown in Figure 2.16(c), can be evaluated via the Equation (2.14) below [79],

$$
\varepsilon_\alpha = n_1^2 \varepsilon_x + n_2^2 \varepsilon_y + n_3^2 \varepsilon_z + n_1 n_2 \gamma_{xy}
$$

(2.14)

where $n_1$, $n_2$ and $n_3$ are the direction cosines of the normal of the imaging plane corresponding to the axes in the sample coordinates which can be determined by the following Equation (2.15), $\gamma_{xy}$ is the shear strain of XY plane (Figure 2.16).

$$
n_1 = \cos \eta \sin \psi_0 \cos \phi_0 - \sin \eta \cos \psi_0 \cos \phi_0 \cos \alpha - \sin \eta \sin \psi_0 \sin \alpha
$$

$$
n_2 = \cos \eta \sin \psi_0 \sin \phi_0 - \sin \eta \cos \psi_0 \sin \phi_0 \cos \alpha + \sin \eta \cos \psi_0 \sin \alpha
$$

$$
n_3 = \cos \eta \cos \psi_0 + \sin \eta \cos \psi_0 \cos \alpha
$$

(2.15)

As shown in Figure 2.16, $\eta = \frac{\pi}{2} - \theta$, $\psi_0$ is the angle between the normal of the sample and the incident beam and $\phi_0$ is the angle between the project of the incident beam to sample surface and $x$ axis [80].
Figure 2.16 a) An illustration of the Pulstec X-ray stress analyser applied in this project  

b) A typical Debye-scherrer ring view from top and $\varepsilon$ strains used in stress calculation  
c) Explanation of measurement of Debye rings for calculating in-plane stresses.  

(images from [63, 81, 82])

The Debye-ring fitting technique calculates the below four functions through measuring $\varepsilon$ from four different angles $\alpha, \pi + \alpha, -\alpha, \pi - \alpha$, [82]

\[
\varepsilon_{a1}(\alpha) \equiv \frac{1}{2}[(\varepsilon_\alpha - \varepsilon_{\pi+\alpha}) + (\varepsilon_{-\alpha} - \varepsilon_{\pi-\alpha})]
\]

\[
\varepsilon_{b1}(\alpha) \equiv \frac{1}{2}[(\varepsilon_\alpha - \varepsilon_{\pi+\alpha}) - (\varepsilon_{-\alpha} - \varepsilon_{\pi-\alpha})]
\]

\[
\varepsilon_{a2}(\alpha) \equiv \frac{1}{2}[(\varepsilon_\alpha + \varepsilon_{\pi+\alpha}) + (\varepsilon_{-\alpha} + \varepsilon_{\pi-\alpha})]
\]

\[
\varepsilon_{b2}(\alpha) \equiv \frac{1}{2}[(\varepsilon_\alpha + \varepsilon_{\pi+\alpha}) - (\varepsilon_{-\alpha} + \varepsilon_{\pi-\alpha})]
\]

(2.16)

According to Toshiyuki et al [83, 84], $\varepsilon_\alpha$ is a single-valued function of $\alpha$ and can be characterized in a Fourier series:

\[
\varepsilon_\alpha = a_0 + a_1 \cos \alpha + b_1 \sin \alpha + a_2 \cos 2\alpha + b_2 \sin 2\alpha + \cdots
\]

\[
= a_0 + \sum_{k=1}^{\infty} (a_k \cos k\alpha + b_k \sin k\alpha)
\]

(2.17)

Provided the plane stress condition is assumed, substituting Equation (2.15) for Equation (2.14), then substitute the updated Equation (2.14) and Equation (2.17) into Equation (2.16) respectively, the coefficient of Equation (2.17) can be determined by the following equations when $\phi_0 = 0$ [84]

\[
a_0 = \frac{\sigma_x}{2E}[2\cos^2\eta\sin^2\psi_0 + \sin^2\eta\cos^2\psi_0]
\]

\[
- v[2\cos^2\eta\cos^2\psi_0 + \sin^2\eta(1 + \sin^2\psi_0)]
\]

\[
+ \frac{\sigma_y}{4E}((1 - \cos 2\eta) - v(3 + \cos 2\eta))
\]

(2.18a)

\[
a_1 = - \frac{1 + v}{2E}\sin 2\eta \sin 2\psi_0 \sigma_x = \frac{\varepsilon_{a1}(\alpha)}{2\cos \alpha}
\]

(2.19b)

\[
b_1 = \frac{1 + v}{2E} \sin 2\eta \sin \psi_0 \tau_{xy} = \frac{\varepsilon_{b1}(\alpha)}{2\sin \alpha}
\]

(2.20c)
\[ a_2 = \frac{1 + \nu}{2E} \sin^2 \eta (\cos^2 \psi_0 \sigma_x - \sigma_y) = \frac{\varepsilon_{a2}(\alpha) - 2a_0}{2\cos2\alpha} \quad (2.21d) \]

\[ b_2 = -\frac{1 + \nu}{E} \sin^2 \eta \cos \psi_0 \tau_{xy} = \frac{\varepsilon_{b2}(\alpha)}{2\sin2\alpha} \quad (2.22e) \]

\[ a_k = b_k = 0 \quad (k \geq 3) \quad (2.23f) \]

where \( E \) is bulk Young's modulus, \( \nu \) Poisson's ratio. Therefore, stresses can be determined via the X-ray measured \( \varepsilon_{a} \) and Equation (2.14) - Equation (2.23), \( \sigma_x \) and \( \sigma_y \) can be demonstrated as:

\[ \sigma_x = -\frac{E}{(1 + \nu)} \frac{1}{\sin2\eta \sin2\psi_0} \frac{\varepsilon_{a1}(\alpha)}{\cos\alpha} \quad (2.24a) \]

\[ \sigma_y = \frac{\sigma_x \left[ \frac{1 + \nu}{2E} \sin^2 \eta \cos^2 \psi_0 + \frac{\Psi}{2E\cos2\alpha} \right] - \frac{\varepsilon_{a2}(\alpha)}{2\cos2\alpha}}{1 + \nu \frac{1}{2E} \sin^2 \eta + \frac{1}{4E\cos2\alpha} \{ (1 - \cos2\eta) - \nu(3 + \cos2\eta) \}} \quad (2.25b) \]

where \( \Psi \) is defined as follows:

\[ \Psi = 2\cos^2 \eta \sin^2 \psi_0 + \sin^2 \eta \cos^2 \psi_0 \]

\[ - \nu [2\cos^2 \eta \cos^2 \psi_0 + \sin^2 \eta (1 + \sin^2 \psi_0)] \quad (2.26) \]

Hence, according to Equation Figure 2.16) and Equations (2.24a) - (2.20), if \( \eta \) and \( \psi_0 \) are given, the in-plane stress \( \sigma_x \) and \( \sigma_y \) can be calculated. Since for the whole Debye ring the \( \alpha \) value ranges from 0° to 360°, many stress results can be calculated. To guarantee the repeatability of the X-ray results, the average \( \sigma \) value and the corresponding standard deviation are output.

For the Pulstec \( \mu \)-X360 X-ray stress analyser, the parameters, \( C_L \) and \( \psi_0 \), can be manually adjusted, as illustrated in Figure 2.16. Lee et al. [81] found \( 30 \leq C_L \leq 50 \) mm and \( 25 \leq \psi_0 \leq 50^\circ \) are the process window for the X-ray measurement. Within this region, the optimized parameters, e.g. \( C_L \) and \( \psi_0 \), vary from different cases. In addition, as the results may be influenced by the external atmosphere, repeat measurements must be carried out to minimize the potential error.

### 2.4.4 Contour method

The contour method [85] is a destructive residual stress measurement technique which can determine the stress distribution of the target plane via carefully cutting a sample in half along the plane and measuring the related displacement data caused by the stress/strain relief. Compared with the diffraction-based techniques e.g. Neutron or
X-ray diffraction, the effect of sample size of sample and its material microstructure on contour results are quite limited because the related FE model can account for the geometry profile of the sample and the deformation can be affected by the material stiffness, not the microstructural variation. (The sample size should be small enough to fit in EDM machine)

In the contour methodology, a derivative of Bueckener’s superposition law is applied [86]. Figure 2.17 indicates the theory of contour method. Before cutting, for $\sigma_{xx}$ residual stress, the compressive stress at the surface regions is balanced by the tensile region in the core part of a specimen, as illustrated in Figure 2.17(a). After cutting, the $\sigma_{xx}$ stress normal to the cutting surface of parts (Figure 2.17 (b)) is completely relaxed. To force the deformed cut plane flat, the stress normal to the cut plane Figure 2.17(c) is required. In theory, the sum of the stress state in stress relieved parts of Figure 2.17(b) and the imposed surface stress about parts Figure 2.17(c) must be equal to the original stress distribution of the whole part Figure 2.17(a). Hence, the surface stress distribution in Figure 2.17(c) can be considered as the residual stress distribution normal to the cut surface in the original specimen.

In reality, the specimen is firstly cut along the plane of interest using an electro-discharge machine (EDM). Then the out-of-plane deformation on the target surface can be measured using a coordinate measuring machine (CMM) or laser scanning machine. The related displacement data is implemented as a surface boundary condition of an elastic FE model of the cut specimen and the stress distribution calculated.
Currently, EDM is the only widespread cutting method that can be adopted in contour technique as it does not generate high temperature and large plastic deformation during the process. Relatively small plasticity could occur when regions with residual stresses are cut, for the contour method it is inevitable but the phenomena can be minimized if the way of clamping is symmetric and an appropriated cutting speed is applied.

Since around the cut tip, plastic deformation can easily be generated due to residual stresses and lead to errors in the subsequent displacement measurement, a measurement procedure to minimize the error due to the plasticity above has been put forward by Prime et al [87]. It includes ways to clamp the sample correctly and to achieve the material displacement around cut tip via FE simulation so as to optimize the measured displacement data. It is discussed in detail in the following chapters.

### 2.5 Summary

An overview including residual stress relief techniques and the stress measurement techniques have been described in this chapter. For reducing residual stresses in extra-large aviation component, the advantages and disadvantages of the current
stress relaxation methods have been reviewed. As an alternative cold working technique for relaxing residual stresses in materials, cold rolling has been introduced.
Chapter 3  AA  7XXX series
material model Calibration

3.1 Introduction

Given the availability of equipment and existing capabilities, cold working techniques are frequently applied to reduce residual stresses in large-sized components. Hence, to achieve a better understanding of the manufacturing process, numerical modelling was performed to simulate the thermal stresses induced by quenching and the stress evolution during the subsequent cold working processes. To develop an accurate model of the SHT, quenching and cold working processes, a detailed illustration of the material response of the 7050 Aluminium alloy is needed. Hence fundamental tests were carried out. The isothermal Gleeble tests were conducted to investigate the material behaviour of 7050 aluminium alloy during quenching and the subsequent working processes, such as cold compression and cold rolling. The effect of various temperatures and strain rates on stress-strain relationship of 7050 Al alloy material will be analysed in the section below.

To develop a finite element model of the manufacturing processes of a T-section component, an accurate description of the constitutive behaviour of the 7050 Al alloy is required. So far many investigations have been prepared to explore the relation between the flow stress and strain of 7050 Al alloy material in T6 condition. Luo et al [88] illustrated the flow behaviour of 7050 aluminium alloy in the T6 final condition. Their yield stress values for test temperatures of 470 °C and 320 °C ranged from 110 to 165 MPa at 20.0 s\(^{-1}\) and from 85 to 155 MPa at strain rates of 0.01 s\(^{-1}\) and 10 s\(^{-1}\), respectively. While Wu et al [89] proposed a hyperbolic sine law to express the yield stress and ultimate stress of 7050 aluminium alloy in the T6 condition for various strain rates and deformation temperatures, the height reduction of their isothermal compression tests is 30 %, 50 % and 70 %. Their yield stress values gradually decreased from ~115 MPa at 380 °C to ~85 MPa at 450 °C under 1 s\(^{-1}\) strain rate. At 450 °C, the solution-treated yield stress values gradually increased from ~40 MPa at 0.01 s\(^{-1}\) to ~120 MPa at 20.0 s\(^{-1}\). Additionally, Hu et al [90] studied the 7050 Aluminium alloy deformation behaviour via tensile tests under different strain rates at various temperatures and presented it by a Zener-Holloman parameter in a hyperbolic sine-
type equation. For their T6 condition specimens, the yield stress gradually decreased from $\sim 110$ MPa at $340 \, ^{\circ}C$ to $\sim 50$ MPa at $460 \, ^{\circ}C$ under strain rates of $0.1 \, s^{-1}$.

3.2 Uniaxial compression tests

3.2.1 Test description

Although quite a few investigations have been carried out to reveal the constitutive behaviour of 7050 Al alloy in the T6 condition, limited research has been conducted with the material under the solutionized condition and T4 condition. Hence, to accurately predict the deformation performance of 7050 aluminium alloy during the manufacturing processes of T-profile component, a series of isothermal compression tests of 7050 Al alloy were carried out at strain rate range of $0.01 - 1 \, s^{-1}$ and in the temperature range of $20 - 450 \, ^{\circ}C$. Effects of temperature and strain rate on flow stress of 7050 Al alloy were investigated. Based on the strain-stress data, a set of constitutive equations describing the relation between flow stress, strain rate and temperature has been developed. This material model is finally applied in finite element models about corresponding forming processes.

3.2.2 Specimen Preparation

The 7050 Al alloy material used in the experiments was provided by AVIC. The chemical composition of the aluminium alloy 7050 considered in this work is given in Chapter 2. The alloy isotropic compression test samples, 12 mm long by 8 mm in diameter were prepared. In industrial manufacturing procedures, a 7XXX component undergoes SHT at $470 \sim 485 \, ^{\circ}C$ and is quenched rapidly in the water bath ($60 \sim 70 \, ^{\circ}C$), and then plastically deformed by cold compression. The similar thermal cycle was applied to samples prepared for Gleeble tests, as shown in Figure 3.1. The 7050 Al alloy samples were firstly heated to $480 \pm 5 \, ^{\circ}C$ in a furnace and held for 3 h, then quenched in $60 \, ^{\circ}C$ water to keep the material structure. A similar thermal cycle as the industrial processes was applied to samples prepared for Gleeble tests [91]. The heating and isothermal compression tests were conducted in a Gleeble 3800 Thermo-Mechanical Simulator (Dynamic Systems Inc, Poestenkill New York, USA) [41]. To minimise the precipitation effect that may generate due to natural ageing between the quench and testing, the Gleeble was programmed to firstly heat the samples $20 \, ^{\circ}C \, s^{-1}$ to $450 \, ^{\circ}C$ then gradually heat them at $2.5 \, ^{\circ}C \, s^{-1}$ to $475 \, ^{\circ}C$ and held at that temperature for one minute in order to avoid any creep effect due to the force that was exerted to hold the sample between two anvils [91] (the primary holding force, $0.5 \, kN$, was
adopted to minimize the effect), then cooled at 10 °C s⁻¹ to the required test temperature in the range of 200 - 450 °C, which are the range of relevance to analysing deformation phenomena during the manufacturing processes. In order to protect the 7050 aluminium alloy specimens from oxidation, the samples and clamping anvils were tested in vacuum during compression at high temperature. To protect the compression planes of its Gleeble machine, graphite lubrication was used between the sample and the plungers. For room temperature test samples, they were firstly air cooled with 10 °C s⁻¹ cooling rate and then quenched by water spray to target temperature so as to keep the cooling rate constant. After compression, the specimens were cooled in air to room temperature. Once the test temperature was reached, samples were compressed at different temperatures between 20 - 450 °C with a constant strain rate of 0.1 s⁻¹ and at strain rates of 0.01, 0.1 and 1 s⁻¹ under 450 °C and were deformed up to a total strain of 0.25. In order to guarantee the accuracy of experimental results, at least three tests were conducted for each test condition during the isothermal compression Gleeble tests, as shown in Table 3.1. Generally, a total of 24 uniaxial compression tests were performed.

Table 3.1 Test Programme of isothermal Gleeble tests (‘√’ represents the selected test condition)

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Strain rates (/s)</th>
<th>0.01</th>
<th>0.1</th>
<th>1</th>
</tr>
</thead>
<tbody>
<tr>
<td>450</td>
<td></td>
<td>√</td>
<td>√</td>
<td>√</td>
</tr>
<tr>
<td>410</td>
<td></td>
<td></td>
<td></td>
<td>√</td>
</tr>
<tr>
<td>380</td>
<td></td>
<td></td>
<td></td>
<td>√</td>
</tr>
<tr>
<td>320</td>
<td></td>
<td></td>
<td></td>
<td>√</td>
</tr>
<tr>
<td>200</td>
<td></td>
<td></td>
<td></td>
<td>√</td>
</tr>
<tr>
<td>20</td>
<td></td>
<td></td>
<td></td>
<td>√</td>
</tr>
</tbody>
</table>
3.2.3 The effect of work parameters on stress-strain curves

The flow stress-strain curves at various deformation temperatures and strain rates are shown in Figure 3.2. It can be seen in Figure 3.2 that, at the beginning, the flow stress increases rapidly with the growth of strain and approaches a peak value, then the curve plateaus to approximately constant.

During the early stages of compression tests, the generation, multiplication and intersect of dislocations lead to work hardening due to straining. Although during this period dislocation cross-slip could cause dynamic softening, it is not enough to overcome the influence of work hardening of the material, as a consequence, the flow stress increases. After a critical strain is reached and exceeded, dynamic recovery takes place due to sufficiently high stacking fault energy during high-temperature compression [89]. Hence the flow stress slightly decreases after the flow stress magnitude approaches the peak value. After exceeding the critical strain, the main softening mechanism is dislocation climb, and the influence of softening becomes significant. Consequently, the stress-strain curve decreases slightly and goes stable as the dynamic balance between work hardening and dynamic softening has been reached.
Figure 3.2 Experimental flow stress versus strain curves a) Flow curve at strain rate 0.1
b) Flow curve at 450 °C.

The flow stress increases with strain rate at 450 °C as shown in Figure 3.2(a). It has been found that when the 7050 aluminium alloy was deformed at 450 °C, at various strain rates, the true stress-strain curve is generally flat. The phenomenon implies that
large plastic deformation can be obtained at almost constant stress-strain curve which means that the 7050 aluminium alloy has good uniform deformability for hot forming processes. Additionally, the influence of dislocation generation, multiplication and intersection on material behaviour is more significant with higher strain rate. It is mainly because the increasing strain rate could lead to the higher dislocation generation rate. The tangled dislocation structure impedes the movement of dislocation, resulting in the growing flow stress in isothermal compression test of 7050 aluminium alloy. Therefore the dislocation density increases when the peak strain is reached, introducing the higher peak stress magnitude.

The effect of a wide range of test temperatures on the physical behaviour of 7050 aluminium alloy is presented in Figure 3.2(b). With the higher deformation temperature, the flow stress magnitude drops continuously. According to Li et al [92] and Wu et al [89]’s research, the reason is the high deformation temperature makes the thermal activation of material atoms become easier, inducing the motion of dislocation and vacancy. As a consequence, the dislocation climb increases, introducing the strengthened dynamic softening influence and correspondingly decreasing flow stress magnitude. Luo et al [88] suggested this phenomenon is because of the dissolution of the second phase of particles into Al matrix at higher testing temperature, lowering the flow stress magnitude.

### 3.2.4 Calibration of the constitutive equations

The determination of the dislocation based material model employed in this work has been mentioned in Section 3.2.2. Dislocation-based constitutive equations have been implemented into a FE analysis via the user defined subroutine, VUMAT, to describe the materials elastic-plastic deformation behaviour. In this study, a material model based on Cao et al. [23] has been employed. The constitutive equations for the material model as follows:

\[
\dot{\varepsilon}_p = \left| \frac{\sigma_v - H - \sigma_y}{K} \right|^{n_1} \quad (3.1)
\]

\[
\dot{\rho} = A(1 - \bar{\rho})\dot{\varepsilon}_p - C_1\dot{\bar{\rho}}^{n_2} \quad (3.2)
\]

\[
H = B\sqrt{\bar{\rho}} \quad (3.3)
\]

\[
\sigma = E(\varepsilon_T - \varepsilon_p) \quad (3.4)
\]
where $\sigma$ is the total stress, $\sigma_v$ the von Mises stress, $H$ the isotropic hardening parameter, $\bar{\rho}$ the dislocation density, $\varepsilon_p$ the plastic strain, $\varepsilon_T$ the total strain, $\sigma_y$ the yield stress, $K$ the drag stress, $E$ the Young’s modulus, and $A$, $B$, $C_1$, $n_1$ and $n_2$ constants. Some parameters, e.g., yield stress $\sigma_y$ and drag stress $K$, temperature-dependent and Arrhenius type equations used for these constants:

$$K = K_0 \exp \left( \frac{Q_K}{R_g T} \right)$$  \hspace{1cm} \text{(3.5)}

$$C_1 = C_{1,0} \exp \left( -\frac{Q_{C_1}}{R_g T} \right)$$  \hspace{1cm} \text{(3.6)}

$$E = E_0 \exp \left( \frac{Q_E}{R_g T} \right)$$  \hspace{1cm} \text{(3.7)}

$$n_1 = n_{1,0} \exp \left( \frac{Q_{n_1}}{R_g T} \right)$$  \hspace{1cm} \text{(3.8)}

$$\sigma_y = \frac{\sigma_{y_0}}{\cosh^2 \left( \alpha_{y_0} (T - T_0) \right)}$$  \hspace{1cm} \text{(3.9)}

$$B = \frac{B_0}{\cosh^2 \left( \alpha_B (T - T_0) \right)}$$  \hspace{1cm} \text{(3.10)}

where the gas constant $R_g$ is 8.314 J mol$^{-1}$K$^{-1}$, $T$ is the absolute temperature (K), $Q_K$, $Q_{C_1}$, $Q_E$, $Q_{n_1}$ ($Q$ is the activation energy), $\alpha_{y_0}$ and $\alpha_B$ material constants.

During quenching, the total strain increment, $\Delta \varepsilon_T$, can be separated into three components,

$$\Delta \varepsilon_T = \Delta \varepsilon_p + \Delta \varepsilon_e + \Delta \varepsilon_t$$  \hspace{1cm} \text{(3.11)}

where $\Delta \varepsilon_p$, $\Delta \varepsilon_e$ and $\Delta \varepsilon_t$ are the visco-plastic strain increment, elastic strain increment and thermal strain increment, respectively. For $\Delta \varepsilon_t$, there is $\Delta \varepsilon_t = \alpha (\Delta T)$. $\alpha$ is the thermal expansion coefficient of the material for a given temperature range, $\Delta T$.

Results of these uniaxial compressive tests of 7050 aluminium alloy were used to calibrate the Equations (3.1) ~ (3.11). The determined material constants for the equations listed in Table 3.2. Under different strain rates and temperatures, demonstrating a good agreement. The average relative error between the fitted and the experimental flow stress is 5.62 % and the maximum relative error is 17.6 %. Hence, it
can be concluded that the constitutive material model established in this study has high precision and could be applied to describe the physical behaviour of 7050 aluminium alloy during a series of manufacturing processes.

Table 3.2 Material constants for the viscoplastic constitutive equations of 7050 aluminium alloy.

<table>
<thead>
<tr>
<th>Material constant</th>
<th>Value</th>
<th>Material constant</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$A$</td>
<td>15.62</td>
<td>$C_0$ (s$^{-1}$)</td>
<td>2.99×10$^5$</td>
</tr>
<tr>
<td>$n_2$</td>
<td>1.6</td>
<td>$Q_c$ (J mol$^{-1}$)</td>
<td>5.88×10$^4$</td>
</tr>
<tr>
<td>$\sigma_{y_0}$ (MPa)</td>
<td>166.12</td>
<td>$B_0$ (MPa)</td>
<td>255</td>
</tr>
<tr>
<td>$\alpha_k$ (1/°C)</td>
<td>4.11×10$^{-3}$</td>
<td>$\alpha_B$ (1/°C)</td>
<td>3×10$^{-3}$</td>
</tr>
<tr>
<td>$K_0$ (MPa)</td>
<td>13.11</td>
<td>$E_0$ (MPa)</td>
<td>6.13×10$^4$</td>
</tr>
<tr>
<td>$Q_K$ (J mol$^{-1}$)</td>
<td>4.11×10$^3$</td>
<td>$Q_E$ (J mol$^{-1}$)</td>
<td>795.24</td>
</tr>
<tr>
<td>$n_{1,0}$</td>
<td>0.69</td>
<td>$Q_{n1}$ (J mol$^{-1}$)</td>
<td>6.81×10$^3$</td>
</tr>
</tbody>
</table>
Figure 3.3 Stress-strain curves at (a) a constant temperature of 450 °C and a range of strain rates and (b) at a constant strain rate of 0.1 s\(^{-1}\) and a range of temperatures.
3.3 Summary

In this chapter, fundamental tests have been designed and conducted so as to build an accurate finite element model of the manufacturing process of Aluminum alloy panel components. It can be concluded that the flow-stress performance of as-quenched 7050 aluminum alloy mainly depends on the test temperatures and strain rates. The material has good plasticity and the main softening mechanism is dynamic recovery. Generally, the experimental curves agree well with the calibrated curves via the dislocation-based constitutive equations. The test parameters are in the temperature range between 20 °C and 450 °C and at strain rates from 0.01 s\(^{-1}\) to 1 s\(^{-1}\), and the average relative error between the predicted and the experimental flow stress is 5.62 % and the maximum error is less than 17.6 %.
Chapter 4  Residual stress relaxation by cold rolling

4.1 Introduction

It is well-known that quenching can introduce large residual stresses into components, especially large components (> 50 mm) which leads to component distortion and impacts their structural integrity. Hence there has been significant work into residual stress mitigation techniques. For the extra-large components, Tanner et. al. [39] studied the influence of cold working techniques, such as cold compression and cold stretching, on the RS distribution in quenched aluminium products. They found that both techniques could significantly reduce quenching-induced RS magnitudes by around 90 %. However, it is difficult to apply these conventional cold working methods for extra-large components e.g. aviation components over 5 m long. For cold stretching, the asymmetric loading causes handling problems for extra-large components, making it difficult to be applied [35]. As for cold compression, it is impractical to build a die set long enough to cover the whole component, while research shows [39] that multi-step cold compression could also lead to a stress differential on the surface of a component in regions of compression strike overlap. In comparison to these aforementioned techniques, cold rolling, given that it is relatively cost-effective, could be employed to easily compress extra-long components.

Research has indicated [72, 73] that cold rolling can lead to relatively large tensile RS (about 53 % of its yield stress) being generated on the surfaces of a cold rolled material. Although Giorgi et al [72] found that, for AISI 301 aeronautic stainless steel, the residual stress magnitudes in single-pass cold rolled components were tensile (up to 36 % of the yield stress) at the rolled surfaces of the plate, little work has been carried out to study the effectiveness of cold-rolling on residual stress mitigation in quenched material. However, these tensile residual stresses may be limited to the regions close to the specimen’s surfaces.

In this chapter, the effectiveness of cold rolling technique for relaxing the RS in quenched 7050 aluminium alloy is evaluated, with the aim of developing an effective method of mitigating the residual stress in aviation components. Neutron diffraction, X-ray diffraction and contour techniques have been employed to quantify the residual stress distributions on (i) a quenched block and (ii) a quenched & cold rolled block of
AA 7050 to determine the effectiveness of cold rolling on mitigating the quenched induced residual stress, both deep inside the sample and very close to the rolled surface. Finite element analysis techniques have also been developed, employing a dislocation-based material model, to simulate the solution heat treatment process, quenching and subsequent cold rolling to predict the residual stress distributions during the manufacturing process and their reduction by cold rolling. These residual stress predictions were validated through the comparisons with experimental measurement results.

### 4.2 Sample preparation and experimental procedure

In industry, solution heat-treated components are often quenched in warm water (around 60 ~ 70 °C) as cold water (about 20 °C) could lead to relatively large residual stresses. If a component was quenched in boiling water it would constrain the generation of the desired super-saturated solution, although less residual stresses would be generated. However, in this case, in order to investigate the effectiveness of cold rolling on residual stress evolution in 7050 aluminium alloy block, given the laboratory condition and the relatively small size of block, cold water (20 °C) was used as the quenchant so as to maximize the quench-induced residual stress in the aluminium blocks.

The as-received state of 7050 forging is T7452 [40] which means the heat-treated material was stress relieved before over-aging. The experimental geometries consisted of seven blocks $62 \times 62 \times 25.4$ mm$^3$ and one block $62 \times 62 \times 21.2$ mm$^3$ in dimensions which were sectioned from the forging mentioned above. As illustrated in Table 4.1, for the seven blocks (A, B, C, D, E, F, G) with the same geometry profile, they were all solution heat treated at $480 \pm 5$ °C for 3~4 hours followed by immersion quenching into the agitated water at less than 20 °C as illustrated in Figure 4.1. The blocks were immersed into the water along their thickness direction such that the $62 \times 62$ mm$^2$ face entered the water first. After quenching, Block E and F were selected to be cold rolled 1.5 % along the longitudinal direction and Block C, D and G were cold rolled 0.5 %, 1 % and 3 % after quenching, respectively. It should be noted that after rapid cooling, the samples were cold rolled within half an hour to minimise any natural aging effects [52]. For the rolling device, two rolls of diameter 140 mm were used and the angular velocity is 1 rad/s. Table 4.1 summarises these experimental details.
Table 4.1 Summary of treatment and measurements conducted to the five AA 7050 forgings and residual stress measurement.

<table>
<thead>
<tr>
<th>Sample code</th>
<th>Cold rolling Deformation ratio</th>
<th>Neutron diffraction measurement</th>
<th>Contour measurement</th>
<th>X-ray diffraction measurement</th>
</tr>
</thead>
<tbody>
<tr>
<td>A,B</td>
<td>None</td>
<td>√</td>
<td>√</td>
<td>√</td>
</tr>
<tr>
<td>C</td>
<td>0.5%</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>D</td>
<td>1%</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>E,F</td>
<td>1.5%</td>
<td>√</td>
<td>√</td>
<td>√</td>
</tr>
<tr>
<td>G</td>
<td>3%</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Note: Block B and F share the same treatment with Block A and E, respectively. But both Block B and F were not measured by SALSA (Neutron diffraction), Contour and μ-X360 (X-ray diffraction) techniques.

Figure 4.1 Schematic temperature profile of heat treatment and cold rolling of 7050 Al alloy blocks.
4.3 Prediction of residual stress in test material during SHT treatment and cold rolling process

4.3.1 Description of FE modelling of Quenching and Cold Rolling Processes

Half of the block was modelled, employing symmetry conditions, as shown in Figure 4.2. The coordinate directions of FE model are defined such that $x$ is along the rolling direction, $z$ is transverse to the rolling orientation and $y$ is through the thickness of the block and the origin is defined at the corner of the top surface about the block (see Figure 4.2(a)). The cooling rates, and thus temperature distribution during quenching, is dependent on the orientation of the block as it enters the water. The lowering direction of the component into the water is shown in Figure 4.2(b).

A sequentially coupled thermal-mechanical analysis was performed for the quenching process, as any heat generation from plasticity during quenching is considered negligible. Three-dimensional (3D), 8 node heat transfer elements DC3D8, were employed in the thermal analysis and 8 node 3D continuum reduced integration elements, C3D8R, were employed in the mechanical analysis as the C3D8 elements are considered overly stiff [39] and C3D8R elements improve the computational efficiency. The 3D roll model included 74000 elements. The mesh close to the surface was refined, where large stress gradients are expected after rolling, to a minimum element size of $0.4 \times 1 \times 1 \text{ mm}^3$, as shown in Figure 4.2(b). The element size elsewhere is $1 \text{ mm}^3$.

In this work, the first stage of the model is to represent the block after removal from a furnace after solution heat treatment, i.e. the third section in Figure 4.1 where the block temperature is uniformly at $485 \, ^\circ\text{C}$. As previously described, it took about $7 \, \text{s}$ to transfer the block in air from a furnace to a water pool. Then, the component cools down in the water, which remains constant and uniform at $20 \, ^\circ\text{C}$. Then, within half an hour, the block was cold rolled to minimize any natural aging effects.

Both radiation and convection heat transfer boundary conditions were applied to the block. Ambient temperature was also taken to be $20 \, ^\circ\text{C}$ and the radiative emissivity ratio was set as $0.3$ [93]. The convective heat transfer coefficient was taken to be $10 \, \text{W/m}^2\text{K}$ for natural convection in air. For water quenching, temperature dependent heat convection values calculated in Appendix A.1 and other temperature-dependent
thermal properties of 7050 aluminium alloy e.g. thermal conductivity, density and specific heat, were taken from Baldwin et. al [94].

![Diagram](image1.png)

Figure 4.2 a) Half geometry of the block modelled indicated symmetry plane and rolling direction b) Illustration of the finite element mesh also indicating lowering direction.

For the subsequent cold rolling process, the rolls were assumed to be analytically rigid bodies. The final state of the quenched aluminium block in the FE model was used as the initial state for the cold rolling model. For the cold rolling process, the friction coefficient between rolls and block was set as 0.1 [95]. For the FE analysis, both top and bottom rolls are cylindrical bars with 50 mm long in dimension. An arbitrary angle velocity 9 rev/min was used for all analysis. Since the plastic deformation is relatively small, the effect of heat generated by plastic deformation during rolling was neglected.

### 4.3.2 Thermal and Residual Stress Distributions due to Quenching

Figure 4.3 shows contour plots of the predicted temperature distribution during quenching. As can be seen from Figure 4.3(a), the decrease in temperature during the transfer period (air cooling) is minor. When the component is in contact with water, the surface temperature decreases quickly (shown in Figure 4.3(b) and (c)). The time for lowering sample into water is set as 0.6 s. Figure 4.3(d) shows such a state when the whole component has been immersed in the water for 1.0 s. At this time, the core of the component is still relatively hot while the surface has rapidly cooled to a low temperature.

Thermal stresses are generated when the surface of a thick component is rapidly cooled (or heated). Rapid surface cooling during quenching leads to the surface contraction that is restrained by the underlying material leading to a large tensile stress being generated at the surface that is balanced by a compressive stress in the underlying material. As time proceeds, the near-surface material has cooled but the central part of the component continues to cool and contract. This core contraction is
resisted by the currently cold and therefore relatively stronger outer material. As a consequence, the tensile stress gradually builds in the centre which is balanced by the compressive stress that develops around the surface of the component.

![Figure 4.3 Contour plot of the temperature distribution during various stages of the quenching process. (a) After 7 s transfer time (air cooling), (b) Initial stages of quenching (7.2 s), (c) Over half the sample immersed (7.4 s), (d) 1.0 s after full immersion (8.6 s).]

The expected trends about post-quench residual stress distribution in the $x$, $y$ and $z$ coordinate directions are given in Figure 4.4. Residual stresses of up to 105% the uniaxial yield stress (250 MPa [2]) have been developed due to quenching. As shown in Figure 4.4, the residual stresses along three components are tensile throughout and attain a peak value of 86% of $\sigma_{yield}$ at the centre and convert to compressive at the surface, as necessary.
4.3.3 The influence of cold rolling on residual stresses in quenched aluminium material

4.3.3.1 Effect of rolling deformation ratio

The optimum deformation ratio for the quenched and cold rolled blocks needed to be determined prior to ND measurement. Therefore, before ND measurement, the residual stresses distribution for different rolling deformation ratios 0.5 %, 1 %, 1.5 % and 3 % were predicted in FE.

Contour plots of the residual stress distribution after 1.5 % and 3 % cold rolling are presented in Figure 4.5 and Figure 4.6, respectively, via a set of rolls with 70 mm radius. Compared to the post-quench residual stress distribution shown in Figure 4.4, it can be seen from Figure 4.5 that after 1.5 % cold rolling, the residual stresses at the core part of material experience a marked decrease in the three coordinate directions, with the percentage peak stress reduction in the range of 60 – 90 %, thus, the residual stresses have been markedly reduced. The results also show that there is no advantage in applying over 3 % rolling deformation ratio for reducing residual stresses in material compared to 1.5 % compression ratio. As shown in Figure 4.6, although the residual stress magnitudes in the central part of the component also experience a remarkable decrease after 3 % cold rolling, for \( \sigma_{xx} \) and \( \sigma_{zz} \), the tensile stress magnitudes develops significantly with the increase of rolling deformation ratio, reaching peak values of 349 MPa and 150 MPa, respectively.
The residual stress distributions predicted through the thickness direction, at the mid length of the symmetry plane of the block, are shown in Figure 4.7. With the increase of rolling deformation ratio, at surface regions of the material, the $\sigma_{xx}$ and $\sigma_{zz}$ compressive residual stresses gradually convert into tensile residual stress. For $\sigma_{xx}$ and $\sigma_{zz}$, the maximum tensile surface residual stresses reach 345 MPa and 106 MPa when the thickness reduction of component reaches 3 %. For the core part of the component, the stress magnitude experiences a decrease with the increase of rolling deformation ratio. When the deformation ratio is 3 %, within the region 3 mm from both surfaces, the maximum $\sigma_{xx}$ and $\sigma_{zz}$ could approach -28 MPa and 69 MPa, respectively. For $\sigma_{yy}$ component, the rolling deformation effect is very limited.

Figure 4.6 Residual stresses distribution of quenched block after cold rolling with 3 % deformation ratio.

Figure 4.5 Residual stresses distribution of quenched block after cold rolling with 1.5 % deformation ratio.
4.3.3.2 Effect of roll radius

As shown in Figure 4.5 and Figure 4.8, the effect of roll radius on stress distribution of quenched and cold rolled block was also studied. It can be seen that the increasing roll radius could further lower the residual stress magnitude of $\sigma_{xx}$ and $\sigma_{zz}$ at the regions from both surfaces to the core part. The reason is a larger roll radius make both rolls close to a set of huge forge dies (with infinite roll radius) about the conventional cold compression process, which is one of the conventional cold working processes to reduce thermal-induced residual stresses in the material. Hence, it can be imaged that a huge die with large roll radius can significantly minimize the residual stresses magnitude in the whole material. At the areas 2 mm from both surfaces, the $\sigma_{xx}$ and $\sigma_{zz}$
reach the peak values 75 MPa and 95 MPa, respectively. Meanwhile, the tensile surface stress magnitudes also decrease to 266 MPa and 80 MPa.

Figure 4.8 Residual stresses distribution of quenched block after cold rolling (roll radius 140 mm).

Figure 4.9 Predicted stress distribution along a) longitudinal (x), b) normal (y) and c) transversal (z) components in the quenched and cold rolled aluminium block under different roll radius along line C-D.
For the $\sigma_{xx}$ and $\sigma_{zz}$ stresses along CD line close to the surfaces, there is no significant change in the stress magnitude due to the roll radius effect. When roll radius is 140 mm, the maximum values of surface $\sigma_{xx}$ and $\sigma_{zz}$ tensile residual stresses are 268 MPa and 45 MPa, respectively, while the peak values of $\sigma_{xx}$ and $\sigma_{zz}$ stresses at the core part are 5 MPa and 71 MPa, respectively. Similarly, the various roll radii also have no remarkable influence on the $\sigma_{yy}$ stress distribution.

From Figure 4.5 - Figure 4.9, it can be seen that a rolling deformation ratio of 1.5 % and rolls with as large as possible radius is the ideal condition in relaxing the $\sigma_{xx}$ and $\sigma_{zz}$ stresses at the both core and surfaces of the block specimens. In addition, the FE prediction shows that the effect of cold rolling on the $\sigma_{yy}$ residual stress distribution in quenched and cold rolled block is insignificant. From these laboratory tests, a block rolled to a deformation ratio of 1.5 % via a set of 70 mm radius rolls was selected for residual stress measurement by the neutron technique.

### 4.4 Neutron diffraction measurement

To study the thermal-induced residual stress evolution during quenching and subsequent cold rolling, the neutron diffraction technique was adopted to measure residual stress distribution in quenched and quenched & cold rolled 7050 aluminum alloy blocks. The residual strains along three orthogonal directions for samples in two different conditions were measured.

#### 4.4.1 Measurement location

Neutron diffraction measurements have been performed on two blocks, one quenched and the other quenched and rolled. Since, through measuring the residual strains in a sample along nine directions, Traore et al. [96] ’s experimental results indicate the axes of the samples were almost aligned with the principal stress components. Thus as illustrated in Figure 4.10, measurements were made at 46 points along the mid length and mid thickness of the block in the rolling direction (line A-B), 24 points through the thickness of the block at the mid length and mid width (along line C-D, same as the analysis line in Section 4.3.1), and 26 points along the mid width and mid thickness of the block, transverse to the rolling direction (line E-F). Measurements were obtained in the three orthogonal directions (i) along the rolling direction, (ii) transverse to the rolling direction, (iii) through thickness direction, which are considered to be the principal stress directions.
Figure 4.10 Dimension of 7050 aluminium alloy blocks indicating the location of the neutron diffraction measurements

A gauge volume of $2 \times 2 \times 2 \text{ mm}^3$ was used for the stress measurements of quenched only block A. For the quenched and cold rolled block, measurement was only conducted along the line C-D across the part thickness 54 points were measured using a gauge volume of $0.6 \times 0.6 \times 10 \text{ mm}^3$, achieving a high special resolution near the surface of the sample which is expected to have high-stress gradients due to rolling process. Both gauge volume sizes could sample a sufficient number of grains within an adequate count time, ranging from 10 to 20 minutes per point.

4.4.2 Stress-free sample preparation and the determination of the reference scattering angle

As shown in Figure 4.11, the reference angles must be determined at stress-free positions. Cold rolling could cause the elongation of grain and further microstructural changes to samples. As a consequence, it could lead to the variety of reference scattering angles in materials. Hence, it may induce incorrect residual strain measurements if the change of reference scattering angle is not taken into account [97]. Small cubes were extracted from a quenched block B and also a quenched and cold rolled block D which were considered nominally identical to those used for residual stress measurements. These coupons were assumed as ‘stress-free’ because the residual stresses were relieved during EDM cutting of these cubes. To achieve the reference diffraction angle, these ‘stress-free’ coupons were measured as well.
In this case, the cubes with dimensions $4 \times 4 \times 4$ mm$^3$, were extracted from the mid-length and mid-thickness of the sample in the rolling direction (line A-B in Figure 4.10) and through the thickness of the sample at the mid-length and mid-width (line C-D in Figure 4.10). As shown in Figure 4.11, each of these cubes was identified by a unique letter or number. Considering that the length and width dimension of these plates are basically the same, reference coupons from line A-B identified in Figure 4.10 are used in processing the data about diffraction angle from line E-F for the quenched block.

### 4.4.3 ND result of quenched material

To achieve accurate residual strain measurements of quenched block via neutron diffraction, the reference scattering angles $\theta_0$ should be carefully determined.

Figure 4.12 Stress free lattice parameter measurements in the a) quenched block (Cubes A-G-M) b) quenched block (Cubes 1-2-G-3-4).
The strain free lattice parameter measurements in the quenched block shown in Figure 4.12(a) and (b), respectively. No clear trends can be seen with position and a systematic variation of $\theta_0$ was also not observed. Therefore the measurements obtained from each cube of a given analysis path have been averaged and used as the $\theta_0$ measurement. The average value of reference scattering angle measured across the measured paths A-B and C-D is $95.5291^\circ \pm 0.0025^\circ$, where the average error was determined from the Equation (4.1) [98].

Average error

\[
\text{Average error} = \frac{(\text{max value} + \text{error}_{\text{max value}}) - (\text{min value} + \text{error}_{\text{min value}})}{\text{Amount of members}}
\]

Note that these values are dissimilar as the measurements because both measurements were made during different visits to the ILL. The wavelengths of the monochromatic radiation of the instrument were 1.68 Å for measuring the quenched & cold rolled block and 1.72 Å during the stress measurement of the quenched sample.

![Graphs](a) A - B line (b) C - D line (c) E - F line)

Figure 4.13 Residual strains measured using Neutron diffraction in quenched 7050 Al alloy sample (a) A - B line (b) C - D line (c) E - F line.
The residual elastic strain components measured for the quenched 7050 aluminium alloy block is shown in Figure 4.13(a), (b) and (c), respectively. The measurement paths for every picture below illustrated in Figure 4.10. It can be seen from Figure 4.13, that the residual strain distributions for the quenched specimen were approximately symmetric about the centre of the block. The residual strains in Figure 4.13(a) and (b) are along the symmetry plane to C-D line and A-B line, respectively. The error bars of residual strain show the maximum of the error in fitting Gaussian peaks to the measured neutron scattering angles, as indicated in Figure 4.13(a), the $\varepsilon_{i,311}^e$ ($i=1,2,3$) along AB line ranges from $\pm 24.23$ to $\pm 56.13 \mu \varepsilon$. In Figure 4.13(b), along C-D line, the $\varepsilon_{i,311}^e$ ($i=1,2,3$) value varies from $\pm 23.50$ to $\pm 52.20 \mu \varepsilon$. Along E-F line, Figure 4.13(c) shows the $\varepsilon_{i,311}^e$ ($i=1,2,3$) is within a range between $\pm 25.14$ and $\pm 47.37 \mu \varepsilon$. The strain errors of different components of the quenched sample are generally of the same magnitude.

Residual stresses were calculated from the strain components via (2.11), Figure 4.14(a), (b) and (c), indicates the residual stresses of three orthogonal directions along A-B line, C-D line and E-F line, respectively.

As shown in Figure 4.14, residual stresses of up to 250 MPa of quenched 7050 Aluminium alloy have been developed due to quenching. As shown in Figure 4.14(a), the components of residual stress post quenching through the thickness and transverse directions, $\sigma_{yy}$ and $\sigma_{zz}$, respectively, are compressive at the surface of the component. Since the thickness of the plate is much smaller than the length of the other two sides, the peak value of the stress component $\sigma_{yy}$ is lower than that of $\sigma_{xx}$ and $\sigma_{zz}$. As expected along A-B line (the rolling direction), $\sigma_{xx}$ measured close to the free surface of the block tends to zero. A large tensile $\sigma_{xx}$ residual stress (88.2 % of the $\sigma_{\text{yield (quench)}}$) exists in the core. In addition, for the transversal component $\sigma_{zz}$ in Figure 4.14(a), the z-component residual stress reaches its highest value around 235 MPa. At the surface, the $\sigma_{yy}$ residual stress is compressive (93.8 % of $\sigma_{\text{yield (quench)}}$) and becomes tensile (30.0 % of $\sigma_{\text{yield (quench)}}$) at an approximate distance 5 mm from the surface. Towards the core region of the block, $\sigma_{yy}$ tends to zero. In general, the fitting uncertainty in the measured residual stresses along rolling direction of the quenched block ranging from $\pm 4.0$ MPa to $\pm 6.7$ MPa.

It can be seen in Figure 4.14(b) along the thickness (y) direction, due to the square nature of the geometry, the stress distribution of x and z-components ($\sigma_{xx}$ and $\sigma_{zz}$) are very similar. Additionally, as shown in Figure 4.14(b), the stress distributions for
\( \sigma_{xx} \) and \( \sigma_{zz} \) are asymmetric. For \( \sigma_{zz} \) residual stress, the compressive stress value close to the top surface is about 45 \% of the \( \sigma_{\text{yield (quench)}} \) while that at the bottom surface is 90 \% of the \( \sigma_{\text{yield (quench)}} \). Similarly, the \( \sigma_{xx} \) compressive stress magnitude near the top surface is 51.2 \% of the \( \sigma_{\text{yield (quench)}} \) while the compressive stress value about the bottom surface is 99.8 \% of the \( \sigma_{\text{yield (quench)}} \). This variation is probably due to the fact that the bottom side of the block (through the thickness position) is immersed in the water first as it falls. As shown in Figure 4.14(b), at 5 mm from the surface the \( \sigma_{xx} \) and \( \sigma_{zz} \) residual stresses become tensile and approximately constant at 9 mm from the surface with peak values of 90.2 \% and 85.7 \% of \( \sigma_{\text{yield (quench)}} \), respectively. The \( y \)-component residual stress \( \sigma_{yy} \) is approx. zero at the centre of the block, as expected. In addition, these results all have the systematic fitting uncertainties from \( \pm 3.6 \text{ MPa} \) to \( \pm 5.5 \text{ MPa} \).

![Graphs showing residual stresses in quenched 7050 aluminum block via neutron diffraction](image)

Figure 4.14 Residual stresses in quenched 7050 aluminum block via neutron diffraction

a) A - B line   b) C - D line  c) E - F line.
In Figure 4.14(c), along with the z direction, $\sigma_{xx}$ residual stress has the value of 83 % of the $\sigma_{\text{yield}(\text{quench})}$ which is slightly smaller than the $\sigma_{xx}$ stress magnitude (95 % of the $\sigma_{\text{yield}(\text{quench})}$). For the $\sigma_{yy}$ stress component, it shares the similar trend with its counterpart in Figure 4.14(a). The error bar of the measured residual stress along E-F line is from ± 4.0 MPa to ± 6.0 MPa.

For the neutron results of the quenched specimen, it can be found that for some residual stress components, their residual stress magnitude is slightly smaller or larger than the yield stress $\sigma_{\text{yield}(\text{quench})}$ (250 MPa [2]) of as-quenched 7050 aluminium alloy. Similar phenomena also existed in other researchers’ studies. Stress magnitudes >200 MPa found in 7000 series components after cold water quenching via layer removal technique [99-102]. The neutron diffraction measurement results on thick 7000 forgings have confirmed the phenomena that the residual stress close to surface can be larger than the yield strength by a small degree[2].

Additionally, the uncertainties of measured neutron stress result of quenched sample range from ± 3.6 MPa to ± 6.7 MPa, which were subject to a systematic fitting error due to the application of a reference scattering angle, $2\theta_0$ measured from another sample with the same size and heat treatment. For the asymmetric stress distribution along the thickness direction of the aluminium block, the deviation between both sides of quenched block approaches approx. 45 % $\sigma_{\text{yield}(\text{quench})}$.

### 4.4.4 ND result of quenched and cold rolled material

The strain free lattice parameter measurements in the quenched & cold rolled block illustrated in Figure 4.15. The mean value of reference scattering angle measured across the path C-D is $79.939^\circ \pm 0.0086^\circ$. Note that the sample $\theta_0$ is diffracted with that of the quenched sample because both measurements were made during different visits to the ILL. The wavelength of the monochromatic radiation of the strain analyser for quenched & cold rolled sample was 1.72 Å.
The residual elastic strain components measured for the quenched & cold rolled 7050 aluminium alloy specimen is indicated in Figure 4.16. It can be seen from Figure 4.16, that the residual strain distributions for the quenched & cold rolled block were approximately symmetric about the central line (A - B line) of the block. As illustrated in Figure 4.16, the error in $\varepsilon_i^e$ ($i=1, 2, 3$) along with C - D line ranges from $\pm 93.75$ to $\pm 267.60 \mu e$ which significantly larger than that of the quenched sample. Comparing the strain data between quenched and quenched & cold rolled samples, it is clear that remarkable residual strain redistribution takes place after the cold rolling process. It can be seen that cold rolling makes $\varepsilon_{xx}$ develop a large tensile strain around the both top
and bottom surfaces of the block. In contrast, the magnitudes of $\varepsilon_{yy}$ and $\varepsilon_{zz}$ along C-D line have been remarkably reduced.

Figure 4.17 Residual stresses evolution along C-D line of 7050 aluminum block during quenching and cold rolling processes featured by neutron diffraction technique a) $\sigma_{xx}$ b) $\sigma_{yy}$ and c) $\sigma_{zz}$.

Figure 4.17 indicates the residual stresses of three orthogonal directions along C–D line. Along the thickness direction, for the longitudinal stress component $\sigma_{xx}$, tensile residual stress was measured at distances of around 1.5 mm from the both surfaces which have a peak value of 236 MPa. Low compressive residual stress, no more than 30 % of the uniaxial yield ($\sigma_{yield(CR 1.5\%)}$ 320 MPa), exists in the core part of the quenched & cold rolled sample. This tensile region is balanced by a compressive region of around -100 MPa (31 % of the $\sigma_{yield(CR 1.5\%)}$) at distances greater than 2 mm from the surface. For $\sigma_{zz}$ residual stress, the rolling process also has relaxed the residual stresses in the transverse direction, $\sigma_{zz}$. Little or no residual
stresses exist at the core of the block. Although peak tensile $\sigma_{xx}$ residual stress of about 31% $\sigma_{yield\,(CR\,1.5\%)}$ was found near the top surface, a generally compressive stress distribution exists 1 mm below the surface. Additionally, the rolling process has generated top surface tensile residual stresses in the thickness direction, with a similar magnitude to the transverse direction. Residual stresses which magnitudes close to zero exist at the core of the block. For the fitting uncertainties in the residual stresses of quenched and cold rolled sample, the values vary from $\pm$ 22 MPa to $\pm$ 28 MPa.

From the neutron results, it can also be found the measured $\sigma_{yy}$ residual stress (67 MPa) exists close to the top surface and near the bottom surface the compressive stress exists (-40 MPa). The biaxial nature of residual stress existed at material surfaces decides only $\sigma_{xx}$ and $\sigma_{zz}$ can have residual stresses. The $\sigma_{yy}$ residual stress which stress direction is perpendicular to the free surface should be 0 MPa. The relatively large $\sigma_{yy}$ residual stresses in the surface regions of the quenched and cold rolled sample, errors in the ND measurements near the top surface of quenched & cold rolled sample may be caused by the adoption of average reference scattering angle, $2\theta_0$ determined from the stress-free material. For the measured stress results via ND technique, a fitting uncertainty of up to $\pm$ 6.7 MPa was measured from the scatter in reference strain measurement of the quenched block. By comparison, a larger uncertainty of up to $\pm$ 28 MPa was determined during the reference strain measurement of quenched & cold rolled block. It needs to be noted that the calculated error bar only represents the fitting error between the Gaussian function and the corresponding neutron intensity peak. In practice, apart from the fitting error, partly filled gage volumes (when measuring the surface stress), oriented grain or material anisotropy all can cause the systematic errors in strain measurements.

### 4.5 Residual Stress Measurement Result via X-ray Diffraction Technique

#### 4.5.1 X-ray Diffraction Residual Stress Measurement Method and Preparation

In order to make a comparison with the residual stress result measurement by neutron diffraction technique, the Plustec $\mu$-X360 X-ray diffractometer [103] was adopted to measure the surface residual stress in the quenched & cold rolled material. For each measurement point, in this case, it only took several 2 ~ 3 minutes for the X-ray diffraction technique.
Before measurement, the Plustec $\mu - X360$ X-ray stress analyser was calibrated through measuring a stress-free coupon of 7050 aluminium alloy. To ensure the detector can capture the diffracted X-ray beam, the blocks were placed on a lifting platform and the angle between the sensor unit and horizontal plane should be adjusted manually or automatically. In this case, the angle between the X-ray incident beam and the horizontal plane is 25°. As shown in Figure 4.18, when the red laser point basically overlaps with the yellow box shown by the screen, the incident beam is aiming at the measurement point and will be detected by the sensor. Like ND measurement, the axes of the samples can be considered to align with the principal stress components, hence for each measurement point, two residual stress components along the length and width directions are measured. The Y axis shown in Figure 4.18 is corresponding to z axis in this study.

This device using a single tilt angle measures distortion in the complete Debye ring formed from diffraction by the $\{311\}$ planes [82, 104]. The peak position arising from diffraction from the Al $\{311\}$ planes was measured and the related diffraction angle ($2\theta$) ranged from 138° to 139°. The elastic parameters were taken from the literature [74]. Due to the limited penetration depth of X-rays (2 – 20 $\mu m$), only surface residual stresses measurements can be conducted.

![Figure 4.18 The interface of Pulstec X-ray machine.](image)

An array of X-ray measurements was conducted on the $62 \times 62$ mm² top and bottom surfaces on five specimens mentioned in Table 4.1. For every measurement point, the XRD measurement was repeated 3 times to reduce the possible error and reliability of
the measurement results. A total of 60 measurements were recorded. The result for every measurement point presented in the following figures is an average of three X-ray measurement results and the related standard deviations were calculated via equations [98].

### 4.5.2 Residual stresses

As shown in the FE results, the tendency for all heat treated & cold rolled samples attains increasingly tensile $\sigma_{xx}$ and $\sigma_{zz}$ residual stresses at surfaces. Generally, the X-ray measurement results have good agreement with the FE results. As shown in Figure 4.19 and Figure 4.20, the measurements made by X-ray on both top and bottom surfaces (Figure 4.10), and the magnitude of these surface residual stresses were found to increase with the degree of the rolling deformation ratio adopted. In Figure 4.19 and Figure 4.20, the deep blue bars represent the predicted surface residual stresses while the light blue bars are the X-ray diffraction measurements.

![Figure 4.19 Comparison of the FE results and surface residual stresses measured by X-ray diffraction in the centre of the top surface of blocks. a) $\sigma_{xx}$ b) $\sigma_{zz}$](image-url)

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Additionally, compared with the averaging fitting error of neutron diffraction result for the quenched block (ranging from 3.6 MPa ~ 6.7 MPa), the fitting error of X-ray diffraction result for the same sample was no less than 15 MPa. The highest error calculated was 38 MPa. As noted by Tanner et al [105], it is difficult for the \( \cos \alpha \) technique to find a fit to experimental data when the intensity of diffraction peak is possibly low caused by few diffracting domains at the detected region. The collimator that Pulstec X-ray analyser can provide could only lead to a 9.6 mm\(^2\) irradiated disc which is much less than that of another X-ray diffraction measurement method using \( \sin^2 \varphi \) technique (48 mm\(^2\)).

### 4.6 Contour measuring residual stress

#### 4.6.1 Cutting procedure and surface measurement

The contour residual stress measurement technique was performed. After the measurements via the non-destructive methods above, one cut was made to the quenched sample and another cut was made to the quenched & cold rolled sample and the deformations of the corresponding cut surfaces were measured. These displacements were applied to two FE models to determine the residual stresses in the longitudinal direction of the cutting surface of the block.

The specimens were cut by wire electric discharge machine (EDM). To perform cutting, two sides of were symmetrically clamped to minimize the plastic deformation of the cut surfaces. The cuts were made by a 0.25 mm diameter brass wire of EDM. The machine was set to “skim cut” settings to minimize the stress induced during cutting.
The blocks were EDM cut along the plane vertical to the symmetry plane of FE model as shown in Figure 4.21. For quenched and cold rolled sample, a cut was carried out at the middle plane vertical to the rolling direction (the mid-plane determined by C-D line and E-F line as shown in Figure 4.10). To make a comparison between the residual stress map of both quenched and quenched & cold rolled samples, a cut was also conducted at the same position of the quenched block.

Figure 4.21 the cutting direction and the location of cutting plane. a) the quenched sample b) the quenched & cold rolled sample.

The profiles of the deformed cut surfaces were measured in the Open University using a measurement procedure consistent with that reported in [96]. An Eclipse Crysta Plus 574 coordinate measuring machine (CMM) equipped with a 3 mm diameter Renishaw PH10M touch trigger probe was used which could achieve a measurement accuracy of 4.9 μm. As shown in Figure 4.22, for both blocks, the measurements were made in a 0.25 × 0.25 mm² grid and the profiles of the perimeter of the cut parts were recorded as well, given between 23,600 – 24,600 points on each cut surface.

Figure 4.22 The 7050 aluminium alloy sample during surface profile measuring for calculating residual stress via contour. a) The Eclipse CMM machine b) the measuring path.

During displacement data measurement, it is inevitable that the data detected by a probe of CMM machine contains some erroneous measurement points due to the
effect of metal scrap generated during EDM cutting. Therefore, the original displacement data from both sides were first processed to delete those erroneous points. To minimize the anti-symmetric errors (due to shear stress caused by wire or the artefacts), for each block, the two displacement datasets are averaged to get the average deformation of the two cut faces. Similarly, the two data sets about the perimeters of both surfaces were also averaged to form the common perimeter and this perimeter will be applied in building the perimeter of FE model.

Spline bivariate smoothing lines were filtered to the data using MATLAB so as to avoid the possible localized stress peaks in the following FE analysis caused by the noise of the raw data. During this procedure, the data of some regions of the cutting surface were fitted by cubic polynomials. Knot spacing, a parameter that can determine the spacing between the knots in the splines (the points from piecewise polynomials), was applied to determine the extent of data smoothing. For example, if the measurement grid is 0.50 × 0.50 mm², an initial 1 mm knot spacing is usually chosen to smooth the data (removing the noise in the deformation data) meanwhile ensuring the peak displacements of the cutting surfaces were retained [85, 106]. Since in this study measurement grid 0.25 × 0.25 mm² was adopted to detect the surface profiles of specimens, the optimum knot spacing was chosen from the calculated stress results for 0.5 mm, 1.0 mm, 1.5 mm, 2.0 mm, 2.5 mm, 3.0 mm, 3.5 mm, 4.0 mm. A fitting error could be given by the root-mean-square of all the nodal stress differences between the stress about the current knot spacing and that of the previous, coarser spacing spline solution. The detail can be found in [106].

Finally, these updated displacement data were imported into the corresponding nodal positions of an FE model used to predict the residual stress. This displacement data processing procedure was introduced by Prime and Dewald et al [74]. In this study, the MATLAB program created by Johnson [85] was developed.

### 4.6.2 Stress evaluation of quenched material

Erroneous points, such as outliers, were chosen manually and deleted from the deformation data via MATLAB program. For example, as the entry of EDM could usually remove extra material close to cut planes, the deformation data close to the edge of the quenched or quenched & cold rolled samples are invalid and should be abandoned before the next averaging step.

The data obtained from the measurement of cutting surfaces is described in Figure 4.23. The displacements of the cut surfaces of both sides were no larger than
± 0.060 mm and ± 0.070 mm, respectively. The data shows that the cutting surfaces were slightly non-symmetric. Prime and Kastengren et al [87] found that the phenomena above is probably due to the uneven clamping influence and it makes the assumption that a flat cut plane to the corresponding geometry of the specimen become invalid. Hence, in this case, it is necessary to average the two contours so as to remove any errors due to anti-symmetric cutting influences [87].

![Figure 4.23 Displacement of one of cut faces of quenched 7050 aluminium alloy block.](image)

The FE job about the quenching sample was run using the averaged and smoothed deformation data, to describe the longitudinal $\sigma_{xx}$ residual stress map as indicated in Figure 4.24.

![Figure 4.24 The averaged and smoothed contours created by the cuts and evaluated at surface nodal locations of FE contour model of quenched block.](image)
A 3D FE model of the quenched block was built via ABAQUS commercial software [107] employing symmetric condition. The elements of cutting surface $0.5 \times 0.5 \times 0.15$ mm$^3$ and the mesh was swept to the back face with increasingly larger elements which the maximum size is $0.5 \times 0.5 \times 1.5$ mm$^3$ as shown in Figure 4.25. Consequently, the model of the quenched specimen has 126,198 reduced integration, quadratic hexahedral brick elements (type C3D20R) and 535,200 nodes, among them, 20,281 nodes are at the cutting surface. The processed displacement data were implemented as the boundary conditions on the cutting surface. The material behaviour was assumed isotropic linearly elastic with a bulk Young’s modulus $E$ of 70.0 GPa, and a Poisson’s ratio of 0.35 [108]. In addition, the boundary conditions that were imposed in the FE model were that the $y$ and $z$ directions of a node at the centre of the other surface of model (opposite to the cut surface) were fixed and the $y$ direction of another node at the same surface was constrained as well so as to restrict the movement of the half block and its rotation, as illustrated in Figure 4.25. In this case, $\sigma_y$, $\sigma_{xy}$ are considered as 0 on the cut (free) surface in Figure 4.25.

![Figure 4.25 The finite element mesh of quenched & cold rolled sample.](image)

The optimum knot spacing that was used to fit bivariate splines to the displacement data was chosen via a method introduced by Prime et al [106] and Kapadia et al [109] about the relationship between the average stress uncertainty and different knot spacing. Figure 4.26 shows the contour result about quenched sample has the smallest error when the knot spacing is 3 mm and the stress uncertainties were still large although the data about knot spacing 1 mm or 2 mm were smoothed as well. Figure 4.27 indicates the stress distribution comparison along CD line between 3 mm knot spacing and 3.5 mm knot spacing.
To study if the mesh size can affect the stress result of FE quenching model or not, the FE analysis using different mesh densities were conducted. As shown in Figure 4.27, $\sigma_{xx}$ stress along CD line for models with element sizes of $0.5 \times 0.5 \times 0.15 \text{ mm}^3$ and $1.00 \times 1.00 \times 0.15 \text{ mm}^3$ were basically the same. The quadratic hexahedral element type C3D20R was applied in the FE analysis. It can be seen that the contour results with various mesh size have slight differences as the FE analysis paths of both are not exactly the same due to the different meshes.

Figure 4.26 The thermal-induced stress uncertainties about determining the optimum knot spacing in the bivariate smoothing spline.

Figure 4.27 The sensitivity studies for the longitudinal thermal-induced residual stress along CD line calculated via the contour method, cutting direction from left to right.
Figure 4.28 The quenched-induced $\sigma_{xx}$ residual stress maps of cutting plane via contour technique

The residual stress maps calculated from the smoothed displacement data of the cut surfaces of quenched blocks are shown in Figure 4.28. As illustrated in Figure 4.28, it can be found that after quenching, a magnitude of peak tensile residual stress ($\sim 0.9 \sigma_{\text{yield (quench)}}$) at the centre and a magnitude of peak compressive residual stress (around $1.14 \sigma_{\text{yield (quench)}}$) around the surface of the block were measured. In addition, it can be seen that the calculated residual stress map is not very symmetric to the central line. This may be because the anisotropy of material due to forging orientation could affect the heat transfer between material and water and the plastic deformation during EDM cutting could lead to the inaccuracy of cutting surface profile measurement. All of these can induce the asymmetries in the stress calculation.

4.6.3 Stress evaluation of quenched & cold rolled material

Figure 4.29 shows the displacement data of quenched and then cold rolled sample via the measurement of the cutting surfaces. The Figure 4.30 indicates the averaged and smoothed deformation data of both cut surfaces of the quenched specimen which were measured via a CMM machine.
Figure 4.29 Displacement of one of cut faces of quenched & cold rolled 7050 aluminium alloy block.

Figure 4.30 The averaged and smoothed contours created by the cuts and evaluated at surface nodal locations of FE contour model. a) the average contour of the quenched and cold rolled block. b) the cutting direction and the location of cutting plane.

For the three-dimensional FE models of half the quenched & cold rolled block, it has 127,889 elements with the same element type and 542,408 nodes, among them, 20,554 nodes are cutting surface nodes.
Figure 4.31 The stress uncertainties of quenched and rolled sample about determining the optimum knot spacing in the bivariate smoothing spline.

For the quenched and cold rolled sample, Figure 4.31 illustrates the contour result with 2.5 mm knot spacing has the smallest error. Figure 4.32 indicates the stress distribution along CD line of 2.5 mm knot spacing and 3 mm knot spacing. For 2.5 mm knot spacing, the peak tensile stress around the top surface is around half of the $\sigma_{yield(CR\ 1.5\%)}$ (320 MPa) and the peak compressive stress exists 5 mm away from the top surface is 10 % of the $\sigma_{yield(CR\ 1.5\%)}$, respectively. For 3 mm knot spacing, the residual stresses magnitude vary from 10 % to 45 % of the $\sigma_{yield(CR\ 1.5\%)}$, respectively.

Figure 4.32 The sensitivity studies for the longitudinal residual stress along CD line of quenched and rolled sample via the contour method.

The FE analysis using different mesh densities were also conducted for quenched & cold rolled block. As shown in Figure 4.32, $\sigma_{xx}$ stress distribution along CD line for both models with different element size are almost the same. The quadratic hexahedral
elements type of model is C3D20R. The element sizes are $0.5 \times 0.5 \times 0.15 \text{ mm}^3$ and $1.00 \times 1.00 \times 0.15 \text{ mm}^3$, respectively.

Figure 4.33 The $\sigma_{xx}$ residual stress maps of cutting plane of quenched & cold rolled block via contour technique.

Figure 4.33 shows the calculated $\sigma_{xx}$ residual stress maps for quenched & cold rolled specimen. They illustrate that after cold rolling tensile $\sigma_{xx}$ residual stress (up to 84 % of $\sigma_{yield(CR\ 1.5\%)}$) appears on the both top and bottom surfaces of 7050 aluminium alloy block. Compared with that of residual stress map of the quenched sample as shown in Figure 4.28, it can be found that compressive $\sigma_{xx}$ residual stress (no more than 27 % of $\sigma_{yield(CR\ 1.5\%)}$) has replaced the thermal-induced tensile stress in the core part of the material.

In the study of Prime et al [74, 106], the deformation of the cut tip during EDM cutting was also considered to affect the accuracy of the contour result. For the quenched & cold rolled sample, if the tensile residual stress appearing at the surfaces’ regions is large enough, it could induce the deformation of the material at the tip of the cut. This means the width of material removed in the regions close to the surface has been reduced compared with the ideal state. Hence, if the $\sigma_{xx}$ stress shown in Figure 4.33 is applied on the deformed surfaces, it may not make the surfaces back to flat. In other words, it could cause an error in the surface stress calculation if a cut tip displacement occurs.

4.7 Comparison of experimental residual stress measurements and FE results

4.7.1 The quenched specimen

The residual stress profiles of the quenched block along the rolling direction (line A - B), through the thickness of the component (line C - D) and through the transversal
direction (line E - F) can be seen from Figure 4.34 to Figure 4.37, respectively, in the three measurement directions.

Generally, considering the results of different measurement methods and FE analysis, the stress distribution of FE quenched is in good agreement with the ND, XRD and contour measurements.

![Graphs showing stress comparison](image)

Figure 4.34 Comparison of the neutron diffraction (symbols) and FE simulation (solid lines) results of the residual stresses measured along line A-B in the a) longitudinal (x), b) normal (y) and c) transversal (z) directions.
As shown in Figure 4.35, it can be seen that the FE predicted residual stress is more symmetric than the contour result as the thermal boundary condition is assumed to be same for the whole block surfaces. In reality, the anisotropy of material due to forging orientation could affect the heat transfer between material and water and the subsequent additional material loss during EDM cutting could lead to the inaccuracy of cutting surface profile measurement. All of these can induce the asymmetries in the stress calculation.
Figure 4.36 Comparison of the Neutron, X-ray diffraction, contour and FE simulation results of the thermal-induced residual stresses measured along line C-D in the a) longitudinal (x), b) normal (y) and c) transversal (z) directions.
Figure 4.37 Comparison of the neutron diffraction (symbols) and FE simulation (solid lines) results of the residual stresses measured from surface to centre along line E-F in the a) longitudinal (x), b) normal (y) and c) transversal (z) directions.

It can be seen in Figure 4.36 that similar stress distribution exists between the contour result, X-ray result, ND result and FE result. The only relatively large stress difference (90 MPa) between contour result and X-ray result exists close to the top surface. It can also be found that at both surfaces the stress magnitude measured via contour technique are relatively lower than that of stress value determined by X-ray and neutron techniques which may suggest that during EDM sectioning the compressive residual stress close to both surfaces may cause plastic deformation of material at the cut tip and consequently results in some error in the calculated stress magnitude at the corresponding regions.

4.7.2 The quenched & cold rolled specimen

Figure 4.38 shows the contour measured and FE predicted $\sigma_{xx}$ residual stress distributions for quenched & cold rolled sample. Considering the measured error due to inevitable material loss via EDM cutting and the possibility of plasticity at the tip during cutting, generally, the comparisons are in good agreement.
Figure 4.38 The comparison of $\sigma_{xx}$ residual stress maps of cutting plane of quenched & cold rolled block between a) contour result and b) FE result.

The comparison of $\sigma_{xx}$ stress distribution along CD line of quenched & cold rolled block is shown in Figure 4.39. Generally, the stress distributions have good agreement with each other. It can be seen that the neutron, X-ray, contour and FE results all indicate that large tensile residual stress appears around the surfaces of quenched material after cold rolling. However, as shown in Figure 4.39, the maximum $\sigma_{xx}$ tensile residual stress magnitude varied between the three measurement methods.
Figure 4.39 Comparison of the Neutron, X-ray diffraction, contour and FE simulation results of the residual stresses measured along line C-D of quenched & cold rolled sample in the a) longitudinal (x), b) normal (y) and c) transversal (z) directions.

4.7.3 Discussion

To study the influence of cold rolling on quenched 7050 aluminum alloy specimens, FE models have been developed to predict the residual stress distribution in quenched & cold rolled samples, which have been validated by comparing the results to measurements from a range of residual stress measurement techniques — two non-destructive techniques, neutron and X-ray diffraction and the destructive contour measurements were performed.

Generally, the results measured by three different methods are in good agreement. All measurement results indicate that cold rolling converts the large tensile stress in the core of the post-quench block into relatively small compressive stresses and the post-quench compressive stresses on the surface into large tensile stresses. Since the depth of the regions that surface tensile residual stress exists is very shallow, thus the average reference scattering angle may not represent the reference scattering angle measurements of the whole material. According to Robert et al [110] and Wither et al [111] studies about the effects of different factors on determining reference lattice parameters, it can be seen that $\theta_0$ varies from position to position along analysis path (A-B line or C-D line) in Figure 4.12 and Figure 4.15, hence, for the cold rolled sample, the different $\theta_0$ spacing effect could lead to the large stress variation due to the $\theta_0$ value varying from position to position. The large strain gradient in the region close to the surfaces, shown in Figure 4.16, makes it difficult to determine the general $\theta_0$ value of the quenched & cold rolled material and the exact $\theta_0$ value of the regions close to both rolling surfaces, whose effect on residual stress distribution in a rolled material.
needs to be further investigated. Therefore, the surface $\sigma_{yy}$ residual stress magnitude which is not around 0 MPa were measured. For the X-ray technique, the relative larger fitting uncertainty is due to the high scatter during X-ray measurement. It is caused by the less diffracting domains for the $\cos \alpha$ technique, compared with the conventional $\sin^2 \phi$ technique. For the contour technique, the measured cut planes may be partially plastically deformed and the residual stress magnitude, especially for the regions close to surfaces, was possibly underestimated due to this.

4.8 Summary

The influence of cold rolling on the distribution of residual stresses in a quenched 7050 Al alloy block has been quantified by using the neutron diffraction, X-ray diffraction and contour techniques. These measurement methods were performed on a quenched block and a quenched & cold-rolled block. The comparison of the ND, XRD experimental data and the simulated FE results demonstrate a good agreement. However, for the quenched block, FE predicted compressive residual stress was up to 90 MPa larger than those measured by ND method and contour methods, in a narrow region close to the top surface of the sample. In addition, for quenched & cold rolled block, the residual stress measured by ND and XRD techniques and those predicted by FE are much larger than the contour result, in a small region near the top surface of the material. The possible error sources were analysed in the Neutron, X-ray diffraction and the contour techniques. Significant stress redistribution occurred during the cold rolling process. According to FE prediction, the increase of deformation ratio of rolling could lead to the growing tensile stresses around the surface of the material meanwhile the relatively lower residual stress magnitude in the core part of the sample. Provided that the surface stresses can be further relaxed by other techniques e.g. constrained aging, surface machining, cold rolling could be considered as an effective technique to remove residual stresses in quenched plates.
Chapter 5  Prediction of residual stresses in quenched and cold rolled T-section specimens

5.1 Introduction

Generally, extra-large plate-like structures for modern aircraft are made from heat treatable high strength 7XXX series aluminium alloys [2]. For Airbus A-380, some plates can be as long as 36 m [9]. A wide range of aviation components forming the wing, fuselage, tail fin and horizontal stabilizer are manufactured from T-section billets [112, 113]. Since the final profiles of most aviation components are complicated, thus it is very difficult to make them uniform plastically deformed via cold working techniques. Hence, it is essential that the level of residual stresses should be mitigated as far as possible in the T-section billets. The typical manufacturing process of critical structural components with stiffened ribs involves a number of operations as illustrated in Figure 5.1. Given the fact that sufficient residual stress remains in the material after multiple cold compression, cold rolling may be an option to replace the multiple cold compression process.

![Diagram]

Figure 5.1 Schematic illustration of a typical manufacturing process for extra-large plate like aluminium components.

For cold rolling to relieve the residual stresses in a material, although some researchers have adopted it in reducing RS in forged steel [72, 73], relatively little work has been performed on cold rolling of heat-treated T-section panel component. In
addition, although experimental results in Chapter 4 have shown that cold rolling can significantly minimize residual stresses in the core part of quenched aluminium blocks, its influence on stresses distribution of heat-treated T-section components also needs further study. In this study, under the laboratory condition, scale-down T-section forgings were formed and the effect of cold rolling on residual stresses in these specimens were analysed.

5.2 Prediction of residual stress removal through cold rolling operation in scale-down T-section forgings

As mentioned in Chapter 4, cold rolling was performed to plastically deform the quenched work-piece by 1.5 ~ 3 % hence significantly removing the residual stresses in the core part of the component. In addition, the FE results in Figure 4.9 show that, with the increase of roll diameter, the stress magnitudes in the core part of block specimens can be further relaxed to some extent.

The main aim of this section is to develop an integrated numerical modelling technique, incorporating a dislocation-based material model, to simulate the SHT and quenching process in scale-down, stiffened T-section plate like component and predict the resultant residual stress distribution, and subsequently predict the residual stress reduction through cold rolling operations. The integrated modelling method has been verified by experimental results measured via X-ray diffraction technique.

5.2.1 Numerical Model

The geometry and dimensions of the scale-down T-section specimen considered in this work are illustrated in Figure 5.2(a), in this study, given the furnace size of our laboratory, a plate of length 450 mm has been considered. The full scale of a component can be seen in Figure 6.1. Two planes of symmetry exist in the geometry, however, only one of these may be exploited to simplify the FE model, due to the nature of the quenching procedure, as shown in Figure 5.2(b). The coordinate directions are defined such that Z is along the length of the plate/stiffener, X is transverse to the stiffener and Y is through the thickness of the plate, with the origin defined at the mid-length and mid-width position of the plate at the top of the plate (i.e. the flat region). The cooling rates/temperature distribution during quenching is affected by the orientation that the component enters the water. Since in industrial practice, it is not feasible to lower the extra-large component into the water in the longitudinal direction, as it is not cost-effective to build such a deep pool and more importantly, it
would take a longer time for the component to be completely immersed in water, which could significantly decrease the cooling rate of the whole component. Hence large-sized components are lowered into the water as illustrated in Figure 5.2(b).

Figure 5.2 Model of quenching and cold compression processes: (a) T-section component geometry (dimension in mm). (b) Illustration of the FE model showing the lowering direction. (c) Thermal cycle for heat treatment and cold compression for 7050 Al alloy.

A schematic illustration of the heat treatment process employed in the current model is shown in Figure 5.2(c), the first stage of the model is the removal of the component from the furnace after SHT, i.e., the third section in Figure 5.2(c), where the component is uniformly at 480 °C. It takes 5 s to transfer the component in air before one end of the component first makes contact with the water. Subsequently, along with the same immersion direction of the large-sized component, it is quickly lowered into the water taking 0.8 s to completely immerse the component. The component cools down in the water, which remains constant and uniform at 20 °C to maximize the thermal-induced
residual stress in the material. Finally, cold rolling is applied to reduce residual stress induced during quenching.

5.2.1.1 Thermal Model of the Quenching Process

As the component has been held uniformly at high temperature for a substantial period of time, the residual stress resulting from the earlier manufacturing processes such as hot forging, as described in Figure 5.1, will have relaxed, and it is reasonable to assume that the stresses in components are negligible before water quenching. The numerical simulations of stress/deformation analysis and of thermal analysis were carried out by using the same finite element mesh, except for different element type and boundary conditions. DC3D8 elements were employed in the thermal analysis and C3D8R elements were adopted on the mechanical analysis. The mesh is illustrated in Figure 5.2(b). The 3D T-section model included 151200 elements. The mesh close to the rib region was refined, where large stress gradients are expected after rolling, the element size is 1 mm × 1 mm × 2 mm, the mesh size of the rest material is 2 mm × 2 mm × 2 mm rectangular element. Both radiation and convection heat transfer boundary conditions are applied to the T-section component. The ambient temperature is taken to be 20 °C and the emissivity ratio was set to 0.3 [93]. The thermal properties are the same as that applying in aluminium alloy blocks mentioned in Section 4.2. During the quenching process, the dominant heat transfer mechanisms between the T-section component and its surroundings change, thus, during the lowering period (Figure 5.2(c)), the air radiation between component and atmosphere is gradually replaced by the heat transfer between the specimen and water.

5.2.1.2 Mechanical Model of the Cold Rolling Process

In the cold rolling model, the rolls are assumed to be discrete rigid bodies, as illustrated in Figure 5.3. The final state of the quenched T-section component in the FE model is used as the initial state for the cold rolling model. An arbitrary rolling velocity of 1 rad s⁻¹ was used for all analyses. The friction coefficient between rolls and sample was set as 0.1 [95]. The cold rolling process is divided into two displacement controlled stages: (i) the specimen is pushed towards the roll gap with an initial constant velocity of 2 mm/s until it makes contact with the rolls (ii) once the specimen touches the roll, the movement of the sample is decided by the both rotating rolls.
Figure 5.3 Schematic of the rolls and the paths AA and BB’ for residual stress analysis. (Points A and B are located on the top surface of component, while points A’ and B’ are on the bottom surface.)

A cross-sectional view of the 3D rolling model is illustrated in Figure 5.3. Symmetry condition has been exploited in the rolling model (see Figure 5.3) and half model has been employed, assuming that the residual stress distribution after quenching was relatively symmetric about Symmetry Plane I (as will be shown later). The top rolls are solid cylindrical bars with 240 mm long and 160/240/480 mm diameters. The length and diameter of the bottom rolls are identical to that of the corresponding top rolls, but it has a circular groove 20 mm wide and 8 mm deep to enable the rib part of T-section component to be rolled. The grooved profile on the bottom roll is shown in Figure 5.3. Since the grooved depth of the bottom roll is fixed at 8 mm, the rib and plate parts of the T-section component are subject to a different deformation ratios during the rolling process and some non-uniform plastic strain could be caused by the cold rolling process.

5.2.2 Thermal and Residual Stress Distributions due to Quenching

Figure 5.4 shows the transient temperature distributions at five locations across the T-section component (along with the x-axis) during the quenching process. These five points, distributed evenly through the width of the plate at 19.5 mm intervals, are located at the mid-length of the plate (z = 0 mm) and positioned 8 mm from the top surface as indicated in the inset in Figure 5.4 to enable the differences in the first few
seconds to be seen clearly. There is little temperature decrease during the air cooling period (0 – 5 s). The surface containing Point 1 touches the water first at time 5 s (see Figure 5.5(b)) and during the lowering period (from 5 to 5.8 s), Points 2 to 5 are sequentially immersed into the water. It can be seen that since Points 1 and Point 5 are at exactly the same position relative to the bounding surfaces of the component, thus after the component is fully immersed into water, the temperature of Point 5 drops faster than that of Point 2-4.

![Cooling curves at different locations in T-section component during quenching in 20 °C water.](image)

Figure 5.4 Cooling curves at different locations in T-section component during quenching in 20 °C water.

Figure 5.5 shows the predicted temperature distribution of T-section component during quenching. As shown in Figure 5.5(a), the effect of air cooling during the transfer period on temperature is very limited. The surface temperature of the T-section component dropped dramatically when it began to contact with water. (Figure 5.5(b) and Figure 5.5(c)). Figure 5.5(d) illustrates the temperature map of the component when the whole component has been immersed in the water for 1 s. At this time, it can be seen the thermal gradient between surface and core part of the material is significant.
Figure 5.5 Contour plot of the temperature distribution during various stages of the quenching process. (a) After 5 s transfer time (air cooling), (b) Initial stages of quenching (5.3 s), (c) Over half the sample immersed (5.5 s) (d) 1 s after full immersion (6.8 s).

The corresponding contour surface plots are given in Figure 5.6 and the expected trends are seen. Residual stresses of up to 227 MPa the uniaxial yield stress (250 MPa [2]) have been developed due to quenching. Through the thickness (y) direction, Figure 5.6 shows that $\sigma_{xx}$ and $\sigma_{zz}$ are compressive at the top and bottom surfaces and are tensile towards the centre, while the $\sigma_{yy}$ stress remains tensile and tends to zero at both ends, as required. In addition, it can be seen from Figure 5.6, that the stresses are generally symmetric about the $y$-$z$ plane (Symmetry Plane I in Figure 5.2(a)) which implies that the fact that Point 1 enters the water before Point 5 has limited effect on the overall residual stress distribution of this T-section component. This justifies the use of a half geometry model along symmetry Plane I in the cold rolling model, thus reducing computational time.

Figure 5.6 Contour plots of the post quench residual stress distribution in the three coordinate directions.
5.2.3 Residual stress evolution by cold rolling

The effectiveness of cold rolling technique on residual stress relaxation has been evaluated by comparing the stress distributions in the T-section component before and after the cold rolling process. The residual stress distribution in the T-section component may be influenced by several factors during rolling operation, such as rolling deformation ratio and roll diameter.

5.2.3.1 Rolling deformation ratio effects

Contour plots of the residual stress distribution in quenched & cold rolled T-section panels under 1.5 % and 3 % deformation ratio via rolls with 80 mm radii are presented in Figure 5.7 and Figure 5.8, respectively. It should be noted that the deformation ratio varies from the rib and the plate which could cause non-uniform plastic deformation. When the deformation ratios of rib part are 1.5 % and 3 %, the compression ratios of plate region are 2.25 % and 4.5 %, respectively.

Figure 5.7 Residual stresses distribution after 1.5 % cold rolling.
Compared to the post-quench residual stress distribution shown in Figure 5.6, it can be seen from Figure 5.7 that after 1.5 % cold rolling, the thermal-induced residual stresses in the core part of the material has a remarkable decrease in the three coordinate directions, with the percentage peak stress reduction ranging from up to 88 %. The results also illustrate that with the increase of roll deformation ratio, the surface compressive residual stresses can be converted into tensile residual stresses. The larger the deformation ratio, the higher the surface tensile stress magnitude. When the deformation ratio comes to 3 % (rib region), for $\sigma_{xx}$ and $\sigma_{zz}$, the maximum tensile surface residual stress could reach 348 MPa and 131 MPa. In contrast, in the core part of the material, i.e. within the region 4 mm from both sides, for the maximum $\sigma_{xx}$ and $\sigma_{zz}$, it predicted that they can be up to 7.5 MPa and 67.3 MPa, respectively. In addition, the various thickness reduction has little effect on stress in the $\sigma_{yy}$ component.

Figure 5.8 Residual stresses distribution after 3 % cold rolling.
Figure 5.9 and Figure 5.10 indicates the residual stress profiles along the path AA’ and BB’ (see Figure 5.3), where 0 % represents the profiles before compression. It shows at lower deformation ratio, i.e. 1.5 % (rib region), the stress magnitude in the core part can be minimized effectively. Although a relatively larger deformation ratio i.e. 3 % (rib region) can also get satisfactory stress reduction in the core part, the surface tensile stress magnitude also increases as well.

Figure 5.9 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (AA’) of the T-section component after rolling to different deformation ratios, roll radii 80 mm.
Figure 5.10 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the plate region (BB') of the T-section component after rolling to different deformation ratios, roll radii 80 mm.

5.2.3.2 Roll radius effects

As illustrated in Figure 5.9 and Figure 5.11, the various roll radii also affects the resultant residual stress distribution in T-section panels. The predicted results indicate that with the growth of roll radii (80 mm/ 160 mm/ 240 mm) and 1.5 % deformation ratio, the surface stress magnitude of both $\sigma_{xx}$ and $\sigma_{zz}$ can be further reduced. These phenomena suggest that a set of huge roll could make the heat-treated T-section specimen further relaxed, including both the surfaces and core part of the material.
Figure 5.11 Residual stresses distribution of heat-treated T-section components post cold rolling (roll radius 240 mm, 1.5% rolling deformation ratio).

Figure 5.12 and Figure 5.13 indicate the residual stress distributions after 1.5% cold rolling (rib region) along the path AA’ (rib region) and BB’ (plate region) under various roll radius conditions. It can be seen that, when the roll radius grows to 240 mm, for the rib part, within the core region between boundaries at 3.5 mm to both sides, the peak stress value of $\sigma_{xx}$ and $\sigma_{zz}$ are 8.5 MPa and 55.6 MPa which are relatively smaller than that of specimen rolled by roll with 80 mm radius. For the top and bottom surface regions, the maximum $\sigma_{xx}$ and $\sigma_{zz}$ are 10.0 MPa and 185.2 MPa, respectively, which are also lower than the results at other conditions. For the plate region, the residual stress distributions along three components for different roll radii conditions also show a similar tendency. There is no remarkable change for the $\sigma_{yy}$ component.
Figure 5.12 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (AA') of the T-section component rolled by roll with various radii, 1.5 % rolling deformation ratio.

As shown in Figure 5.7-Figure 5.13, it can be seen that a 1.5 % rolling deformation ratio and a set of as large as possible rolls are the ideal condition in relaxing the $\sigma_{xx}$ and $\sigma_{zz}$ stresses at the whole specimens. In addition, the simulation results show that the effect of cold rolling on $\sigma_{yy}$ residual stresses distribution in quenched and cold rolled T-section panels is quite limited. Given the laboratory conditions, residual stresses for heat treated T-section components rolled to deformation ratios of 1.5 % and 3 % via a set of 80 mm radius rolls were measured via X-ray diffraction technique to verify the FE results.
Figure 5.13 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the plate region (BB’)
of the T-section component rolled by roll with various radii, 1.5 % rolling deformation ratio

5.3 The application of cold rolling to relief residual stress in SHT treated scaled-down T-section component

As mentioned in Section 3.2, it is impractical to build a die set long enough to cover the whole component, while research shows that cold compression could induce complex stress distribution in T-section component [39]. Hence, considering the fact that cold rolling can mitigate the residual stresses magnitude in the core part of an aluminium block, the aim of this section is to study the influence of cold rolling on removing residual stresses in quenched T-section component.
5.3.1 Experimental procedure

To investigate the cold rolling process on residual stress distribution in T-section component, a set of rolls for the scale-down T-section component have been designed and manufactured from EN24T steel [114]. The influence of cold rolling on as-quenched scaled down T-section component has been validated by using this small rolls as illustrated in Figure 5.14. The detail of roll assemblies including main components can also be seen in the corresponding Appendix B.

Figure 5.14 The configuration of the rolling machine and the location of the rolls

Three rectilinear 28 × 160 × 450 mm³ 7050 - T74 Aluminium alloy forgings were used in this test. The experimental specimens are scale-down T-section components which were sectioned from the forging mentioned above, as shown in Figure 5.15.

For the three components with the same T-cross section, they were solution heat treated at 480 ± 5 °C for 3~4 hours followed by immersing into the agitated water at 20 °C along transversal (X) direction (Figure 5.2), so as to maximize the thermal-induced residual stress. After rapid cooling, two of them were 1.5 % and 3 % (rib region) cold rolled within half an hour so as to relieve possible natural aging effects.

For quenched and quenched & cold rolled T-section components, the Plustec μ-X360 X-ray diffractometer was applied to measure the surface residual stress in the quenched & cold rolled material. As shown in Section 5.2, along with the longitudinal (Z) direction of T-section panel, apart from two regions suffering edge effect i.e. within
15 mm to both ends, the most material (middle part of the component) has steady stress distribution according to FE prediction. It means the surface residual stresses distribution can be illustrated by measuring residual stresses along two measurement paths at both the top and bottom surfaces of each component as shown in Figure 5.15. There are 13 measurement points for both top and bottom surfaces. For the top surfaces of both plate regions, there are 5 points at 5 mm spacing between each other and the point at the outside is 5 mm to one side. For the rib region, there are 3 measurement points. One point is located at the junction of the two centre lines. At the bottom surface, the distributions of the 13 points are corresponding to the points at the top surfaces. To verify the accuracy of X-ray results, each point was measured three times. For quenched and quenched & cold rolled components, a total of 234 measurements have been carried out.

![Diagram](image)

Figure 5.15 The X-ray diffraction measurement points for surface residual stress of T-section component (Dimension: mm).

### 5.3.2 Stress evaluation in quenched and quenched & cold rolled T-section component.

#### 5.3.2.1 Stress evaluation of the quenched T-section panel

As predicted in Section 5.2, for heat-treated T-section panels, the residual stresses existed at surface regions are compressive. As shown in Figure 5.16, the measured stresses show good agreement with the numerical results. It indicates that, generally, the $\sigma_{xx}$ and $\sigma_{zz}$ residual stresses are symmetric around the Symmetry Plane I in Figure 5.2.
Figure 5.16 Comparison of the residual stress predictions from FE simulations with X-ray diffraction measurements in quenched T-section specimen a) AA’ analysis path b) BB’ analysis path.

5.3.2.2 Stress evaluation of the quenched & cold rolled T-section panel

Figure 5.17 and Figure 5.18 illustrate the stress distribution of quenched and cold rolled T-section panels under 1.5 % and 3 % rolling deformation ratio. Generally, the experimental results agree well with the FE predictions. After cold rolling, the surface compressive residual stresses convert into tensile residual stresses. With the increase of rolling deformation ratio, it can be seen the stress magnitudes of $\sigma_{zz}$ at both surfaces all have an increase to different extents. However, for the $\sigma_{xx}$ component, comparing the $\sigma_{xx}$ stress distributions around both surfaces of materials of 1.5 % and 3 % rolling deformation ratios in Figure 5.17ai, bi and Figure 5.18ai, bi, the $\sigma_{xx}$ stress magnitude does not have a significant change under various rolling deformation ratio conditions.
Figure 5.17 Comparison of the residual stress predictions from FE simulations with X-ray diffraction measurements in heat-treated and 1.5 % cold rolled T-section specimen
  a) AA' analysis path b) BB' analysis path.
Figure 5.18 Comparison of the residual stress predictions from FE simulations with X-ray diffraction measurements in heat-treated and 3% cold rolled T-section specimen a) AA’ analysis path b) BB’ analysis path.

5.4 Summary

To apply the cold rolling technique to control the residual stresses in T-section components, a set of rolls were designed and manufactured to be used in this study. A model for simulating the heat treatment and the subsequent cold rolling has been developed and verified by X-ray diffraction measurements at different stages of the processes. After 3% cold rolling, peak residual stress up to 338 MPa (96% of $\sigma_{y\text{ield(CR 3%)}}$ 350 MPa) was measured. Such large stress components exist as the stresses were highly bi-trial around the material surface. The phenomenon is caused by two factors. One is the friction between the component and both rolls, the other is the non-uniform plastic deformation during the cold rolling mentioned in Section 5.2.3.1.
Residual stress measurements were made using X-ray diffraction which generally has been found in good agreement with the numerical results. In addition, the possible source of deviation has been identified in X-ray diffraction technique. It can be found that for some data, the related error can approach 40 MPa. In the XRD measurement, the existence of a thin oxide layer and the related stress generation due to oxidation can affect the accuracy of X-ray results to some extent [115, 116] and make the Pulstec X360 machine take a longer time to evaluate the stress value of measurement point. Grinding the material surface can effectively remove the oxidation layer and make the measurement job quicker, but it can also influence the surface residual stress distribution. It is because the oxide layer depth (0.06 μm) is within the penetration depth of X-ray and its distribution on both surfaces is random, thus it is inevitable to remove both the oxidation layer and material itself during grinding. Consequently, it will affect the accuracy of the subsequent X-ray measurement. Therefore, the measurement was conducted on specimens without surfaces grinding.

The FE results reveal the influence of work parameters, e.g. rolling deformation ratio and roll radius, on the residual stress distribution in materials. It predicts that for scale-down T-section component, a rolling deformation ratio around 1.5 % and a set of huge rolls are the ideal condition of making scale-down T-section panels with low residual stresses.
Chapter 6  Numerical analysis of
cold working technique effect on
relieving residual stresses in large-
sized SHT treated T-section
component

6.1 Introduction

For large-sized aerospace aluminium alloy forgings, given the cost, a cold-work compressive stress relief process is usually adopted to reduce the quench-induced residual stress. However, the multiple compression process [12, 41] does not make the SHT treated component fully stress relived. Moreover, after compression and the subsequent artificial aging, as illustrated in Figure 5.1, the retained stresses are still large enough to lead to distortion in forged aviation components [117]. In addition, another cold working technique, cold stretching is also used in mitigating residual stresses in aviation components, but it is seldom applied on extra-large components because of the non-uniform loading due to the handling problem.

Hence, as discussed in Chapter 4 and Chapter 5, the cold rolling process may be considered as an alternative method to replace the current cold compression method in relieving residual stresses in large-sized T-section forgings. In this chapter, the reasons why sufficient thermal-induced residual stresses remain in the large-sized T-profile components after cold compression are analysed via FEA. Modelling of reducing residual stresses in heat-treated T-section component by cold compression has been built. Then the influence of cold rolling on large-sized T-section component is investigated through numerical analysis. Although the FE results need to be further confirmed by trials in an industrial level, the optimised processing parameters for forming extra-large T-section components with low residual stress recommended by FEA could provide guidance for the future industrial production.
6.2 Estimation of residual stresses in quenched & cold compressed large-sized T-section components

Since multi-step cold compression operations are required with a certain length of overlap between operations for some extra-large components. Consequently, complex stress states can be generated in the overlapping area which affects the dimensional accuracy and consequently the life expectancy of in-service components.

Tanner and Robison [39] studied the influence of cold compression in sections, denoted ‘bites’, on residual stress distribution in 7449 aluminium alloy in a relatively small block of material (length 550 mm), and found that high tensile stresses are generated in the longitudinal and transversal directions, in the vicinity of the overlap region at a location at the end of the initial ‘bite’. Prime et al. [40] compared the predictions obtained from a 2D FE analysis and contour measurement results for a multi-step compression process with overlap on a 7050 Al alloy block and found that the stress predicted showed periodicity with an interval corresponding to the length of the compression die, less the length of overlap.

The main aim of this section is to simulate the SHT and quenching process in extra-large T-section panels and predict the resultant residual stress distribution, and subsequently predict the residual stress reduction through multi-step cold forging operations. In particular, the residual stress redistribution in overlap regions caused by multiple compression is investigated.

The residual stress generation due to SHT and subsequent quenching has been modelled, followed by the cold compression process. The residual stress redistribution produced by the cold compression operation and the influences of processing parameters have been studied in detail. The integrated modelling method has been verified by simulating two cases for bulk-forming operation published in the literature [35] [39] and very good agreement between the predictions and measurements was found.

6.2.1 Finite element model

The geometry and dimensions considered in this work are illustrated in Figure 6.1. For this project, a plate of length 5 m has been considered (the influence of plate length is examined later). Two planes of symmetry exist in the geometry, only one of these will be exploited to simplify the FE model, due to the nature of the quenching procedure, as shown Figure 6.1(a), the coordinate directions are shown as well.
A schematic illustration of the heat treatment process employed in the current model is shown in Figure 6.2. According to Prime et al. [26, 40] and Robinson et al. [27], the deleterious effect of large residual stresses should not be ignored when cold water (25 °C) is used as a quenchant. However, it will hinder the formation of the desired super-saturated solution if the component is quenched in boiling water (around 100 °C). Therefore, to ensure quenching with adequate cooling rate whilst reducing the quench induced residual stresses, the water temperature is usually chosen between 60 °C and 70 °C. In industry, a huge water pool (sometimes over 20 m long) is built to quench extra-large T-section components. In addition, several agitators are installed at the bottom of the pool to keep the water temperature constant. For the current study, the water temperature is taken to be 65 °C. Considering the component size, it takes 15 s to transfer the component in the air before one end of the component first makes contact with the water. Subsequently, it is quickly lowered into the water with a
constant velocity of 360 mm s$^{-1}$, thus taking 2 s to completely immerse the component. The component cools down in the water, which remains constant and uniform at 65 °C. Finally, cold working techniques are applied to reduce residual stress induced during quenching. The 3D T-section model included 299,884 elements. The mesh close to the rib region was refined, where large stress gradients are expected after rolling, the element size is 12 mm × 5 mm × 5 mm, the mesh size of the rest material is 12 mm × 10 mm × 10 mm rectangular element.

6.2.2 Model of the residual stress relieving process via compression

Since the large-sized T-section component has been held at SHT temperature for 6 - 9 hours, it is reasonable to assume that the stresses in components are negligible before water quenching. The numerical simulations of an uncoupled heat transfer FEA and then a stress/deformation analysis were conducted with the same finite element mesh and different element type and boundary conditions. DC3D8 elements were employed in the quenching analysis and C3D8R elements were applied to the stress/deformation analysis. The mesh is demonstrated in Figure 6.1(b). During the quenching process, both radiation and convection heat transfer boundary conditions between the aviation component and the environment are considered. The air temperature is taken to be 65 °C meanwhile the water temperature is 20 °C and the emissivity ratio was set to 0.3 [93]. The thermal properties of the large-sized component are the same as that applying in specimens mentioned in Chapter 4 and 5. During the lowering period (Figure 6.2), the air radiation between component and air is gradually replaced by the heat transfer between the extra-large component and the water in a huge pool.

For the mechanical model of the cold compression process, since the thickness of ribbed region, $H_{\text{rib}}$, and plate region, $H_{\text{plate}}$, of T-section component differ, an initial gap, of height $H_{\text{gap}}$ is required so that the compression ratio in both regions are equal. Note that $H_{\text{gap}}$ may be calculated as the product of ($H_{\text{rib}}$ - $H_{\text{plate}}$) and the compression ratio.

In the cold compression model, the dies are assumed to be discrete rigid bodies. As illustrated in Figure 6.3, the top die is flat and the bottom die is stepped to accommodate the T-section geometry. The final state of the quenched T-section component in the FE model is used as the initial state for the cold compression model. An arbitrary compression speed of 1 mm s$^{-1}$ was used for all analyses, as speed has little influence on simulation results at room temperature according to Koc et al. [35] The cold forging process is divided into three displacement controlled stages: (i) the top die is moved downwards to compress the component (loading), (ii) the die is held at
constant displacement (holding), and (iii) the top die is moved upwards to remove the applied load (unloading) and the residual stress develops.

![Diagram of die and paths for residual stress analysis](image)

Figure 6.3 Schematic of the die and the paths AA' for residual stress analysis. (Point A is located on the top surface of component, while point A' is on the bottom surface.)

In the analysis, the bottom die is fully fixed in position, whereas the top die is free to move in the vertical direction during the compression process. A cross-sectional view of the 3D compression model is illustrated in Figure 6.3. Full symmetry conditions have been exploited in the compression model (see Figure 6.3(a)) and a quarter model has been employed, assuming that the residual stress distribution after quenching was relatively symmetric about Symmetry Plane I (as will be shown later). The top die is a cuboid of width 400 mm, height 50 mm. Its length depends on the component size. The length and width of the bottom die are identical to that of the top die, but its height is dependent on the value of the compression ratio required. The height profile of the bottom die is shown in Figure 6.3. The bottom die is designed such that the rib and plate in the T-section component are subject to the same deformation ratio during the forging process, thus minimising any non-uniform plastic strain caused by the cold compression process.

### 6.2.3 The prediction of temperature and stress distribution in large-sized T-section panel caused by quenching

Figure 6.4 shows the transient temperature distributions at five locations across the T-section component (along with the x-axis) during the quenching process. These five points, distributed evenly through the width of the plate at 180 mm intervals, are located at the mid-length of the plate (z = 0 mm) and positioned 50 mm from the top surface as indicated in the inset in Figure 6.5 to enable the differences in the first few seconds to be seen clearly. There is little temperature decrease during the air cooling period (0 – 15 s). The surface containing Point 1 touches the water first at time 15 s.
(see Figure 6.5(b)) and during the lowering period (from 15 to 17 s), Points 2 to 5 are sequentially immersed into the water. It can be seen that negating the 2 s time difference in time for Points 1 and 5 to enter the water, both points have similar thermal profiles during quenching, as is the case for Points 2 and 4. This is because that Points 1 and 5 (and Points 2 and 4) are at symmetric positions, i.e. at exactly the same position relative to the bounding surfaces of the component.

Figure 6.4 Cooling curves at different locations in extra-large T-section component during quenching in 65 °C water.

Figure 6.5 Contour plot of the temperature distribution during various stages of the quenching process. (a) After 15 s transfer time (air cooling), (b) Initial stages of quenching (15.6 s), (c) Over half the sample immersed (16.2 s) (d) 3.5 s after full immersion (20.5 s).

Figure 6.6(b) shows the post-quench residual stress distribution in the x, y and z coordinate directions through the thickness of the plate from the flat surface (y = 0 mm)
to the surface of the rib ($y = 120$ mm) i.e. the AA’ line in Figure 6.6(a). Figure 6.6(c) shows the three stress components in a path along the length of the plate (BB’ Line) and at a depth 50 mm below the flat surface (without rib). Note that $z = 0$ mm corresponds to the centre of the component and $z = 2500$ mm to the surface. The corresponding contour surface plots are given in Figure 6.7.

Figure 6.6 The schematic of analysis paths (a) and quenching residual stresses profile in the T-section component along the (b) $y$ and (c) $z$ coordinate direction.
The expected trends are seen in Figure 6.6 and Figure 6.7. Residual stresses of over 0.6 times the uniaxial yield stress have been developed due to quenching. As shown in Figure 6.6(c) and Figure 6.7, the components of residual stress post quenching in the transversal and through thickness directions, \( \sigma_{xx} \) and \( \sigma_{yy} \) respectively, are compressive at the surface of the T-section component. At 30 mm from the surface, they become tensile and become approximately constant at 200 mm from the surface with peak values of 118 MPa and 78 MPa, respectively. The longitudinal direction stress, \( \sigma_{zz} \) is however tensile throughout and attains a peak value of 210 MPa at the centre and diminishes to zero at the surface, as necessary. Neglecting edge effects, (200 mm from the sample surface) the longitudinal stress is relatively uniform along the length of the component.

Through the thickness \((y)\) direction, Figure 6.6(a) shows that \( \sigma_{xx} \) and \( \sigma_{zz} \) are compressive at the top and bottom surfaces and are tensile towards the centre, while the \( \sigma_{yy} \) stress remains tensile and tends to zero at both ends, as required. In addition, it can be seen from Figure 6.7, that the stresses are relatively symmetric about the \( y-z \) plane (Symmetry Plane I in Figure 6.1(a)) which implies the fact that Point 1 enters the water before Point 5 has limited effect on the overall residual stress distribution of this T-section component. This justifies the use of a half/quarter geometry model in the cold compression/rolling model in the following FE analysis, thus reducing computational time.

**6.2.4 Residual stress reduction by cold compression**

The effectiveness of the cold compression residual stress reduction technique has been evaluated by comparing the stress distributions in the T-section component before and after cold compression. To quantify the level of residual stress reduction, the percentage stress reduction at the point of peak residual stresses prior to compression have been calculated. The peak reduction in both tensile and
compressive residual stresses have been evaluated to assess the influence of the cold compression on residual stress relaxation.

The residual stress distribution in the large-sized T-section component may be influenced by many factors during forging operation, such as compression ratio, friction coefficient, the length of overlap and its longitudinal length.

| Table 6.1 Variables employed in the parametric study. |
|----------------|----------------|----------------|----------------|
|                | Longitudinal Length, $L$ (m) | Compression Ratio (%) | Friction Coefficient | Overlap Length (%$L$) |
| 1              | 1                | 0.5/1.5/3         | 0.05             | 0                  |
| 2              | 1                | 1.5              | 0.05/0.1/0.2     | 0                  |
| 3              | 0.4/1/2.5/5      | 1.5              | 0.05             | 0                  |
| 4              | 1                | 1.5              | 0.05             | 0/20/30/40         |
| 5              | 1                | 1.5              | 0.05             | 20/30/40           |

(Compressed by different dies)

6.2.4.1 Compression Ratio Effects

Contour plots of the residual stress distribution after 1.5 % and 3 % compression are presented in Figure 6.8 and Figure 6.9, respectively, assuming a friction coefficient of 0.05 (lubricated condition) [73]. Compared to the post-quench residual stress distribution shown in Figure 6.7, it can be seen from Figure 6.8 that after 1.5 % compression, the residual stresses experience a marked decrease in the three coordinate directions, with the percentage peak stress reduction in the range of 88.4 – 96.5 %. Clearly, with both compression ratios, the residual stresses have been markedly reduced. The results also show that there is no advantage in applying 3 %
compression to the cold forging for reducing residual stresses in T-section component compared to 1.5 % compression ratio. In Figure 6.9, residual stress levels for 3 % compression show a slight increase over those for 1.5 %.

Figure 6.8 Residual stresses distribution after 1.5 % compression

Figure 6.9 Residual stresses distribution after 3 % compression

Figure 6.10 indicates the residual stress profiles along the path AA' (see Figure 6.3), where 0 % represents the profiles before compression. It also shows that the stress relaxation trend is more prominent at low compression ratios, i.e. at 0.5 % and 1 %. While for higher compression ratios (>1.5 %), a further decrease in residual stress with increasing compression ratio becomes less remarkable. Therefore the residual stresses in the T-section component can be effectively removed with the compression ratio of around 1.5 % and friction coefficient of 0.05.
6.2.4.2 Friction coefficient effects

The level of friction between the die and the component can also affect the resultant residual stress distribution post-compression. As discussed above, Figure 6.8 shows the residual stress distribution of the component with a lubricated surface and subject to 1.5 % compression. In comparison, a contour plot of the residual stresses employing a friction coefficient of 0.15 between the component and both die, which represents semi-dry lubrication conditions [35], is presented in Figure 6.11. At this friction coefficient, all residual stress components have been reduced post-compression, but less than those obtained using a friction coefficient of 0.05. Comparing Figure 6.8 and Figure 6.11, it can be deduced that high levels of friction can lead to less residual
stresses reduction. Therefore, a low friction coefficient is preferred. In practice, lubrication is applied between the die and the component.

Figure 6.11 Residual stresses distribution after compression (μ=0.15).

Figure 6.12 shows the residual stress distribution after compression along the path AA' for various friction coefficients. The importance of lubrication to reduce residual stresses by cold forging is evident from this figure. An increase in friction coefficient leads to progressively higher magnitude residual stresses, as expected.
Figure 6.12 Influence of friction coefficient on the residual stress profiles along path AA', through the thickness of plate and rib.

6.2.4.3 Component Length Effects

For an extra-large component, longitudinal length is an essential factor on residual stresses. Four longitudinal lengths, namely 400, 1000, 2500 and 5000 mm were simulated to investigate its effect. Table 6.2, Table 6.3, Table 6.4 and Table 6.5 state the value and percentage reduction of residual stresses, at the point in the T-section component identified with the peak stress value prior to compression, for the longitudinal lengths, 400 mm, 1000 mm, 2500 mm and 5000 mm, respectively, which are the extreme values considered in the current study. The stress distribution in a component with 5 m long post-compression is shown in
Figure 6.13. From these tables, it can be seen that increasing longitudinal length of the T-section component has no clear influence on residual stresses in the $\sigma_{xx}$ and $\sigma_{yy}$ directions after quenching. The magnitudes of different cases only differ by a few MPa. However, it is expected that if the size is reduced to a certain level, the residual stress will start to decrease [2]. The phenomena may result from the fact that there is no marked difference in temperature distribution of T-section component for the lengths examined. As a consequence, $\sigma_{xx}$ and $\sigma_{yy}$ residual stresses also have no obvious distinction. The maximum $\sigma_{zz}$ tensile residual stress magnitude of the component only has a slight increase with the development of component length. This can be attributed to the fact that increasing length leads to a larger temperature gradient in the z direction. A similar conclusion can be drawn for the trends of the residual stress of T-section components after cold compression.

Table 6.2 Reduction in the peak residual stresses of quenched T-section block (400 mm length) after cold compression.

<table>
<thead>
<tr>
<th>Maximum residual stress (MPa)</th>
<th>Post quench</th>
<th>Post compression</th>
<th>Percentage stress reduction (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{xx}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tensile</td>
<td>171.4</td>
<td>14.7</td>
<td>91.4</td>
</tr>
<tr>
<td>Compressive</td>
<td>-252.6</td>
<td>-24.2</td>
<td>90.4</td>
</tr>
<tr>
<td>$\sigma_{yy}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tensile</td>
<td>77</td>
<td>9.5</td>
<td>87.7</td>
</tr>
<tr>
<td>Compressive</td>
<td>-195.3</td>
<td>-5.7</td>
<td>97.1</td>
</tr>
<tr>
<td>$\sigma_{zz}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tensile</td>
<td>181.8</td>
<td>13.2</td>
<td>92.7</td>
</tr>
<tr>
<td>Compressive</td>
<td>-201.1</td>
<td>-16.5</td>
<td>91.7</td>
</tr>
</tbody>
</table>
Table 6.3 Percentage peak stress reduction in the T-section component (1000 mm length) after lubricated cold compression without overlap ($\mu = 0.05$).

<table>
<thead>
<tr>
<th>Maximum residual stress (MPa)</th>
<th>Post quench</th>
<th>1.5% compression</th>
<th>Percentage stress reduction (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{xx}$</td>
<td>Tensile</td>
<td>170.3</td>
<td>11.6</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-258.4</td>
<td>-29.9</td>
</tr>
<tr>
<td>$\sigma_{yy}$</td>
<td>Tensile</td>
<td>95.4</td>
<td>3.6</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-196.3</td>
<td>-15.1</td>
</tr>
<tr>
<td>$\sigma_{zz}$</td>
<td>Tensile</td>
<td>190.8</td>
<td>16.2</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-217.2</td>
<td>-12.1</td>
</tr>
</tbody>
</table>

Table 6.4 Percentage peak stress reduction in the T-section component (1500 mm length) after lubricated cold compression without overlap ($\mu = 0.05$).

<table>
<thead>
<tr>
<th>Maximum residual stress (MPa)</th>
<th>After quench</th>
<th>After compression</th>
<th>Stress reduction proportion (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{xx}$</td>
<td>Tensile</td>
<td>170.4</td>
<td>16.1</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-257.3</td>
<td>-42.4</td>
</tr>
<tr>
<td>$\sigma_{yy}$</td>
<td>Tensile</td>
<td>96.9</td>
<td>24.5</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-193.1</td>
<td>-17.5</td>
</tr>
<tr>
<td>$\sigma_{zz}$</td>
<td>Tensile</td>
<td>195.5</td>
<td>59.2</td>
</tr>
<tr>
<td></td>
<td>Compressive</td>
<td>-182.6</td>
<td>-19.5</td>
</tr>
</tbody>
</table>
Figure 6.13 Residual stresses distribution after compression ($L = 5000$ mm).

Table 6.5 Residual stresses of quenched T-section block (5000 mm length) after cold compression.

<table>
<thead>
<tr>
<th>Maximum residual stress (MPa)</th>
<th>Post quench</th>
<th>Post compression</th>
<th>Percentage stress reduction (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{xx}$ Tensile</td>
<td>174.1</td>
<td>15.5</td>
<td>91.1</td>
</tr>
<tr>
<td>Compressive</td>
<td>-239.2</td>
<td>47.8</td>
<td>80.0</td>
</tr>
<tr>
<td>$\sigma_{yy}$ Tensile</td>
<td>114.1</td>
<td>20.5</td>
<td>82.0</td>
</tr>
<tr>
<td>Compressive</td>
<td>-227.6</td>
<td>-16.8</td>
<td>92.6</td>
</tr>
<tr>
<td>$\sigma_{zz}$ Tensile</td>
<td>208.3</td>
<td>64.5</td>
<td>69.1</td>
</tr>
<tr>
<td>Compressive</td>
<td>-242.7</td>
<td>-26.9</td>
<td>88.9</td>
</tr>
</tbody>
</table>
6.2.4.4 Effect of overlap length

During the multiple cold compression process, the existence of overlap compression regions is inevitable and the overlap length between two consecutive forging operations also influences the residual stresses distribution of the component. Contour plots of the residual stress distribution with overlap lengths of 20 \%L, 30 \%L and 40 \%L are shown in Figure 6.14(a) - (c), respectively. The position of the end of the die in contact with the component during compression can be clearly identified in Figure 6.14, as, for example, the two blue lines on the surface of the plate for the \(\sigma_{xx}\) stress contour plot etc. These blue lines indicate a region of intensified compressive stress, due to the stress concentration feature effects of the corner of the die. Comparing Figure 6.8 and Figure 6.14, shows that, compared with the component compressed without overlap, the compression with overlap not only leads to the growing area of undesired stress fluctuation in the three coordinate directions but also increases the magnitude of residual stresses in all directions. The maximum \(\sigma_{xx}\) tensile residual stress after compression with overlap increases markedly from 15 MPa to about 76 MPa, while the maximum compressive stress changes from -85 MPa to -121 MPa. The \(\sigma_{yy}\) stress component shows similar tendencies. There is not much influence of multistep cold compression on the tensile \(\sigma_{zz}\) stress, but the compressive \(\sigma_{zz}\) stress changes from -37.1 to around -124 MPa. However, as shown in Figure 6.14, variations in overlap length of 20 \%L, 30 \%L and 40 \%L, have little effect on the overall values of the residual stresses.

In reality, some industries, e.g. AVIC Ltd, applies the same set of dies for the hot forging T-section component and the post-quench cold multiple compression processes. The schematic of both the dies and component is slightly different from that in Figure 6.3 which can be seen in Figure 6.15. Apart from the influence of multiple compression, it can be found that during the cold compression, the deformation ratio between the rib region and the plate region must be different as the thickness of both regions is various. Although the residual stress magnitude is relaxed to some extent, the combined effect of non-uniform plastic deformation during compression and the multiple compression makes the stress distribution of the T-section component complicated, as shown in Figure 6.16. \((H_{rib} - H_{plate} = 40 \text{ mm})\)
Figure 6.14 Effect of overlap length on residual stress distribution, (a) 20 %L (b) 30 %L (c) 40 %L.
Figure 6.15 Schematic of the die and T-section component in industrial case.

Figure 6.16 Effect of overlap length on predicted residual stress distribution of T-section after compression via dies in Figure 6.15, (a) 20 %L (b) 30 %L (c) 40 %L.
Compared with the stress magnitude in Figure 6.14, the maximum tensile stresses magnitude of $\sigma_{xx}$, $\sigma_{yy}$ and $\sigma_{zz}$ are all beyond 150 MPa and the maximum value approaches 200 MPa. Since research shows around 40 % of residual stresses magnitude could be relaxed during over-aging [47, 49], it can be concluded that the residual stresses pattern in a T-section forging in Figure 6.16, is one of the sources to cause part distortion. Although the magnitude of residual stresses is less than that of yield stress, if the superposition of the tensile residual stresses exceeds the yield strength of the material, the part will be locally yielded and distortion or crack could be probably caused.

### 6.2.5 Numerical examples of FEM compression simulation

In order to verify the correctness of the FE modelling mentioned above, two numerical instances from a published paper were repeated via the same method of building above FE models. Using the published material properties data to verify the effectiveness of the simulation method is a useful pattern, which has been widely applied in numerous research spheres, especially when the validation experiment is not practical on a laboratory scale.

In this section, the availability of residual stress prediction of quenching operation and stress relaxation of T-section component through the cold compression was assessed by repeating two FE modelling from a reference. In Abaqus FE simulation done by Koc.et.al [35], residual stress magnitude and distribution of quenched 7075 aluminium block were predicted and then cold compression was used to relieve residual stresses. The experimental results about residual stress measured by neutron diffraction have good agreement with the FE simulation results.
Figure 6.17 Comparison of simulation residual stresses of quenched Block A along x-axis with results [35]. a) Block A geometry; b) x-component; c) y-component; and d) z-component;

Figure 6.17 shows the comparison between residual stresses distribution along the x-axis of block A of author’s simulation results and that of reference [35]. And Figure 6.18 indicates the comparison between current stresses distribution of Block A along y-axis and results of reference [35]. Figure 6.19(a) illustrates the symmetric plane of the quenched model B and the analysis paths that residual stresses were measured. For quenching models, Figure 6.19 and Figure 6.20 reveal the results of author’s simulations have good agreement with publication data [35]. Residual stresses magnitudes and distribution are also consistent with the experimental curves of reference [35].
Figure 6.18 Comparison of simulation residual stresses of quenched Block A along y-axis with results [35]. a) x-component; b) y-component; and c) z-component.

In addition, for comparing the residual stresses distribution of as-quenched author’s model after cold compression with related results for reference [35], Block B was used to probe the validation of FE model built by the author, as shown in Figure 6.19(a). It can be seen from Figure 6.20 and Figure 6.21 that under different conditions the contrast between residual stresses of author’s simulations and corresponding publication data is shown.
Figure 6.19 Comparison of simulation residual stresses of quenched B Block with results [35]. a) Block B geometry; b) along x-axis; c) along y-axis;

Figure 6.20 Comparison of simulation residual stresses on Block B along x-axis with results [35] (66 °C water quenched, 1 % cold compression and 2 % cold compression). a) x-component; b) y-component; c) z-component;
Figure 6.21 Comparison of simulation residual stresses on Block B along y-axis with results [35] (66 °C water quenched, 1 % cold compression and 2 % cold compression).

Figure 6.17-21 indicate that the author’s simulation results show a high similarity with related results for reference [35]. Additionally, they can also indirectly prove that the FE models of quenching built by the author in this chapter can be used in predicting the residual stresses distribution of T-section component during the quenching and cold compression periods.

According to these figures shown above and related analysis on residual stresses magnitude and distribution, it can be concluded that the accuracy of FE model can be guaranteed and consequently the corresponding results are reliable.
6.3 Prediction of residual stresses relief with cold rolling process for T-section panels

6.3.1 Model of the residual stress relieving process via cold rolling

For the cold rolling process, the rolls were assumed to be analytical rigid bodies. As illustrated in Figure 6.22, the bottom roll is stepped to accommodate the T-section geometry. The final state of the quenched T-section model is used as the initial state for the cold rolling model. To study the residual stress distribution in the quenched and cold rolled T-section component, two analysis paths were examined. The AA’ line is located at the mid-width and mid-length (rib part) of the component. The BB’ line is in the same XY plane (with AA’ line). As shown in Figure 6.22, at XY plane the position of BB’ line is 200 mm away from one side of the component.

Given the FE result in Section 6.2.4, in Figure 6.22, the groove depth of bottom roll is dependent on the required rolling deformation ratio so as to make the rib part and plate part have the same roll deformation ratio. Corresponding to the plate region of T-section component, the radius and width of the bottom roll are identical to that of the top roll. For the cold rolling process, the friction coefficient between rolls and T-section component was set as 0.1 [95]. For the angular velocity, the same parameter for rolling scale-down T-section specimens (1 rad/s) was used. Since the plastic deformation is relatively small, the effect of heat generated by plastic deformation could be negligible.

Figure 6.22 Schematic of the rolls and the paths AA’ and BB’ for residual stress analysis. (Points A and B are located on the top surface of component, while point A’ and B’ are on the bottom surface.)
6.3.2 Rolling Deformation Ratio Effects for large-sized T-section component

Contour plots of the residual stress distribution after 1.5 % and 3 % rolling via rolls with 400 mm radii are presented in Figure 6.23 and Figure 6.24, respectively. Compared to the post-quench residual stress distribution shown in Figure 6.7 and Figure 6.23, it can be seen that after 1.5 % rolling deformation ratio, the residual stresses in the core part of material experience a marked decrease in the three coordinate directions, with relatively low residual stress up to 26 % of the $\sigma_{\text{yield}}(CR 1.5\%)$ (320 MPa). Clearly, with both rolling deformation ratios, the residual stresses in the core part of the material have been markedly reduced. The results in Figure 6.24 also show that there is no advantage in applying less than 1.5 % or over 3 % rolling deformation ratio to the cold rolling for reducing residual stresses in T-section component. In fact, for 1.5 % rolling deformation ratio case, peak tensile $\sigma_{zz}$ residual stress of up to 72 % of $\sigma_{\text{yield}}(CR 1.5\%)$ was found at the rib region of the bottom surface, a relatively low residual stress (no more than 90 MPa) exists at the rest material. Residual stress around the rib region for 3 % deformation ratio shows an increase over those for 1.5 %. In addition, it can be found in Figure 6.23-24 that the $\sigma_{zz}$ tensile residual stress at the rib region of component experiences an increase. The maximum stress magnitude increases from 235 MPa to 298 MPa.
Figure 6.23 Residual stresses distribution after with 1.5 % deformation ratio. a) $\sigma_{xx}$ b) $\sigma_{yy}$ c) $\sigma_{zz}$ (Roll radius 400 mm)
Figure 6.24 Residual stresses distribution after with 3 % deformation ratio. a) $\sigma_{xx}$  b) $\sigma_{yy}$  c) $\sigma_{zz}$ (Roll radius 400 mm)
Figure 6.25 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (AA') of the T-section component after rolling to different deformation ratios.

Figure 6.25 indicates the residual stress profiles along the path AA' (see Figure 6.22), where 0 % represents the profiles before rolling. It can be seen the stress condition in T-section component is more complicated than that in a block with paralleled surfaces. It can be found that similarly with the residual stress distribution in quenched and cold rolled block, for the rib region of T-section component (Figure 6.26), $\sigma_{zz}$ tensile residual stress appears on the both surfaces of the rib region of the component, replacing the original large compressive stress. For $\sigma_{xx}$ and $\sigma_{yy}$ stress components, it can be seen in Figure 6.25, the increase of rolling deformation ratio has limited effect on these two stress components. As shown in Figure 6.26, it reveals that the contact area and velocity of the rib region differ between its corresponding top surface and bottom surfaces as the groove region of the bottom roll has a smaller radius than that of top roll. In Figure 6.26, the sizes of both rolls are exaggerated to show the difference of both contact areas between component and two rolls. As illustrated in Figure 6.25, along with rolling direction ($\sigma_{zz}$ stress component), the bottom surface of the rib region
has large tensile residual stress while the stress magnitude of the another side is much smaller. With the increase of rolling deformation ratio, as shown in Figure 6.25(c), the magnitude of tensile stress around the both surfaces increase with the rolling deformation ratio.

Figure 6.26 The schematic of the cross section of the rib region of T-section component and during cold rolling

Figure 6.27 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (BB') of the T-section component after rolling to different deformation ratios.
Figure 6.27 indicates the residual stress profiles along the path BB'. In contrast, despite the effect from rib region, for the plate region of T-section panel, there is no difference between both top and bottom rolls, thus the stress distribution is relatively symmetric around the middle line near $Y= 40$ mm (The related thickness post rolling is ~ 80 mm.). For $\sigma_{yy}$ stress component, the residual stress magnitudes under various deformation ratio conditions are too small to be shown in Figure 6.27(b). For the sample rolled to 1.5% rolling deformation ratio, thermal induced residual stresses in the material have significantly decreased.

### 6.3.3 Roll Radius Effects

The size of contact area between rolls and component which has a relationship with roll radius can also affect the resultant residual stress distribution post-rolling. As discussed above, Figure 6.23 shows the residual stress distribution of the component rolled by a set of rolls with radius 400 mm. In comparison, a contour plot of the residual stresses employing a set of rolls with radius 600 mm, is presented in Figure 6.28. With 600 mm radius rolls, all residual stress components in the core part of material have been further reduced.

![Figure 6.28 Residual stresses distribution after 1.5 % rolling via rolls with 600 mm radii.](image)

a) $\sigma_{xx}$ (MPa)  

b) $\sigma_{yy}$ (MPa)  

c) $\sigma_{zz}$ (MPa)
Figure 6.29 shows the residual stress distribution after rolling along the path AA' for various roll radii. The influence of roll radius on relaxing residual stresses by cold rolling is limited for $\sigma_{xx}$ and $\sigma_{yy}$ stress components. A decrease of roll radius could lead to larger $\sigma_{zz}$ tensile residual stress magnitude at the core part of T-section component, reaching 33% of $\sigma_{yield(1.5%) \text{ when the radius is } 200 \text{ mm}}$. In addition, it should be noted that although the tensile stress magnitude in the core part of the material decreases with the increase of rolls' radii, the tensile stress gradient from surface to core part of the component is decreased. When the roll radius comes to 600 mm, the core region of low residual stress (< 65 MPa) exists in the area between the top surface and within 15 mm below the bottom surface. Hence, although a set of huge rolls can make the residual stress magnitude in the core part of the material further reduced to some extent, generally the region of large residual stress (>100 MPa) can be expanded due to a relatively low-stress gradient close to the bottom surface.

Figure 6.29 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (AA') of the T-section component after rolling with different roll radii, 1.5 rolling deformation ratio.
Figure 6.30 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (BB') of the T-section component after rolling with different roll radii, 1.5 rolling deformation ratio.

Figure 6.30 illustrates the residual stress distribution after rolling along the path BB' for various roll radii. For the plate region of T-section component, an increase of roll radius could lead to a relief of $\sigma_{zz}$ and $\sigma_{xx}$ tensile surface stress in the core part of the material. At the material core part, the original $\sigma_{zz}$ tensile stress magnitude even turns into compressive residual stress when the radius is 600 mm. There is no clear roll radius effect on $\sigma_{yy}$ stress component. The $\sigma_{yy}$ residual stress magnitudes are too small to be illustrated in Figure 6.30(b).

6.4 Prediction of surface machining on residual stress evolution in quenched and cold rolled components.

According to the FE and experimental results of residual stresses distribution in quenched and cold rolled samples mentioned above, the tensile surface residual stresses in a material are always the potential sources to lead to the distortion or crack
of the final components after aging and the subsequent machining. Hence, this section focuses on predicting the effect of surface machining on residual stresses evolution in quenched and cold rolled blocks and T section components and the possible part distortion.

A mechanical analysis was performed for the quenching process, as any heat generation from plasticity during machining is considered negligible. The final state of the cold rolled T-profile component in the FE model was used as the initial state for the machining model. The material model mentioned in Chapter 3 was used.

For the extra-large T-section component, during the machining process, the surface material of the rib part (5 mm thickness, one layer elements) was removed firstly, then the material of the top surface (5 mm thickness) shown in Figure 6.31 was machined. The pattern of machining extra-large T-section component is illustrated in Figure 6.31.

Figure 6.31 The pattern of the surface machining a large-sized T-section component
Figure 6.32 The residual stress distribution in cold rolled large-size T-profile component.

As shown in Figure 6.32, for extra-large T-section component, the residual stress $\sigma_{zz}$ along the longitudinal direction has a significant decrease, lowering to less than 118 MPa. No remarkable distortion was found, as shown in Figure 6.32. In addition, Figure 6.33 indicates that the residual stresses distribution and magnitude in the core part of the T-profile component were not affected by the surface machining and this hints that it is highly possible to manufacture a large size aviation component without part distortion.
Figure 6.33 (a) $\sigma_{xx}$, (b) $\sigma_{yy}$ and (c) $\sigma_{zz}$ residual stress profiles in the rib region (along AA' line in Figure 6.22) of the cold rolled T-section component (1.5% rolling deformation ratio) before and after machining.

In practice, the work condition of machining is more complicated than this simplified modelling [118]. Some other factors, e.g. cutting speed, cutting edge radius, coolant type, coolant pressure, etc, can also influence the final shape of a component and the residual stresses distribution in the material. In addition, given the mechanical requirement of aviation components, the aging treatment before the final machining is indispensable. Hence, for the residual stresses distribution in the final components, further investigations are needed.

**6.5 Discussion**

The numerical results about cold compression have revealed that although compression with one operation may effectively minimize quenching–induced residual stresses in T-section component, the non-uniform plastic deformation of the overlap compression region caused by multiple cold compression can make the stress distribution of component complicated, increasing the residual stress magnitude. Furthermore, this case also illustrates that for components with a complicated profile of cross-section, e.g. T-section, the deformation ratio varies from region to region during cold compression contributes the growth of residual stresses in materials.

In addition, the sufficient large residual stresses that exist in quenched and multiple compressed T-profile components showed by FE analysis could probably cause the part distortion during machining. The large tensile residual stresses up to 200 MPa appear in the overlap region between different strikes which is a potential source of part distortion during the subsequent machining process or service life.

Compared with the multiple cold compression technique, for residual stresses relaxation in extra-large components with a non-rectangular cross section, e.g. T-section, there are two advantages to adopt the cold rolling technique:

1) It can make the extra-large component avoid the overlap regions’ effect caused by multiple cold compressions. Compared with the complicated residual stress distribution in Figure 6.16, the residual stress distributions in cold rolled T-section components are more consistent (See Figure 6.23).

2) It has been found that the residual stresses in the core part of material can always be effectively relaxed regardless of how small the roll radius is if a sufficient rolling deformation ratio is adopted. This means it is practical to roll
large-sized aviation component via a relatively small rolling machine so as to save lots of factory space. Compared with the compression dies for cold compression with/without overlap in Figure 2.14, the fabrication of the rolls for cold rolling technique needs much less material.

In addition, although tensile residual stress exists at both surfaces of cold rolled T-section panels, the depth of regions that tensile stress exists is quite shallow (approx. 5 mm) when rolling deformation ratio is 1.5% and roll radius is 400 mm. Given the following:

1) The subsequent artificial aging can further relieve the residual stresses in the material.
2) 60% – 70% material will be removed during final machining.
3) Low residual stresses exist in the core part of the material.
4) The FE results predict the stresses distribution in material is not affected by the surface machining process and no significant distortion is observed.

It can be deduced that for cold rolling the possibility of the occurrence of unacceptable part distortion of the final component is expected to be lower than that of component processed by the multiple cold compression technique.

**6.6 Summary**

To study cold working techniques, e.g. cold compression and cold rolling, for relaxing thermal-induced residual stresses in extra-large T-section component, two FE models were developed. Although these predictions cannot be directly verified by measuring the stress distribution in large-sized T-section component, the residual stress predictions are reliable as two numerical cases about cold compression in one publication were repeated to validate the accuracy and efficiency of this compression modelling method. Additionally, the rolling model for scale-down T-section samples has been experimentally validated.

Generally, it is reasonable to apply cold rolling to mitigate residual stresses in large-sized T-section component as, except for the surface region (0-6 mm to the both surfaces) the residual stresses magnitude in most region of component can be relieved via 1.5 % - 3 % rolling if a set of rolls of appropriate size (R > 400 mm) is employed. By contrast, the non-uniform plastic deformation due to multiple cold compression could make contribution to the complexion of the stress distribution in material.
Chapter 7 Conclusions and suggestions for future work

7.1 Conclusions

Residual stress relief in large-sized heat treated aluminium alloy via cold rolling technique is a promising technique to better fulfil low residual stress requirement, low cost and good mechanical properties in aviation applications. A case-study of a manufacturing process for Al Alloy samples with low residual stresses, has been achieved via cold rolling. This method has been proposed and experimentally verified in this study.

To ensure an effective manufacturing process for forming a component with optimised performance, the fundamental material models describing the deformation behaviour of 7050 aluminium alloy under SHT treatment condition, as well as the response of a quenched material in a cold forming situation, have been improved.

To ensure the process control of the residual stress relief process of forming extra-large T-section panel with functional performance, comprehensive understanding numerical models have been built, on the ground of experimental investigations and mechanism studies. Further conclusions associated with the main aspects of the research are summarised in the following subsections.

7.1.1 Thermo-mechanical properties of AA 7050 under the manufacturing processes

To investigate the material behaviour during the manufacturing process, Isothermal Gleeble tests were performed to study the thermo-mechanical properties of the AA 7050 Al alloy. For analysis of the test results, the below conclusions have been obtained:

- For the AA 7050 Al alloy, pronounced viscoplastic properties at elevated temperatures have been observed: the stress level increases with increasing strain rate and decreasing temperature;
• The strain rate dependence of flow stress (at true strain rate levels of 0.01, 0.1, and 1) approximates to a power law. The temperature dependence of flow stress follows the Arrhenius activation energy equation for Aluminium Alloy. Although the fit for the AA7050 specimens at different strain rates has a small deviation, it can still provide acceptable prediction of the tensile behaviour.

To determine the influence of quenching on residual stresses evolution in material, the heat transfer coefficient for predicting the heat transfer of component during quenching process has been calculated and adopted to describe the deformation behaviour of the AA7050 material during the quenching process. Further conclusions are listed as below:

• The effects of temperature, temperature-dependent thermal conductivity, density, specific heat on the heat transfer between AA 7050 sample and water are taken into account.
• The stress evolution during the quenching period can be accurately modelled.
• The predicted temperature vs time curve from one dimensional FE model show good agreement with the experimental results from the thermocouples.

7.1.2 Feasibility of the cold rolling technique to relieve residual stress in AA 7050 material

The cold rolling technique can successfully mitigate the residual stress in the core part of aluminium block to an acceptable level. Neutron diffraction, X-ray diffraction and contour techniques were performed to study the cold rolling effect on stresses distribution in a material. Based on experimental and numerical results, the following conclusions have been drawn:

• For heat treated AA7050 Aluminium blocks, large compressive residual stresses (>200 MPa) exist close to the material surfaces while the tensile residual stresses in the core part of material. After cold rolling process, the residual stresses magnitude in the core part of component decreased to a low level (less than 140 MPa), meanwhile tensile surface residual stresses (>200 MPa) appear around the both materials surfaces contacted with the rolls. However, the depth of the region with tensile stresses is shallow (around 2 mm).
• The surface tensile stress magnitude increases with the growing deformation ratio and while the increase of deformation ratio also leads to the stress magnitude in the core part of material to be further reduced.
The increase of the roll radius can further relieve the residual stress magnitude in the core part of material to some extent but have limited effect on the residual stresses magnitude around the material surfaces.

7.1.3 The influence of cold working stress relief techniques on heat treated AA 7050 T-profile aviation components

As it is impractical to form extra-large T-section component in lab level, thus the scale-down T-section components were adopted. Numerical analysis and X-ray diffraction stress measurement method were conducted to study the residual stresses evolution in heat-treated T-section components after cold rolling. Modelling of multiple cold compression and cold rolling quenched extra-large T-section components were built. The influence of various work parameters of cold compression/rolling on residual stresses in the material was investigated, and the following conclusions have been drawn:

- If cold compression without overlap and the bottom die like Figure 6.3 were adopted, the residual stresses in T-profile panel can be greatly reduced (no less than 67 %), for different tensile/compressive stress components. Under this work condition, the optimal compression ratio for reducing residual stress in the T-section component is around 1.5% under lubricated condition. Cold compression with deformation ratio above 3% does not provide any more benefit and can induce surface tensile stress in the rib area.
- Lubrication has strong effect on stress reduction by cold forging process. A high level of friction between work-piece and dies effectively limits the success of compression and leads to relatively large $\sigma_{xx}$ and $\sigma_{zz}$ tensile residual stress near/at the surface.
- Overlapping between multiple cold forging operations significantly affects the residual stress distribution. It increases the final residual stress by several times compared with the component compressed without overlap. The application of bottom die in Figure 6.15 could aggregate the phenomena of non-uniform plastic deformation.
- For extra-large T-section components, considering the economic factors and FE predictions, the optimised work condition is at 1.5 - 3 % rolling deformation ratio and a set of rolls with 400 mm roll radius.
- Compared with the residual stress distribution caused by multiple-compression, the residual stress distributions in cold rolled T-section components are more
consistent. Low residual stresses (no more than 120 MPa) in the core part of T-section component.

- Although the influence of aging process is not considered, the FE analysis of surface machining on cold rolled T-section components predicts that the surface machining has limited effect on the residual stresses distribution in the core part of the material. This result can provide a guideline for the future industrial production

### 7.2 Suggestions for future work

Although cold rolling technique owns several advantages over the current multiple cold compression technique, the tensile surface residual stress could be the source to lead to the premature failure of the material. Hence, to further mitigate the surface residual stress magnitude, the subsequent over-aging treatment (See Figure 5.1) could be adopted.

Experiments, e.g. neutron diffraction, x-ray diffraction and contour method, and FE analysis of the aging treatment need to be carried out to quantify how much residual stress in scale-down T-section panels can be relieved during over-aging processes. Considered the fact that low residual stresses (only tens of MPa) exist in material made by creep age forming, thus making the component keep its geometry during aging process via clamping tool (constrained aging) may help relieve the large surface residual stresses of material. A set of clamping tool should be designed. The current material models could be extended to include the material response during the aging process so as to predict the stress evolution during age process. In other words, based on a variety of fundamental tensile and compressive tests at different aging temperatures and initial stress conditions, a set of comprehensive constitutive equations should be built.

Moreover, the stress distribution in the material during the final machining process should also be investigated. During this stage, the thin regions existing large tensile residual stresses will be machined, the residual stress distribution and evolution in the final product during the machining process requires further study. Like the aging process, to accurately describe the material behaviour of 7050 aluminium alloy during the machining process, the corresponding material model should be studied as well. In addition, the corresponding FE model of the machining process should be built and experiments of residual stresses measurement need to be conducted to determine the residual stress magnitude and distribution in the final product. Furthermore, during the
machining stage, since the regions close to the material surfaces existing tensile residual stresses will be machined, thus how to remove these regions via milling machine and meanwhile keep the residual stress magnitude in the rest material at a low level (no more than 100 MPa) requires further study.

Given the cost, it will be more economical for manufacturing a large-sized component with constant cross section, e.g. T-section, if a set of rolls can be adopted in hot forming the component before SHT treatment. I.e. roll-forging experiments should be carried out to form T-section component. Roll-forging means to fabricate large-sized rectangular blocks into T-section components via rolls under hot temperature (over 400 °C). Since the cost will be higher with the increase of roll radius, thus the influence of work parameters, e.g. roll radius on forming T-section components should be studied in detail. In addition, the temperature of rolls and components during the test should also be investigated as they also have an effect on the formation of the component and the yield strength of material not only depends on the strain rate of deformation but the temperature.

Some related fundamental experiments should be conducted. To better describe the material behaviour under hot roll-forging conditions, a related material model should be considered to be built. In addition, the interfacial heat transfer coefficient between rolls and components should be figured out. Based on the information, the numerical model about roll-forging could be carried out with the implantation of the interfacial HTC value and the material model via user defined subroutines.
References


A.1 The determination of convective heat transfer coefficient (C-HTC) between test material and water

To determine the heat transfer coefficient for predicting the heat transfer of component during quenching process, the measurement of temperature is required. Quite a few of methods have been conducted to measure the interfacial temperature between component and quenchant and consequently achieved heat flux so as to calculate the C-HTC. One method for calculating C-HTC is to run a plenty of simulations using various sets of temperature dependent C-HTC. When one FE model result about ‘cooling curve’ (plotting the temperature and time data as x-y coordinates and drawing a curve through the data points) under a set of C-HTC is similar to that measured in experiments, the set of temperature-dependent C-HTC could be considered as the surface heat transfer coefficient between specimen and coolant [119, 120]. In Tanner et al [120]’s study, a 1.6 mm diameter thermocouple was inserted at the centre of one face of a block to measure the temperature evolution during quenching. An empirical relationship between C-HTC, time and Delta T ($\Delta T = T - T_{sat}$), where T the surface temperature of material and $T_{sat}$ the temperature of the quenchant adjacent to the surface is achieved via an inverse method to combine the measured and predicted temperatures.

According to Newton laws of cooling, to determine the C-HTC, the surface temperature of material during quenching is needed. However, it is very hard to measure the surface temperature of the material and the temperature of adjacent water directly and accurately, especially for stirring water, because during quenching the water adjacent the component flows vividly. If a thermocouple is welded at the surface of material, the temperature measured by thermocouple would be significantly affected by the flowing water, and it is difficult for a thermocouple that embedded near the material surface, e.g. 0.5 mm, to get an accurate material surface temperature.

Based on an assumption that if a specimen is large enough, during quenching the heat in the core part of the material only flows outwards along a path with the smallest distance to a surface, researchers try to calculate the surface temperature of one position of material via measuring the inner temperatures of material where the related positions at the corresponding smallest heat transfer path. In J. zhang et al’ s research [121], for their rectangular specimen, two thermocouples were embedded at two
positions along the shortest heat transfer path with different distance to the centre of a quenching surface. Via the Fourier’s heat transfer equation, the approximate value of surface temperature was achieved and then the value was substituted into Newton equation to get temperature-dependent C-HTC. In Wang et al [122]’s study on stress distribution in 6061 Aluminium alloy, they used a similar method to predict C-HTC. M. Bamberger et al [123] used a similar method based on Fourier’s heat transfer equation. In their study, four thermocouples were placed along the same heat transfer path (thickness direction) at depths of 10, 20, 30 and 75 mm below the centre of one surface. Then, an implicit finite difference method was applied to calculate the surface temperature and further get the C-HTC of the test material. However, in their studies, the thermal parameters that used in their equations, such as thermal conductivity, material density and specific heat capacity, all assumed as a constant value. Hence, to adopt temperature-dependent material properties in the current methods and to build the following modelling about heat treatment process, an efficient closed-form method was conducted in this study to determine the C-HTC between 7050 aluminium alloy and water.

A. 1.1 Convective Heat Transfer Model

As demonstrated by Tanner et. al. [120], the temperature of Aluminium alloy during quenching mainly depends on the heat transfer between material surfaces. The influences of both the quenching induced strain & volume changes and the precipitate formation on the 7000 series alloys are negligible. During quenching, the residual stresses can be developed and its magnitudes are largely dependent on the rate of convective heat transfer between the surface of the material and the cooling media (water). Thus, the thermal gradient in material due to quenching become the main source to the development of elastic and plastic strains from which residual stress can be predicted. Note that the convective heat transfer coefficient is dependent on many parameters, e.g. physical properties [124], in this study experimental measurements have been performed to determine values for the material considered in this work. Due to the limited material, for the convective heat transfer coefficient study, the used sample size is 62 × 62 × 21.2 mm³. The experimental data and calculated results are ultimate to be used in thermal-stress models to predict the thermally induced strain and residual stress of 7050 Aluminium alloy during quenching.

For determining the C-HTC, the first step of this method is to check whether the assumption mentioned above could be met or not. A simplified FE model via ABAQUS
commercial software [107] was applied to predict the temperature distribution of the test sample during quenching.

Since the quenched block is symmetric, a half FE model which size is $62.0 \times 31.0 \times 21.2$ mm$^3$ as shown in Figure A. 0.1, was adopted to study the temperature distribution in quenched block during the rapid cooling process. The heat transfer coefficient between the sample and water was from literature [35]. The initial temperature of the block is 485 °C and the element type is 8 node heat transfer elements DC3D8. The mesh size, 1 mm cube element, was employed. The predicted temperature distribution in quenched sample after fully immersing into water 4 s, is shown in Figure A. 0.1(a). Although it can be seen from the Figure A. 0.1(a) and (b) that the temperature varies from centre to the surface, in the Figure A. 0.1(a) near the centre line (the dashed line), the temperature distribution can be considered as a one dimension heat transfer process as the heat in the core part did not flow along any other directions to edges. Here Region A can be defined as a domain within a 14 mm diameter circle from the central line of the block.

![Figure A. 0.1 a) Predicted temperature field of half specimen model after fully immersing into water 4 s and the location of Region A b) the temperature distributions](image-url)
Simulated temperature variations for block bottom surface (at 0 mm of the analysis path) during the quenching process

The temperature distribution at selected distances from the surface in the block at 4 s is illustrated in Figure A. 0.1(a). It can be seen in Figure A. 0.1(b) and (c) that inside the Region A, the temperature profile is basically horizontal, which predicts the heat flow is purely axial in this area. Therefore, the heat transfer process at 4 s of the quenching period inside the boundary of Region A can be simplified as one-dimensional.

Figure A. 0.1(c) shows the temperature distribution on the bottom surface of the block at various moments of quenching process. The predicted surface temperature profiles of the block at moments, 0 s, 2 s, 4 s, 16 s and 32 s shown in Figure A. 0.1(c), shows the calculated surface temperature lowered dramatically from 485 °C to around 35 °C in 32 s. It means the heat transfer phenomenon within the Region A during the quenching process (32 s) can be simplified as one-dimensional heat convection problem.

The above simulation results can prove that the heat transfer phenomenon of the core part of material can be considered as one-dimensional along the heat transfer path (the dashed central line in Figure A. 0.1(a) during a 32 s quenching period. Hence, at high temperatures during quenching, it can be assumed the cooling at the centre of the 62 mm × 62 mm surfaces would be bidirectional.

**A.1.2 The 1D heat flux model and application**

As shown in Figure A. 0.2, a rectangular sample of dimension 62 × 62 × 21.2 mm³ was thermocoupled to determine the transient temperature distribution during quenching. Two holes with a diameter of 1.1 mm were drilled at the positions shown in the Figure A. 0.2 to a depth of 31 mm so that two ‘K’ type thermocouples of 1 mm diameter could be inserted. The block was uniformly heated to 485 °C and then quenched into a bath of agitated water at approximately 20 °C. Temperature measurements were obtained at a rate of 10 Hz via a Novus data acquisition system. The block was lowered into the water so that the 62 × 62 mm² face close to Thermocouple 2 (as shown in Figure A. 0.2) entered the water first. To further ensure that the experiments meet the one-dimensional heat flow analysis assumption, high temperature-cement, which effectively prevents the heat loss of the surfaces of the block (with a conductivity of 0.29 W/(m K)), was applied to all four 62 mm × 21.2 mm faces.
Figure A. 0.2 The sample geometry and thermocouple locations for convective heat transfer coefficient measurements

As one dimensional heat flow is assumed, the heat flux flows from the inner material with relatively high temperature to the material's surface close of lower temperature. Using the finite-difference technique, for each measurement point it can be written as:

\[
\frac{\Delta T_y^i}{\Delta y} = \frac{T_{y_{i+1}}^t - T_{y_{i-1}}^t}{\Delta y} \quad \text{or} \quad \frac{T_{y_{i+1}}^t - T_{y_i}^t}{\Delta y}
\]  
(A.1)

\[
\frac{\Delta T_y^i}{\Delta t} = \frac{T_{y_{i+1}}^t - T_{y_{i-1}}^t - T_{y_i}^{t+\Delta t} - T_{y_i}^{t-\Delta t}}{\Delta t} \quad \text{or} \quad \frac{T_{y_i}^{t+\Delta t} - T_{y_i}^t}{\Delta t}
\]  
(A.2)

where \( T_{y_i}^t, T_{y_{i-1}}^t \) and \( T_{y_{i+1}}^t \) the temperatures along the heat transfer path at different positions \( y_i, y_{i-1} \) and \( y_{i+1} \) at time \( t \), the subscript \( i \) \((i = 1, 2, \ldots, N)\) represents the spatial increment, \( T_{y_i}^t, T_{y_i}^{t-\Delta t} \) and \( T_{y_i}^{t+\Delta t} \) the temperatures of a measurement point at time \( t, t - \Delta t \) and \( t + \Delta t \).

It is well-known that the heat conduction equation, a partial differential equation, illustrates the temperature variation in a given region over time. According to the first law of thermodynamics and the Fourier model, for unsteady-state heat convection, a function \( T(x, y, z \text{ and } t) \) of three spatial parameters \( x, y \text{ and } z \) and the time variable \( t \), the equation can be written as [125, 126]

\[
\rho \alpha c_p \frac{\partial T^t}{\partial t} = k_{tc} \left( \frac{\partial^2 T^t}{\partial x^2} \right) + k_{tc} \left( \frac{\partial^2 T^t}{\partial y^2} \right) + k_{tc} \left( \frac{\partial^2 T^t}{\partial z^2} \right) + q_v
\]  
(A.3)
where \( k_{tc} \) is the thermal conductivity, \( \rho_d \) is the physical density and \( c_\rho \) is the specific heat, all of which are temperature-dependent. Temperature-dependent values of \( c_\rho, k_{tc} \) and \( \rho_d \) for the material have been taken from literature [35] for 7xxx series aluminum alloys. \( q_v \) is the latent heat of phase transformation. Since the aim of quenching 7050 aluminum alloy is to constrain the generation of Zn-Mg precipitates, here the \( q_v \) value can be set to 0.

As described in Section A.1.1, this case can be considered as one dimension heat transfer condition, thus the Equation (A.3) can be simplified into [119, 127]

\[
\frac{\partial T^t_y}{\partial t} = \alpha_d \left( \frac{\partial^2 T^t_y}{\partial^2 y} \right)
\]

(A.4)

Where \( \alpha_d = \frac{k_{tc}}{\rho_d c_\rho} \), is the thermal diffusivity.

Substituting Equation (A.1) and Equation (A.2) into Equation (A.4), it can be written as [126, 128].

\[
\frac{\Delta T^t_{y_i}}{\Delta t} = \frac{T^t_{y_i} - T^t_{y_i-\Delta t}}{\Delta t} = \alpha_d \left[ \frac{\partial^2 T^t_{y_i}}{\partial^2 y} \right]_{i-\frac{1}{2}} - \alpha_d \left[ \frac{\partial^2 T^t_{y_i}}{\partial^2 y} \right]_{i+\frac{1}{2}}
\]

(A.5)

\[
= \alpha_d \left( \frac{T^t_{y_i-1} + T^t_{y_i+1} - 2T^t_{y_i}}{(\Delta y)^2} \right)
\]

neglecting the truncation error terms, Equation (A.4) can be written

\[
T^t_{y_{i-1}} = \frac{(\Delta y)^2}{2\alpha_d \Delta t} T^t_{y_i-\Delta t} - T^t_{y_{i+1}} + \left( \frac{(\Delta y)^2}{\alpha_d \Delta t} + 2 \right) T^t_{y_i}
\]

(A.6)

Or

\[
T^t_{y_i} = \left( \frac{1}{\Delta t} T^t_{y_{i-1}} + \frac{\alpha_d}{(\Delta y)^2} T^t_{y_{i+1}} + \frac{\alpha}{(\Delta y)^2} T^t_{y_{i-1}} \right) \frac{1}{\Delta t} + \frac{2\alpha_d}{(\Delta y)^2}
\]

(A.7)

Therefore, if the temperature-time curves from two thermocouples could be attained, the temperature evolution of other positions along the same heat transfer path (Figure A. 0.3) can be calculated via Equation (A.6) and Equation (A.7). The detailed calculation procedure is shown in Appendix A2. Hence, at given time, the temperature at a point closest to the surface and its neighboring position can be calculated and used to deduce the heat flux per unit across the interface between the work-piece and water.

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\[ q'_c = q'_h \implies \frac{k_{tc}(T^t_{y_2} - T^t_{y_1})}{y_2 - y_1} = h(T^t_{surface} - T_{\infty}) \] (A.8)

Here \( T^t_{y_1} \) and \( T^t_{y_2} \) the temperatures of two adjacent positions. Among them, \( T^t_{y_1} \) represents the temperature of one position \((y_1)\) which is closest to the surface (along with the shortest distance direction) at time \( t \), \( T^t_{y_2} \) is the temperature of the position \( y_2 \) next to \( y_1 \) towards the core of block. At the surface of the component, the heat flux per unit \( q'_c \) to the surface from conduction must equal that removed from the surface by convection [122]. Therefore, the heat flux per unit \( q'_h \) at the component surface can then be calculated by the Equation (A.8). \( h \) is the convective heat transfer coefficient between Aluminum block and quenchant, \( T^t_{surface} \) is the surface temperature at a time \( t \), and \( T_{\infty} \) is the temperature of quenchant. During quenching, since the pool size is far larger than the material size and water is agitated, the initial surface temperature of material is not high enough for the film boiling regime to occur [120]. Research also proved that when quenching steels that the film boiling stage is scattered or nonexistent. If the vapor blanket does appear, it is generally unstable and easy to changes in material surface. Therefore the temperature of the quenchant could be assumed constant. The temperature-dependent value of \( h \) can be deduced using Equations (A.1)–(A.8).

![Figure A.0.3 Simplified 1D numerical model to theoretically predict heat flux.](image)

In this work, a time increment \( \Delta t \) of 0.1 s was employed. The temperature measurements at the thermocouple locations i.e. at \( y = 1 \) mm, at \( y = 10.6 \) mm, were used to calculate the temperature at other places along the heat transfer path at a
given time. Initially, a uniform work-piece temperature was set as 483 °C which is assumed as the transient temperature of material before the component makes contact with the water. In order to get the more accurate surface temperature, the distance between the position of \( y_1 \) and the surface should be no more than 0.01 mm. A Matlab program was written to calculate the heat convection value.

![Convective Heat Transfer Coefficient](image)

Figure A. 0.4 Convective Heat Transfer Coefficient \( h \) vs surface temperature for water quenching of a 7050 Al alloy block.

### A.1.3 FE Validation

A one dimensional thermal FE model using the commercial FE software ABAQUS [107] was built to verify the numerical 1D model in order to realize one-dimensional heat flow as illustrated in Figure A. 0.4. The model included 53 elements which size is 0.1 mm × 0.2 mm. The temperature dependent convective heat transfer coefficient determined from the analyses described above, as shown in Figure A. 0.5.

![Temperature vs Time](image)
Figure A. 0.5 Comparison of the predicted and measured temperature profiles during quenching a block at a) 10.6 mm and b) 1 mm from the surface of the block.

Good agreement was found between the cooling curves simulated and measured by thermocouples located 1 mm and 10.6 mm from the sample’s surface, as shown in Figure A. 0.5, confirming that the values of $h$ determined are appropriate. Hence these temperature dependent $h$ values were used in the quenching and cold rolling FE models.

The calculation procedure of the heat transfer coefficient versus temperature between AA7050 aluminium alloy and water is shown as Figure A. 0.6.
APPENDIX B

Rolls Design

The assembly of a set of rolls for rolling the scale-down size T-section component have been illustrated in Figure B.1. The roll specification can be found in Table B.1. In Figure B.1, the mould (component number 7) can be replaced by other moulds with different outer faces to meet the product requirement.

![Figure B.1 Rolls for the scale-down size T-shaped component](image)

<table>
<thead>
<tr>
<th>Table B.1 The roll specification</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material type</td>
</tr>
<tr>
<td>Hardness</td>
</tr>
<tr>
<td>Young’s modulus</td>
</tr>
<tr>
<td>Tensile Strength</td>
</tr>
<tr>
<td>Thermal conductivity</td>
</tr>
</tbody>
</table>
The influence of cold rolling on as-quenched scaled down T-section component will be validated by using this scaled down rolls due to the limited laboratory condition and the current rolling machine configuration as illustrated in Figure B.1. The main components are listed in Table B.2.

Table B.2 Scale-down size of rolls details

<table>
<thead>
<tr>
<th>No.</th>
<th>Description</th>
<th>Material</th>
<th>Quantity</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Flange</td>
<td>EN24T</td>
<td>4</td>
</tr>
<tr>
<td>2</td>
<td>Left Junction ring</td>
<td>EN24T</td>
<td>4</td>
</tr>
<tr>
<td>3</td>
<td>Right Junction ring</td>
<td>EN24T</td>
<td>4</td>
</tr>
<tr>
<td>4</td>
<td>M4 × 10 socket Screw</td>
<td>Mild Steel</td>
<td>44</td>
</tr>
<tr>
<td>5</td>
<td>Locating Plate</td>
<td>Mild Steel</td>
<td>4</td>
</tr>
<tr>
<td>6</td>
<td>Bottom Roll</td>
<td>EN24T</td>
<td>1</td>
</tr>
<tr>
<td>7</td>
<td>Mould</td>
<td>EN24T</td>
<td>2</td>
</tr>
<tr>
<td>8</td>
<td>Ring</td>
<td>EN24T</td>
<td>4</td>
</tr>
<tr>
<td>9</td>
<td>M6 × 25 socket Screw</td>
<td>Mild Steel</td>
<td>12</td>
</tr>
<tr>
<td>10</td>
<td>Roller Ring</td>
<td>EN24T</td>
<td>4</td>
</tr>
<tr>
<td>11</td>
<td>M8 × 30 socket Screw</td>
<td>Mild Steel</td>
<td>16</td>
</tr>
<tr>
<td>12</td>
<td>Top Roll</td>
<td>EN24T</td>
<td>1</td>
</tr>
</tbody>
</table>