THERMAL MECHANICAL ANALYSIS OF WHEEL DEFORMATION INDUCED FROM QUENCHING

by

CHRISTINA MICHELLE ESTEY

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Christina Michelle Estey
Name of Author (please print)

October 7, 2004
Date (dd/mm/yyyy)

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Department of Materials Engineering
The University of British Columbia
Vancouver, BC Canada
Abstract

The focus of this present investigation was to develop a mathematical model capable of predicting the evolution of temperature, formation of thermal strains and stresses and resulting displacements within a die cast aluminum wheel during the quenching stage of the T6 heat treatment process. The development of this model will aid in understanding the mechanisms associated with wheel deformation and the factors that influence wheel deformation. A sequentially coupled finite element based thermal-mechanical model was developed using the commercial finite element software ABAQUS. The model consisted of a heat transfer model to predict the evolution of temperature and a mechanical model to predict the formation of strains and stresses within a die-cast aluminum wheel during quenching.

The thermal model was validated against industrial temperature measurements acquired at Canadian Autoparts Toyota Inc. using embedded thermocouples throughout the wheel. The parameters describing the thermal boundary conditions were systematically adjusted until an acceptable agreement was obtained with the measured data.

The temperature-time predictions within a 180° section of the wheel obtained from the thermal model were used as input for the mechanical model. The mechanical analysis revealed that the quenching process induces a high state of compressive residual stresses on the surface and a high state of residual tensile stresses in the interior of the wheel. The mechanical model was validated against industrial wheel deformation results obtained at Canadian Autoparts Toyota Inc, which proved to be in good qualitative agreement at some locations and in good quantitative agreement in others. This model revealed that temperature differences occurring in a circumferential direction around the wheel play a more significant role in influencing the degree of wheel deformation as compared to higher quenching rates. Therefore, to minimize the amount of wheel deformation induced from quenching, a reduction in the thermal gradients around the wheel would be required, in which a better design of the quenching system should be implemented.
Overall, the stress and displacement predictions obtained from the thermal-mechanical analysis revealed that this finite element based mathematical model can be used as a powerful tool to predict overall wheel deformation. Based on an understanding of the mechanism associated with wheel deformation, the quench conditions can be optimized to reduce wheel deformation while meeting the industry standards for strength and fatigue performance.
# Table of Contents

Abstract ........................................................................................................................ ii
Table of Contents .............................................................................................................. iv
List of Tables .................................................................................................................. vi
List of Figures ................................................................................................................ vii
Acknowledgments ........................................................................................................... x

## CHAPTER 1 INTRODUCTION

1.1 Wheel Manufacturing Process .................................................................................. 3
1.2 Wheel Deformation ................................................................................................. 4

## CHAPTER 2 LITERATURE REVIEW

2.1 The Heat Treatment Process for Al-Si-Mg Alloys .................................................... 7
   2.1.1 Solution Heat Treatment .................................................................................. 7
   2.1.2 Quenching ...................................................................................................... 8
   2.1.3 Artificial Aging .............................................................................................. 10
2.2 Dimensional Changes during heat treatment ............................................................ 11
   2.2.1 Distortion during Solution Heat Treatment .................................................... 11
   2.2.2 Distortion during Quenching ......................................................................... 12
   2.2.3 Distortion during Aging ............................................................................... 13
2.3 Model Development ............................................................................................... 14
   2.3.1 Thermal Boundary Conditions - Boiling Heat Transfer ................................... 15
   2.3.2 Constitutive Behavior of A356 ...................................................................... 21

## CHAPTER 3 SCOPE & OBJECTIVES

3.1 Scope of the Research Project .................................................................................. 27
3.2 Objectives of the Research Project .......................................................................... 28

## CHAPTER 4 EXPERIMENTAL MEASUREMENTS

4.1 Industrial Measurements ....................................................................................... 29
   4.1.1 Temperature Measurements ......................................................................... 29
   4.1.2 Deformation Analysis ................................................................................... 33
4.2 Laboratory Measurements ..................................................................................... 40
   4.2.1 Heat Transfer Coefficient Measurements .................................................... 40
   4.2.2 Constitutive Behavior Tests ......................................................................... 43

## CHAPTER 5 THERMAL MODEL

5.1 General Thermal Model Formulation ..................................................................... 61
5.2 Thermal Physical Properties .................................................................................. 61
5.3 36° Thermal Model ............................................................................................... 62
   5.3.1 Geometry ..................................................................................................... 63
   5.3.2 Initial Conditions .......................................................................................... 63
   5.3.3 Boundary Conditions ................................................................................... 63
List of Tables

Table 4.1: Location Designations of the CMM Measurements ............................................. 34

Table 4.2: Radial Deformation Values in Ascending Order in Terms of Wheel Location in the Tray ................................................................. 36

Table 4.3: Vertical Deformation Values in Ascending Order in Terms of Wheel Location in the Tray ................................................................. 37

Table 5.1: Thermal Physical Properties of A356 used in the Thermal Model .................. 62

Table 5.2: List of Model Parameters used for the Three Orientations. Bath Temperature 70°C and Submersion Rate 0.233 m/s ................................................................. 65
List of Figures

Figure 1.1: 36-Wheel T6 Tray Assembly Schematic .................................................. 6

Figure 2.1: Precipitation Hardening Characteristics of an Aluminum Alloy (From Source [14]) ................................................................. 24

Figure 2.2: Cooling Curve of a Metal Part Quenched in Water (from source [21]) ...... 24

Figure 2.3: Characteristic Boiling Curve (From Source [26]) .................................. 25

Figure 2.4: Heat transfer coefficient as a function of surface temperature .......... 25

Figure 2.5: Flow curves for Al-Mg-Si alloy at a true strain rate of 1.3 x 10^{-3} s^{-1}. (From Source [38]) ................................................................. 26

Figure 4.1: Datapaq 11 Data Logger ........................................................................ 47

Figure 4.2: Datapaq Thermal Barrier Unit ................................................................. 47

Figure 4.3: Photograph of the T6 Heat Treating Tray .............................................. 47

Figure 4.4: Thermocouple Locations Embedded within the Wheel ....................... 48

Figure 4.5: Temperature Measurement Locations to Determine Influence of Wheel Orientation ................................................................. 48

Figure 4.6: Thermal History of the Wheel during Quenching for Orientation #1 ...... 49

Figure 4.7: Thermal History of the Wheel during Quenching for Orientation #2 ...... 49

Figure 4.8: Thermal History of the Wheel during Quenching for Orientation #3 ...... 50

Figure 4.9: Location and Positioning of the Measured Wheels Top View (With Spokes Facing out of the Page) ............................................. 51

Figure 4.10: CMM Locations for Measurements ...................................................... 52

Figure 4.11: Schematic Representation of Possible Miscalculation of Circle Centre .... 52

Figure 4.12: Angular Locations of Spokes and Support Flanges ......................... 53

Figure 4.13: Percent Change in Radius as a Function of Angle around the Wheel .... 53

Figure 4.14: Percent Change in Radius (Wheels #1 - #4: Level A-Corners) ............ 54
Figure 5.12: Thermal Profiles for Orientation #2 .................................................. 80
Figure 5.13: Thermal Profiles for Orientation #3 .................................................. 81
Figure 6.1: 180° Section Wheel Mesh ................................................................. 95
Figure 6.2: Temperature Distribution at initial stages of the Quench
(Time = 1.292 seconds) .................................................................................... 96
Figure 6.3: Axial Stress Distribution at initial stages of the Quench
(Time = 1.292 seconds) .................................................................................... 96
Figure 6.4: Axial Stress Distribution at the end of the Quench ..................... 97
Figure 6.5: Equivalent Plastic Strain Distribution at Initial Stages of the Quench
(Time = 1.292 seconds) .................................................................................... 97
Figure 6.6: Equivalent Plastic Strain Distribution at End of Quench .......... 98
Figure 6.7: Von Mises Stress Distribution at end of Quench ....................... 98
Figure 6.8: Top View of Wheel Deformation (Displacements Exaggerated by a
Factor of 20) ..................................................................................................... 99
Figure 6.9: Bottom View of Wheel Deformation (Displacements Exaggerated by a
Factor of 20) ..................................................................................................... 99
Figure 6.10: Side View of Wheel Deformation (Displacements Exaggerated by a
Factor of 20) ..................................................................................................... 100
Figure 6.11: Enhanced Side View of Wheel Deformation (Displacements Exaggerated
by a Factor of 20) ............................................................................................ 100
Figure 6.12: Comparison of the Predicted and the Measured Change in Radius .... 101
Figure 6.13: Angular Location of Thermal Measurements and the 180° Model Plane
of Symmetry ....................................................................................................... 101
Figure 6.14: Comparison of the Change in Radius for the two Mechanical Analyses... 101
Figure 6.15: Von Mises Stress Distribution at End of Quench (Uniform HTC) ...... 102
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1 INTRODUCTION

The wheel is probably the most important invention of all time and the very first step toward man-made transportation. It is believed that the first form of wheeled transportation originated in Mesopotamia (part of modern day Iraq) as a two-wheeled chariot as early as 3500 BC [1]. The chariot, pulled by humans or animals, increased the speed of travel and made the transportation of people and goods much easier. It is believed that the first wheel was solid and made of a cross section of a tree [1]. Later modifications of the wheel pinned three wooden planks together for increased strength, bored with an axle hole through the centre. The evolution of the wheel was furthered with the advent of the spoke in an attempt to lighten the wheel. The wooden-spoked-wheel was crude, but was lighter and stronger than the solid wheel, consisting of a hub (with an axle hole), rim and spokes. Later, more attempts were made to further reduce the weight of the wheel by narrowing the width of the spokes and the rim. Significant advances in wheel technology were made in the 1920’s with the development of the disc wheel, in which the rim was rolled out of a straight strip of steel and the disc was stamped from sheet metal and then the two components were welded or riveted together [1]. This created a wheel that was relatively light weight, stiff, resistant to damage and easily produced in mass quantities. Further advances in metallurgy have lead to the development of extremely lightweight, strong spoked wheels. Even today, there are continual improvements in designing wheels to reduce its weight, while improving its overall strength and physical properties.

Currently, there are two major trends emerging from the wheel manufacturing industry; the first is an increased use of aluminum for wheels and the second is an increase in the demand for larger diameter wheels. Although aluminum offers several benefits over conventional steel in terms of weight savings and more efficient heat dissipation, the primary reason for increased demand of die cast aluminum wheels is that they are more aesthetically pleasing. The wheel casting process offers stylists more freedom and flexibility in designing a variety of stylish shapes as compared to the conventional steel stamping processing route. Therefore the use of aluminum die-casting as a manufacturing process is rapidly replacing stamped and welded steel wheels. A market analysis, performed by JD Powers Associates in 1997
reported that 40%, 35% and 23% of all passenger cars produced in Japan, US and Europe, respectively, were fitted with aluminum wheels as a standard feature and that for each subsequent year, additional increases of 3% could be expected [2].

The automotive industry is also faced with challenges of increased demand for larger diameter wheels. While this larger diameter wheel trend is also driven by aesthetics and by the fact that people like the appearance of highly styled wheels with low profile tires, there is considerably more important performance benefits associated with larger diameter wheels. Owing to the lightweight properties of aluminum as compared to steel, wider rims and larger diameter wheels can be used without sacrificing the overall weight of the wheel. The wider flange section allows for the use of wider tires, which improves the overall handling and performance of the vehicle while maintaining the original gearing of the vehicle.

In addition, recent trends in the automotive industry are also demanding more powerful engines requiring more powerful and bigger braking systems which, in turn, demand larger diameter wheels [3]. Larger diameter wheels allow for a braking system to be fitted with larger diameter disc brake rotors, improving the braking system power and efficiency. Furthermore, larger diameter wheels improve brake cooling by providing a larger area for air passage around the brakes.

There are challenging aspects that must be overcome in order to meet the demands of the cast aluminum wheel market. One of the more challenging problems is that associated with wheel deformation and distortion. Wheel deformation is inherent to wheel production and is often more prominent in larger diameter wheels. To address this issue, balancing weights are used to counter wheel distortion as well as to balance the tire and wheel assembly to reduce vibration that may lead to wear problems or ride disturbances. In spite of this increased use of balancing weights associated with larger diameter wheels, one goal of wheel manufacturers is to minimize the use of these balancing weights and to replace commonly used lead weights with steel as a lighter alloy. To overcome this challenging problem, the wheel manufacturing industry has focused on minimizing the amount of wheel deformation inherent to wheel production.
In order to combat wheel deformation in larger diameter wheels, a collaborative effort between Canadian Autoparts Toyota Inc., a manufacturer of die cast aluminum wheels, and the University of British Columbia is underway in order to gain a better understanding of the fundamental aspects associated with wheel distortion and deformation. Understanding the mechanisms associated with wheel deformation is very important from an industrial point of view for two main reasons: 1) to reduce reliance on balancing weights which are required to compensate for distortion in the wheel and 2) to reduce the number of rejected wheels associated with 'goose-skin' or off-roundness problems. With regard to the first reason, if the wheel is not machined true, balancing issues arise requiring weights with a maximum weight allowance of 35 to 40 grams [4]. In regard to the second reason, goose-skin is a problem associated with excess deformation and occurs in the final machining operation when some areas do not become machined due to excess distortion, while other areas exceed material removal tolerances. For the two reasons stated above, wheel manufacturers are highly motivated to identify the mechanisms driving wheel deformation and to develop counter measures to control them.

In an attempt to assess wheel deformation, this project will focus on the development of a finite element model to simulate the T6 quench stage of a 17-inch aluminum die cast wheel produced for the Toyota Tundra. This T6 quench stage of the manufacturing process has been identified by the industrial sponsor as the largest contributor to overall wheel deformation. However, to gain a better insight into the issues relating to wheel deformation, the wheel manufacturing process and the mechanisms of wheel deformation will be discussed in the following sections.

1.1 Wheel Manufacturing Process

The die cast aluminum wheel manufacturing process currently used at Canadian Autoparts Toyota, Inc. (CAPTIN) in Delta, B.C. consists of seven major operations: casting, rough machining, solutionizing, quenching, aging, final machining and finishing.

There are two types of casting processes employed at CAPTIN; low-pressure die casting and Toyota Differential Pressure die casting [4]. In both processes, molten aluminum is forced
Chapter 1: Introduction

up through the sprue-tubes from the melt furnace into the mould cavity. For the low-pressure
die casting process, the aluminum is injected into the die cavity by pressurizing the chamber
containing the melt at pressures less than 170 kPa [5]. Whereas, the Toyota Differential
Pressure die casting process draws the molten metal into the die cavity by applying a vacuum
within the mould itself. Following solidification, the wheel is ejected from the die and water
quenched. The wheels are then rough machined to remove excess metal and flashing
consequential of the casting process. The wheels are then individually loaded into a 36-
wheel tray arrangement, as displayed in Figure 1.1, and transported to the T6 heat treating
operation. The T6 heat treating tray is then placed into the solution treating furnace, in which
the tray advances approximately 2 meters every 15 minutes for a total of 4 hours at 540°C.
The tray is then advanced to the pre-quench stage, held in that position for no longer than 15
seconds and lowered into the quench tank. The temperature of quench tank water is
maintained at 70°C and agitation is initiated approximately 3 seconds after full tray
immersion. The tray is submerged in the water for approximately 2½ minutes before it is
removed to air dry for 2 minutes. Following the quench, the tray proceeds through the aging
furnace for 4 hours at 140°C after which the tray is allowed to air cool and each wheel is
unloaded individually from the tray. The wheels are then subjected to a final machining and
finishing operation to obtain the desired final appearance of the wheel.

Unfortunately, rejection rates of aluminum die cast wheels are relatively high as compared to
other aluminum castings [6] due to a very high degree of geometric complexity and the fact
that aluminum die cast wheels have some of the most stringent requirements for product
quality of automotive castings. The types of defects most common to the production of
wheels include porosity associated with shrinkage, poor fatigue or impact performance and
excess ovality (off roundness). The problem being addressed in this present investigation is
that related to excess ovality or wheel deformation, in which rejection rates range from 1 –
2% of wheel production for this particular 17-inch diameter die cast wheel.

1.2 Wheel Deformation

Investigations carried out at CAPTIN identified the most significant contribution to overall
wheel deformation to occur during the quenching stage of the T6 heat treatment operation.
The problem of wheel deformation or excess ovality has a direct correlation with the quench rate during the quenching operation of the T6 heat treatment. Faster quench rates produce large thermal gradients that induce large thermal stresses and, if sufficient, can cause inelastic yielding leading to differential strains in the component (or residual stresses). These residual stresses typically remain in the component after the age hardening treatment, as these temperatures are insufficient to allow stress relaxation. In the case of wheels, these residual stresses are a mixed blessing as on the one hand they can improve fatigue performance (the surface of the wheel is typically in compression) while on the other, the wheel can become significantly distorted. If the magnitude of the distortion is above manufacturing tolerances, the wheel is rejected, thus decreasing the overall efficiency and the economics of the manufacturing process. Unfortunately, the solution is not simply a matter of reducing the quench rate. Slower quench rates lead to heterogeneous precipitation of large Mg2Si particles, which results in poor fatigue performance of the wheel. In addition, these slower cooling rates result in a loss of surface compressive stress, which also reduces the fatigue performance. Therefore, a compromise in the cooling rate must be made in order to minimize the distortional effects while maximizing fatigue performance.
Figure 1.1: 36-Wheel T6 Tray Assembly Schematic
2 LITERATURE REVIEW

2.1 The Heat Treatment Process for Al-Si-Mg Alloys

There has been an increasing trend to use Al-Si-Mg casting alloys for die cast wheel production, specifically A356 (Al-7Si-0.3Mg). Additions of silicon (Si) to pure aluminum produce a material with high fluidity, good feeding characteristics, low shrinkage, good hot cracking resistance and improved wear resistance due to hard Si particles [7]. The addition of magnesium (Mg) produces a heat treatable alloy in which significant strengthening can be achieved through precipitation hardening with the appropriate heat treatment process. An important characteristic of a precipitation-hardened alloy system is a temperature-dependent equilibrium solid solubility, in which the solubility increases with increasing temperature [8, 9]. When the alloy is held at temperatures above the element’s solubility limit for a sufficient period of time required for complete diffusion, the element will dissolve, leading to a solid solution state. At temperatures below the solubility limit for Mg-Si, a precipitation reaction occurs, in which finely dispersed precipitates are formed. These precipitates act as barriers to dislocations, which resist deformation when a component is subjected to stress, and result in an increase in the strength of the alloy.

In order to take advantage of the solubility characteristics of A356 and to increase the alloy’s strength, cast components are typically heat treated using a T6 heat treatment process. This T6 heat treatment operation is a three-step process consisting of a solution heat treatment, a water quench and an ageing heat treatment. These three stages of the heat treatment process are described below.

2.1.1 Solution Heat Treatment

The purpose of the solution heat treatment is to dissolve any Mg_2Si particles that may have formed during solidification and subsequent cooling, to homogenize the casting and to change the morphology of the eutectic silicon [9]. In order to facilitate the dissolution of Mg and Si into solution, the alloy must be held at a temperature above the solubility limit for an extended period of time [8].
The solutionizing temperature plays an important role in influencing the final microstructural and mechanical properties of the alloy. The solutionizing temperature should be sufficiently high for increased solubility and diffusion rates to ensure a maximum concentration of magnesium and silicon in solid solution, but below the temperature at which high temperature grain growth, coalescence of dispersoid particles and melting of eutectics can occur, as the latter can cause a reduction in mechanical properties due to local melting at grain boundaries [9, 10]. The solubility of magnesium and silicon in the Al-rich phase decreases as the solutionizing temperature is decreased. Several sources [9, 11] state that the typical solutionizing temperature for the A356 alloy is 540±5°C, at which approximately 0.6% Mg and 0.9% Si can be dissolved into solution. Wang and Davidson [12] suggest, however, that the solubility limit of Mg is 0.5 wt% at a temperature of 540°C. Since these values reported for the Mg solubility are greater then the concentration of Mg in A356 (0.3% Mg), it should be fully in solid solution after the solution heat treatment process. This does not hold for Si, which is present in amounts in excess of the solubility limit.

The solutionizing time also plays an important role in determining the alloy’s properties and is dependent on the microstructure, the section thickness and the furnace loading. Typical times can vary from less than one minute to as long as twenty hours [7]. Apelian et al. [9] suggested that dissolution of the hardening elements (Mg and Si) and the homogenization of the casting are essentially complete in approximately 30 to 45 minutes. However, longer solution times (from 6 to 16 hours) are required for the Si eutectic particles to undergo changes in morphology – e.g. spheroidization and coarsening [9]. During this time, the sharp edges of silicon-based eutectic phases are rounded, reducing internal stresses and stress risers enhancing ductility [13]. The solutionizing conditions used at Canadian Autoparts Toyota Inc. for A356 die cast aluminum wheels consists of holding the alloy at 540±5°C for approximately 4 hours [4].

2.1.2 Quenching

Immediately following the solution heat treatment, the component is rapidly cooled, in a water quench. The purpose of a rapid quench is to inhibit diffusion or formation of undesirable phases, resulting in a non-equilibrium state where the solid solution is
supersaturated with second phase atoms that possess a low degree of solubility at room temperature [9, 14]. This supersaturated solid solution is critical for the subsequent precipitation-hardening stage.

During the quench, the cooling rates must be sufficiently high to limit the time spent in the temperature regime in which precipitation kinetics are high. This will avoid precipitation of large Mg-Si particles, which can be detrimental to mechanical properties or to corrosion resistance [8]. To avoid appreciable precipitation during cooling, several conditions must be fulfilled. The first is that the time required to transfer the wheel from the solution treatment furnace to the quench tank (termed the quench delay) must be minimized in order to prevent slow cooling in the temperature range in which the precipitation kinetics are very high [8]. Rogers and Koepke [13] reported that quench delay should be less than 15 seconds for high strength aluminum alloys for automotive components. Apelian et al. [9] on the other hand, reported that a quench delay should be no longer than 30 seconds to ensure sub-microscopic and uniform dispersion of Mg$_2$Si particles during aging. However they also stated that if the quench can be performed within 10 seconds, then there is greater potential for uniform dispersion of the precipitate phase during aging and thus optimum properties can be obtained.

The second requirement for preventing appreciable precipitation during quenching is that the quench media must have sufficient volume, heat absorption capacity and rate of flow to enable rapid cooling [8, 9]. In addition, it is necessary that the quench water flow past all surfaces of the part during the first few seconds after immersion to ensure quenching effectiveness [8]. During this critical time, sufficient agitation is required to prevent local increases in temperature due to the formation of steam pockets [8]. Furthermore, there should be no disruptions in the quench, which could result in reheating of the material into a temperature range where precipitation kinetics are rapid [8]. Reports indicate that poor implementation of the quench operation could lead to parts with high residual stress, non-uniform properties, low corrosion resistance, distortion and soft spots, leading to low strength and untimely failure [15].
2.1.3 Artificial Aging

After the quench, the supersaturated solid solution is held at an intermediate temperature (approximately 140°C) for a specified period of time until the desired mechanical properties are obtained. The purpose of this age hardening heat treatment is to form uniformly dispersed precipitates within the crystal and at grain boundaries from the constituents that were dissolved during solutionizing [9, 14]. Significant strengthening is associated with the formation of this dispersion of fine precipitates.

The precipitation sequence of the Al-Si-Mg system from the supersaturated solid solution state (SSS) is as follows [7, 16]:

\[
\text{SSS} \rightarrow \text{GP-I}_{\text{spherical}} \rightarrow \text{GP-II}(\beta'')_{\text{Needle}} \rightarrow \beta'_{\text{Rod}} \rightarrow \beta(\text{Mg}_2\text{Si})_{\text{Platelet}}
\]

During the initial stages of the precipitation process, two types of Guinier-Preston (GP) zones are formed. The first type are called GP-I zones or pre-clusters, which are coherent and spherical with no internal order [7]. Edwards et al. [16] have proposed that during the GP-I stage of the precipitation sequence, an initial clustering sequence occurs, in which independent clusters of Si and Mg atoms form first, followed by the dissolution of Mg clusters, and finally the formation of Mg/Si co-clusters, which transform into small precipitates of unknown structure. As aging proceeds, the GP-II zones or \(\beta''\) are formed as the GP-I zones become even more ordered and change to an acicular or needle shape [7, 16]. This \(\beta''\) stage is associated with peak-aged condition [16]. Further exposure to these elevated temperatures transforms the coherent \(\beta''\) needles into semi-coherent \(\beta'\) rods [7], which nucleate preferentially at dislocations within the Al matrix. Even more prolonged aging, results in the loss of coherency between the \(\beta'\) rods and the matrix phase and formation of the equilibrium \(\beta(\text{Mg}_2\text{Si})\) phase [7]. The final equilibrium Mg\(_2\)Si phase appears as incoherent platelets in the aluminum matrix [17].

These small Mg\(_2\)Si precipitates improve the strength and hardness of the alloy only when finely dispersed throughout the aluminum grains and can act as barriers to dislocations [10, 18]. If the alloy is slow cooled from the solution treatment temperature, these precipitates form at the grain boundaries and do not act as effective barriers to dislocations [18]. Alternatively, if the alloy is overaged, then the precipitates begin to coalesce forming coarser...
particles, resulting in a reduced number of dislocation barriers and consequently a reduction in strength and hardness [15].

Aging temperatures and times both have a significant influence on the final properties of the alloy, as displayed in Figure 2.1. Age hardening at higher temperatures results in an earlier and lower peak hardness as compared to aging at lower temperatures [16]. However, the final state of most industrial products is in their underaged condition for an acceptable combination of strength and ductility [9, 17]. Canadian Autoparts Toyota Inc. ages their wheels at a temperature of 140±5°C for approximately 4 hours [4].

2.2 Dimensional Changes during heat treatment

As previously described, during the heat treatment operation both permanent and non-permanent dimensional changes can occur. The non-permanent changes result from elastic deformation, which disappears when the loads causing this deformation are removed such as the dissipation of thermal gradients. Changes of a permanent nature form as a result of inelastic deformation. The inelastic deformation arises from creep and/or plastic deformation resulting from the presence of thermal and/or gravitational loads. In the general case, additional loads and dilational changes can arise from externally applied loads, constraints, crystallographic phase transformations, and/or precipitation reactions [8]. The dimensional changes that occur in the three stages of the heat treating process (solution heat treatment, quenching and aging) will be discussed in further detail.

2.2.1 Distortion during Solution Heat Treatment

During the solution heat treatment, dimensional changes or wheel distortion can be attributed to creep at elevated temperatures [8]. This effect can be minimized by a combination of controlled temperature and time, proper design of the heat treating tray in which the wheels are loaded and proper loading of the wheels within the tray. For example, the tray is made of steel with a lower coefficient of thermal expansion than aluminum and therefore proper design would incorporate allowances to ensure that the expansion of the aluminum wheel is not restricted. Proper loading would require that adjacent wheels do not impinge on wheel expansion during the solution heat treatment. In addition, dimensional changes can also be
attributed to the solution of phases formed by major alloying elements, which cause volumetric expansion or contraction, depending on the alloy system [8]. Overall, based on previous work at CAPTIN that characterized wheel deformation at each manufacturing stage, the magnitude of dimensional changes that occur in the solution treatment are minimal compared with the quench process.

2.2.2 Distortion during Quenching
The rapid quench required to inhibit precipitation can introduce large thermal gradients within the sample that can lead to large thermal stresses, inelastic strain accumulation, residual stresses and distortion within the quenched component [15]. Upon submersion, rapid cooling of the surface causes the component surface to contract, imposing tensile stresses at the surface and compressive stresses in the interior [15, 19]. The surface tensile stresses have the potential to exceed the local yield or flow stress of the alloy, which is relatively low due to the high temperature of the material, resulting in inelastic elongation of the surface layer [15, 19]. As the interior section of the component cools, the cooler surface layer restricts the interior section from contracting, initiating a state of compressive stress at the surface layer and tensile stress in the interior section [15, 19]. At the end of the quench, the component will be in a state of high compressive stress at the surface balanced by a state of high tensile stress in the interior [15, 19]. The stresses remaining in the quenched component are termed residual stresses and are undesirable as they may cause excess distortion in the component.

In addition, to the ‘through-thickness’ or local accumulation of differential inelastic strain described above, there is a potential for different amounts of plastic strain to accumulate in different sections within the wheel. For instance, more yielding (thus residual stress formation) may be experienced in the hub section as compared to the spoke section, or different amounts could arise within the wheel rim than in the spoke. These additional differential strains lead to a further accumulation of residual stress and distortion as the body seeks to equilibrate.
The magnitude of residual stress and the degree of distortion that develop during quenching increase as the cooling rate increases due to the associated increase in thermal gradients. Reducing the cooling rate by increasing the temperature of the quench water can reduce thermal gradients. At CAPTIN, the wheels are quenched in a hot water bath (approximately 70°C), to avoid excessive distortion. However, the wheel distortion problem cannot be solved simply by adopting a milder quench as there are negative impacts on the precipitation hardening process resulting in reduced fatigue performance. Too slow of a cooling rate results in heterogeneous precipitation of large Mg\textsubscript{2}Si particles, which reduces the concentration of Mg and Si in solid solution required for subsequent precipitation of fine precipitates [20]. In addition, as the quench water temperature is increased, there is a greater potential for locally formed steam pockets, which can result in a reduction of local cooling rates and thus, a further reduction in strength in those areas [9]. Finally, there is an advantage in some residual stress formation in that the compressive surface stresses act to inhibit fatigue failure and stress corrosion [8]. Therefore, a compromise in the cooling rate must be made in order to minimize the distortion effects while maximizing the strength and fatigue performance.

### 2.2.3 Distortion during Aging

In the aging heat treatment, the temperature is relatively low (~140°C) hence there is little capacity for creep-based relaxation of residual stresses and/or creep-based accumulation of distortion due to gravity loading. The factors that have the greatest influence on dimensional changes are the dilution of the solid solution (which changes the lattice parameter) and the formation of precipitates [8]. Although these changes in density and specific volume should be the reverse of those caused by the solution heat treatment, the precipitate is of non-equilibrium transition forms and thus the amount of dimensional change during precipitation does not completely counteract the opposite change that occurs during the solution treatment [8]. The net overall effect is a relatively small dimensional expansion during this stage of the heat treatment.

It is important to point out that although residual stress formation during quenching is the largest single contributor to the overall wheel deformation, the total deformation is a product
of all the manufacturing stages involved in wheel production; including casting and machining operations (the deformation arising from machining operations is caused by the removal of material and the resulting re-establishment of equilibrium and from plastic deformation induced from tool/part interaction) [21]. In fact, the deformation caused during quenching cannot only offset previous deformation, but can also add to any previous deformation, resulting in severe wheel distortion. The complete description of wheel distortion is thus very complex and beyond the scope of this project.

2.3 Model Development
Due to industry’s desire to reduce overall wheel distortion, there is a large incentive to develop a better understanding of the mechanisms associated with wheel deformation during the quench operation. One approach toward this goal is to develop mathematical models to simulate the thermal strain and stress formation during the quench operation and thereby understand the factors that influence wheel deformation. Once developed, such a model could be used to explore the effect of changes in the process parameters on wheel deformation.

There are only a few published papers in literature that use modeling to predict residual stress formation in cast aluminum components. Becker et al. [19] used finite element modeling to predict both distortion and residual stress in aluminum bars that were quenched on one surface. The model used to describe the experimental setup was relatively simplistic. However, this model was used to demonstrate the sensitivity of quench distortion to the thermal boundary conditions, thermal physical properties and the constitutive data.

Ruan [22] used numerical approaches to determine the residual stress pattern and the magnitude in continuously quenched axisymmetric components. They concluded that the magnitude of inelastic strains rapidly increased initially and then remained nearly constant for the rest of the cooling process.

Hetu et al. [23] developed a three-dimensional finite element model of a die cast aluminum wheel to predict filing, solidification, residual stresses and associated shrinkage during
solidification of the casting process. Although their numerical results could not be used for modeling the quench process, it did identify potential high-stress areas of the wheel.

Auburtin and Morin [24] used finite element modeling to account for residual stress generation in A356-T7 cast aluminum cylinder heads during the water quenching operation. In their paper they outlined the method used to determine the heat transfer coefficient from an experiment conducted on a simple-shaped component. The predicted thermal profiles within the cylinder head were shown to be very close to the experimental profiles, which served to validate the experimentally determined heat transfer coefficients. They also presented predictions from a stress model that were in good agreement with the experimental observations. As to be expected, they stated that regions of highest residual stresses occurred in high thermal gradient areas. In addition, they stated that residual stress distributions in the cylinder head differed depending on the use of different heat transfer coefficients, based on geometry and quenching orientations.

From the standpoint of model development there are several key parameters that must be obtained from the literature or measured experimentally. As the model will necessarily need to predict the evolution of temperature and thermal stresses, parameters related to both the thermal analysis and mechanical analysis will be required. For the thermal analysis, this includes the heat transfer coefficient to serve as thermal boundary conditions and for the mechanical analysis, a detailed description of the constitutive behavior of A356 in its solutionized condition is necessary.

2.3.1 Thermal Boundary Conditions - Boiling Heat Transfer

Modeling the heat transfer during the quenching operation is complicated by the non-linear convective boundary condition imposed on the surface of the component. Problems arise in developing an accurate model due to the large variation in magnitude of the heat transfer coefficient during the quench and its sensitivity to small differences in the quenching conditions and the state of the component [25]. During quenching, the heat transfer coefficient is not constant, but a complex function dependent on the quench water
temperature, the cooling rate, the surface temperature, the geometry and the surface condition of the component [20].

During a quench, the surface of a component will experience the characteristic cooling curve, as illustrated in Figure 2.2. When the component is quenched from a temperature that is well above the saturation temperature of the liquid coolant, the surface of the component experiences four distinct heat transfer regimes. These four distinct stages of cooling during quenching are represented in Figure 2.2 and include the initial liquid contact stage, the vapor blanket stage (also termed the film boiling regime), the nucleate boiling stage and the convective cooling stage (also termed the liquid cooling stage).

In the initial liquid contact stage, the hot casting is immersed in water and the contact between the water and the hot surface leads to intense boiling [21, 25]. Cooling is extremely rapid, but only lasts for a very short time (about 0.25 seconds). It should be noted that this behavior is typically not observed in cooling curves produced from thermocouples embedded in a specimen because the effect cannot be observed sub-surface [25].

When sufficient vapor has been generated to completely surround the surface of the casting, a vapor blanket is formed [15, 21, 26]. This vapor blanket insulates the surface from the water and the dominant heat transfer mechanisms are radiation and gas convection [20]. Consequently, the heat transfer coefficient and cooling rates in this stage are relatively low compared to nucleate/convective cooling [15, 24].

When there is no longer a sufficient amount of heat available to maintain the vapor blanket, the vapor blanket breaks down locally and allows direct contact between the quench water and the aluminum casting, resulting in violent boiling [15, 21, 25, 26]. This stage is termed the nucleate boiling stage and results in significantly greater surface heat transfer coefficient and cooling rates [15, 21, 26]. The transition from the vapor blanket stage to the nucleate boiling stage is not sudden, but occurs gradually as the amount of liquid/solid contact increases [25]. During this transition, the size of the bubbles decreases and the duration of liquid/solid contact increases as the casting cools, resulting in considerably higher surface
heat fluxes [25]. This is the most critical stage during the quench as it results in high cooling rates and large thermal gradients between the surface and the interior of the casting, and potentially also between different sections of the casting [21].

As the temperature of the component falls to the boiling point of the liquid, the rate of vapor generation is greatly reduced and the component enters the convective cooling stage or the liquid cooling stage [21, 26]. During this stage, convection currents circulate within the superheated liquid and natural convection heat transfer occurs resulting in a comparatively low rate of heat transport [15, 26].

Poirier & Geiger [26] have reported that quenching heat transfer has a strong correlation with boiling heat transfer, which is a very complicated process. Figure 2.3 displays the characteristic boiling heat-transfer curve, in which several regimes exist. The vapor cooling stage or the film boiling regime, as described previously, corresponds to stages V and VI in Figure 2.3. The nucleate boiling stage corresponds to stages III and IV with associated large heat fluxes. The convective cooling stage corresponds to stages I and II in which relatively low heat transfer is observed.

There are several factors that influence the heat transfer coefficient. One of the primary parameters is the quench bath temperature. As the bath temperature increases, the maximum surface heat flux decreases [21] and the vapor blanket becomes increasingly more stable, (particularly for water temperatures above 40 – 50 °C) leading to prolonged cooling times in this region [21, 27].

Quench tank agitation also has a substantial influence on the heat transfer coefficient. Increased agitation and fluid momentum acts to facilitate the breakdown of the stable vapor blanket and thus increase heat transfer [21, 27]. Agitation also causes the transition from stable film boiling to nucleate boiling to occur at higher temperatures, increasing the peak value in the heat transfer coefficient and shifting it to higher temperatures [21]. In addition, the heat transfer coefficient is more sensitive to agitation as the quench water temperature
increases [27, 28]. Thus increased agitation may be expected to increase thermal stresses and wheel distortion [21].

The surface heat flux is also affected by the surface condition of the casting [21]. One source [20] reported that the surface roughness and wettability influences the stability of the vapour film. While another source [21] agreed and added that the number of nucleation sites for bubbles increases with surface roughness and that with increasing surface roughness, the breakdown of vapor blanket occurs at higher temperatures, resulting in the peak temperature differences between surface and the interior occurring at higher temperatures [21]. This suggests that the component’s yield stress would be exceeded at higher temperatures, increasing the potential for a greater amount of distortion. They concluded that increasing the surface roughness increased the distortion of the part, indicating that chaotic vapor formation is the main reason for distortion problems.

The geometry and temperature of neighboring surfaces affect the flow pattern of the water and influence the stability of the film, thereby also influencing the heat transfer coefficient [20]. The uniformity of the quench, thereby also influencing the heat transfer coefficient [20]. The local heat transfer is influenced by the direction and velocity of the quenchant flow past the casting [21]. In an industrial setting and with components with complex geometries, it is extremely difficult to control the direction and velocity of the flow of water. Tiryakioglu et al. [21] stated that although this task is very difficult, every effort should be made to maintain the uniformity of flow conditions past the casting, especially in the vertical direction in order to minimize distortion problems.

Due to the complex nature of the heat transfer coefficient, experimental temperature measurements at the surface and interior of the component are often used to calculate the heat transfer coefficient [25]. Auburtin and Morin [24] measured the thermal profiles with thermocouples at five locations in a thick square plate made from an A356 aluminum alloy during the solution treatment and the quench. The plate was held at 540°C for 6 hours and then quenched in a non-agitated room temperature water bath. A thermal model of the thick plate was then developed using the temperature-time data recorded on the surface of the plate.
as a boundary condition. Using the thermal gradients at the surface of the sample estimated from this thermal model, the heat flux, $\phi_1$, leaving the sample at a given time was calculated using the following expression.

$$\phi_1 = k \cdot \left( \frac{dT}{dx} \right)_{Surface}$$

Note that the thermal conductivity, $k$, is dependent on the surface temperature. The heat flow through the interface of the aluminum surface and the quench water is expressed as

$$\phi_2 = h \cdot (T_{Surface} - T_{Bath})$$

Since heat flow continuity must be maintained, $\phi_1 = \phi_2$ and therefore the following expression is produced.

$$h = \frac{k \cdot \left( \frac{dT}{dx} \right)_{Surface}}{(T_{Surface} - T_{Bath})}$$

This expression can be solved to present the heat transfer coefficient as a function of surface temperature and temperature gradient at the surface.

Although most heat transfer coefficient data are found by experimental means, Bamberger and Prinz [29] derived an expression for the heat transfer coefficient as a function of the thermal physical properties of the quenched material, the surface temperature of the metal (above 250°C) and the initial bulk temperature of the cooling water. They proposed the following equation for the heat transfer coefficient at surface temperatures greater than 250°C.

$$h = 1.4 \sqrt{k_m \rho_m C_p} \exp \left( 0.32 \cdot \frac{T_s - T_e}{T_0 - T_e} \right) + h_v + h_{rad}$$

where $h_{rad}$ is given by the following expression.

$$h_{rad} = \sigma \varepsilon \frac{T_e^4 - T_0^4}{T_e - T_0}$$

where $k_m$ is the thermal conductivity of metal (W/m/K), $\rho_m$ is the density of metal (kg/m$^3$), $C_p$ is the specific heat of the metal (J/kg/K), $T_s$ is the surface temperature of the metal (K), $T_e$ is the evaporation temperature of the cooling agent (K), $T_0$ is the initial bulk temperature of the
cooling agent (K), $T_a$ is the ambient temperature (K), $h_v$ is the heat transfer coefficient for stable film evaporation at high surface temperature ($\sim 750\text{W/m}^2/\text{K}$), $h_{\text{Rad}}$ is the heat transfer coefficient for radiation ($\text{W/m}^2/\text{K}$), $\sigma$ is the Stephan-Boltzmann constant and $\varepsilon$ is the emissivity of aluminum.

The variation in the heat transfer coefficient with surface temperature is shown in Figure 2.4 for Auburtin and Morin's experimental results together with the results using Bamberger and Prinz's empirical expression, for a bath temperature of 25°C. The heat transfer coefficient in this expression, which is only valid for temperatures above 250°C, gradually decreases with increasing surface temperature. Bamberger and Prinz do not define a Leidenfrost point, which is commonly observed in plots of heat transfer coefficient as a function of surface temperature. The results using Bamberger and Prinz's expression have also been produced using 70°C quench water which is employed at CAPTIN.

As can be seen, Auburtin and Morin's calculated heat transfer coefficient function leads to generally higher values for the heat transfer coefficient with the difference increasing with decreasing temperature. As expected, the effect of increasing the quench water temperature from 25 to 70°C is a decrease in the heat transfer coefficient. At temperatures between 250 and 500°C, the coefficient is reduced by 74%. This shows the large impact that the water temperature has on the heat transfer coefficient.

One problem with the approaches described above is that the heat transfer coefficient functions were determined from small test specimens with geometrically simple shapes. Hence, it is unclear to what extent the data will be applicable to components possessing complex geometries. Reports have suggested that the heat transfer coefficient is dependent on the shape, size, surface condition and composition of the component being quenched [25]. Hence it may prove necessary to make substantial adjustments.

Auburtin and Morin [24] have presented three adjustment factors; a geometric complexity factor, an orientation factor and a space-time factor. A geometric complexity factor would account for the differences in the heat transfer coefficient arising from the complex geometry
of the component. An orientation factor may be important because as the steam bubbles rise vertically in the water; vertical surfaces of the component may exhibit different heat transfer characteristics as compared to the horizontal surfaces. Bubbles could potentially become trapped adjacent to the underside of various sections within the component leading to local areas of reduced heat transport. Furthermore, a difference may be observed in the cooling characteristics of horizontal surfaces depending on the water being above or below this surface. A space-time factor represents the differences in the heat transfer coefficient as a function of height. This factor could account for effects such as the lowering of the component into the bath and the fact that the collapse of the stable vapor film layer typically starts at the bottom of the casting and then proceeds upwards with time.

Totten et al. [27] showed that all three stages of nucleate boiling cooling behaviour could occur on the surface simultaneously during the quenching operation. The net results will be local variations in heat transfer that may give rise to increased thermal stresses and increased levels of distortion [27]. Totten et al. suggested that an increase in the water temperature could result in an increase in the thermal gradients and increased distortion via the mechanism sited above [27]. Tiryakioglu et al. [21] have suggested that in fact the main mechanism for distortion is not the temperature differences between the surface and interior, but the non-uniform cooling between various regions within the component due to the simultaneous presence of different cooling mechanisms on the same part during quenching. This may be even more prominent in quench components with complex geometries, such as the die-cast wheel.

2.3.2 Constitutive Behavior of A356

Another important requirement for developing an accurate mathematical model for the quenching process is a detailed description of the constitutive behaviour of the A356 alloy in the solutionized condition. However there seems to be a lack of available data for this particular alloy in this condition. Most stress-strain data for A356 focuses on the strength of the material in its final T6 condition. Brosnan and Skivkumar [30] reported the room temperature yield stress and ultimate stress of A356 in the T6 final condition at different elevated-temperature exposure times. While Voorhees and Freeman’s [31] investigation
Chapter 2: Literature Review

reported the yield strength and tensile strength of A356 in the T6 condition held for varying times at an elevated deformation temperature, for strain rates from 0.00005 to 1.0 s\(^{-1}\). Their yield stress values for test temperatures of 232°C and 316°C ranged from 139 to 141 MPa and from 93 to 73 MPa at strain rates of 1 and 0.01 s\(^{-1}\), respectively. Another study by Skivkumar et al. [11] concentrated on the yield stress of A356.2 (an alloy with more alloying elements than A356 and less Mg than A356) obtained from room temperature tensile tests performed on samples that were solutionized at constant temperature for various times, quenched and aged. While Chan et al [32] reported the yield strength of A356.2 in the solution treated, natural aged and peak-aged conditions at temperatures ranging from -60 to 400°C at a strain rate of 5.7 x 10\(^{-4}\) s\(^{-1}\). Their solution treated yield stress values gradually increased from ~70 MPa at room temperature to ~145 MPa at 250°C and then decreased to ~40 MPa at 350°C. For their peak-aged condition specimens, the yield stress gradually decreased from ~208 MPa at room temperature to ~155 MPa at 250°C and then significantly dropped to ~40 MPa at 400°C. This showed that the yield stress is significantly greater in the T6 condition as compared to the solution treated condition at lower temperatures and that the difference between T6 and solution treated conditions becomes negligible as the temperature is increased above 250°C.

Other studies [33] and [34] discussed the effects of the aging treatment by performing room temperature tensile tests on specimens aged at different temperatures and for various times. Romietsch et al. [35] developed a model to predict the room temperature yield strength of A356 in the T6 condition as the aging conditions are varied. Although the constitutive data for A356 has not been provided in the solution treated condition, general trends emerge from tests performed on the T6 heat-treated condition that may apply to the solution treated condition. For instance, Dighe et al. [36] conducted uniaxial compression tests on A356 samples in the final T6 condition at strain rates ranging from 10\(^{-4}\) to 3.7 x 10\(^{3}\) s\(^{-1}\) at room temperature. They concluded that the constitutive behavior of A356 is very sensitive to strain rate and that the dynamic yield point increases and the rate of work hardening decreases as the strain rate is increased. Brosnan and Skivkumar [30] stated that work hardening is minimal or absent at elevated temperatures, due to a loss of dislocation pile-up and thus a decrease in the strength of the matrix. They added that this
loss in work hardening is due to the loss of precipitate coherency and increased diffusion, leading to ease of dislocation motion. Another source [37] stated that the flow stress of A356 decreases with increasing deformation temperature in the as cast condition at temperatures ranging from 300 and 540°C. They also reported the findings of uniaxial compression tests on A356 at temperatures ranging from -51.1 to 121.1°C and concluded that the yield strength and rate of work hardening increased as the temperature decreased. They also observed a stronger strain rate dependence on yield and hardening rate as the temperature is increased.

Blaz and Evangelista [38] performed compression tests at strain rate of $1.3 \times 10^{-3}$ s$^{-1}$ at temperatures ranging from 197 to 497°C on a solution treated AlMgSi alloy with a composition of (0.56wt%Si, 0.53wt%Mg, 0.23wt%Fe, 0.03wt%Cu, 0.04wt%Ti). Their results, as displayed in Figure 2.5, reveal that strain and precipitation hardening were observed at deformation temperatures up to 277°C (550K) and a steady state flow stress was observed at higher temperatures.

These curves are useful in predicting general trends for an aluminum alloy in its solutionized condition relating to deformation temperature and strain rate. However, they cannot be used as constitutive data input for the stress model due to compositional differences between this Al-Mg-Si alloy and A356.
Figure 2.1: Precipitation Hardening Characteristics of an Aluminum Alloy (From Source [14])

Figure 2.2: Cooling Curve of a Metal Part Quenched in Water (from source [21])
Figure 2.3: Characteristic Boiling Curve (From Source [26])

Figure 2.4: Heat transfer coefficient as a function of surface temperature
Figure 2.5: Flow curves for Al-Mg-Si alloy at a true strain rate of $1.3 \times 10^{-3}$ s$^{-1}$. (From Source [38])
3  **SCOPE & OBJECTIVES**

3.1 Scope of the Research Project

The goal of this research project is to develop a mathematical model to predict the evolution of thermal stresses within a die-cast aluminum wheel during the quenching stage of the heat treatment process. This thermal-mechanical model will aid in understanding the mechanisms associated with wheel deformation and the factors that influence wheel deformation.

To achieve this goal, a fundamental approach to mathematical modeling was used to predict the thermal history and thermal stress generation during the quench using the commercial finite element code, ABAQUS\(^*\). ABAQUS is well suited to solve non-linear heat transfer and mechanical analyses and has well documented user-defined subroutines for incorporating common non-linearities encountered in thermal-mechanical models. The evolution of thermal stresses in the wheel during the quenching operation will be predicted using a sequentially coupled thermal-mechanical model that will use the thermal history obtained from the thermal model as input for the mechanical model.

Industrial measurements were performed at Canadian Autoparts Toyota Inc. to validate both the thermal and mechanical components of the model. For validation of the thermal model, the temperature-time response during the quench was measured using thermocouples embedded at various locations within the wheel. The thermal histories as predicted by the thermal model were fit to those measured experimentally by adjusting the relevant model parameters accordingly. For mechanical model validation, the extent of wheel deformation induced during the quenching process was measured using a coordinate measuring machine (CMM). The CMM was used to perform a geometric analysis on the wheel both before and after the quench, measuring the change in shape arising from the quenching operation. These deformation results were then compared to the displacement results as predicted by the mechanical model.

\(^*\) ABAQUS is a registered trademark of Hibbitt, Karlsson & Sorensen, Inc.
An essential component of model development for both the thermal and mechanical analyses is model input parameters. The thermal model required the heat transfer coefficient and the thermal physical properties, while the mechanical model required the thermal mechanical properties for A356 in its solutionized condition. Some of this data was collected from literature while other data was provided by laboratory experiments. Laboratory experiments were necessary to determine the heat transfer coefficient from a temperature of 540°C to room temperature by submerging the end of an A356 sample in an agitated bath and recording the temperature evolution within the sample. The thermal histories were then used to calculate the heat transfer coefficient as a function of surface temperature. A second set of experiments were performed to characterize the constitutive data of A356 in its solutionized condition. For these tests, uniaxial compression tests were performed on A356 samples in their solutionized condition in temperatures ranging from 200 to 500°C and strain rates ranging from 0.001 to 1 s\(^{-1}\).

### 3.2 Objectives of the Research Project

The two objectives of the present investigation are as follows:

- To develop and validate a finite element thermal-mechanical model capable of predicting the thermal and stress distributions in a die-cast aluminum wheel during the quench of the T6 heat treatment process.
- To suggest possible counter measures to control wheel deformation.

This will involve completion of the following subtasks:

- To measure the thermal history of the wheel during the quench in an industrial heat treatment process.
- To measure wheel deformation following the quench.
- To understand the mechanisms of wheel deformation during quenching based on model predictions as well as experimental measurements.
4 EXPERIMENTAL MEASUREMENTS

Both industrial and laboratory-scale experiments were performed to support model development and provide validation. The following sections will describe the industrial experiments, which were used for model validation, and the laboratory experimental work, which will serve as input data for the thermal-mechanical model.

4.1 Industrial Measurements

Industrial measurements were performed in collaboration with Canadian Autoparts Toyota Inc. (CAPTLN) at their wheel manufacturing facility in Delta, British Columbia. Two sets of measurements were completed; one involved experiments with instrumented wheels to measure the heat transfer at different locations throughout the wheel and the second involved using the coordinate measuring machine to characterize wheel deformation occurring during quenching. Temperature measurements throughout the wheel were used to validate the thermal model, while a deformation analysis was used to characterize wheel distortion during quenching and therefore to validate the mechanical model. Both the temperature measurements and the deformation analysis will be discussed in the two following sections.

4.1.1 Temperature Measurements

To validate the thermal model, wheels were instrumented with thermocouples at a number of locations within the wheel to measure the variation in temperature with time during the quenching process. The two objectives for this set of temperature measurements were: 1) to obtain data suitable for validation of the heat transfer model and 2) to characterize any variations in heat transfer associated with orientation within the rack.*

4.1.1.1 Experimental Procedures

The thermal history of the wheel during the quenching operation was monitored using a Datapaq 11 Data Logger data acquisition unit. This data logger is accurate to 0.5°C and is capable of storing 110 000 data readings from 10 thermocouple channels with a sampling rate of 10 Hz.

* Note: In this context, orientation refers to circumferential position within one wheel location in the tray.
frequency up to 10 Hz. One of the challenges encountered while performing these tests is that due to process limitations, the data acquisition unit must endure the entire heat treating operation including the four hours in the solution heat treating furnace, quenching in water and another four hours in the aging heat treating furnace while recording the temperature-time data. Therefore the Datapaq 11 Data Logger was inserted into a water-cooled thermal barrier unit in order to maintain a temperature below its maximum operating temperature of 110°C. This thermal barrier unit is capable of protecting the data logger at temperatures up to 550°C for 20 hours using a water-chilled evaporative layer and an insulating fiber blanket. The Datapaq 11 Data Logger and the thermal barrier unit are shown in Figure 4.1 and Figure 4.2, respectively. The thermal barrier unit was placed in a corner position in the upper level of a T6 tray, in place of a few wheels, as shown in Figure 4.3. The data logger was programmed to start recording temperature-time data when the trigger thermocouple experienced a temperature dropping below 500°C, and continued to record temperature-time data until the data logger memory was full (approximately 38 minutes of thermal readings). After recording the temperature during the T6 heat treatment, the Datapaq 11 Data Logger was removed from the thermal barrier unit and the data was downloaded to a computer and analyzed with Datapaq’s software (Oven Tracker for Windows, Version 4.10).

In order to measure the temperature variation within the wheel, nine thermocouples were embedded in a cast and rough-machined wheel. Thermocouple holes (1/16” in diameter) were drilled into the wheel section to approximately 1 mm beneath the surface of interest and Type K thermocouples sheathed in stainless steel were inserted into the holes and punched into place. Figure 4.4 shows the locations of the nine thermocouple holes, indicated as lines in a profile view of the wheel at a spoke location. The thermocouples were placed in an R-Z plane which bisected one of the wheel spokes. The thermocouple wires were run from the wheel to the data acquisition unit by wrapping the wires around the support beams of the T6 tray and connecting them to the Datapaq 11 Data Logger. Although the Datapaq 11 Data Logger has 10 channels for thermocouples, the number of thermocouples recording thermal

*Note: This is the T6 tray used for the 16-inch wheel and not the tray used for the 17-inch wheel, therefore the wheel arrangement within the tray is slightly different.
profiles in the wheel itself was limited to nine, since one thermocouple was required as the trigger thermocouple, measuring the furnace and quench water temperatures.

The side or edge position located on the bottom level of the T6 tray (the level of the tray that comes into contact with the quench water first) was chosen as the location to measure the temperature response in a wheel during the quench. Based on experience from CAPTIN, wheels in this location in the T6 tray typically experience a large amount of wheel deformation and therefore would be well suited for this analysis.

In order to realize the goal of determining if there are differential cooling conditions dependent on the wheel orientation within the T6 heat-treating tray and to overcome issues with a limited number of data acquisition channels, several measurements were made with the wheel located in the same position within the tray, but rotated to three different positions. The three test orientations are illustrated in Figure 4.5. Orientation #1 (as indicated by X₁) corresponds to a region in proximity to the support flange, orientation #2 corresponds to an exterior region of the tray, which is exposed directly to fresh water, and orientation #3 corresponds to a region adjacent to another wheel.

4.1.1.2 Results

The measured cooling curves for the wheel during the quenching process are presented in Figure 4.6, Figure 4.7 and Figure 4.8 for positions #1, 2 and 3, respectively. Technical difficulties were encountered with the thermocouples during quenching which resulted in failure of thermocouples #5, 6 and 7 for various tests. The cooling curves exhibited an initial slow cooling stage, followed by a extremely rapid decrease in temperature and then a gradual decline to the quench bath temperature (70°C). For the most part, slower cooling was observed for the thermocouples embedded in the hub (TC #1) and the spoke (TC #2 & 3) sections as compared to the thermocouples in the rim section. This was to be expected due to thicker sections present within the hub and spoke.

Temperatures measured in the wheel in proximity to the support flange (corresponding to orientation #1) experienced significantly slower cooling conditions as compared to the other
two orientations. The slower cooling is most likely due to the fact that the support flange limits the transport of water to the areas of the wheel in close proximity to the support flange. Furthermore, there is a possibility that the steel support flange affects heat transport at this location of the wheel due to direct contact between the aluminum wheel and the steel support flange. In addition, this orientation exhibited some thermal variations between the hub, spoke and rim sections.

For orientation #2, significantly faster cooling rates were observed possibly due to increased transport of water and the absence of flow obstructions such as the support flanges. In addition, the cooling conditions resulted in less temperature variation between the hub, spoke and rim sections. The only exception is the thermal history at thermocouple location #2, embedded in the spoke section, where delayed cooling may be the result of vapor formation in the spoke region. The observation remains that the hub (TC #1) and spoke (TC #2 and 3) still cool more slowly due to variations in thickness.

The largest variation in thermal histories within the wheel was observed for orientation #3, which is adjacent to another aluminum wheel. The temperatures in the rim section exhibited very fast cooling rates, cooling to 100°C in under 4 seconds, while those in the spoke and hub cooled somewhat slower, cooling to 100°C in ~7 seconds. The cooling rates of the rim section were very similar to those experienced in orientation #2, suggesting similar cooling characteristics of the rim section for the areas not in proximity to the support flange.

These thermal profiles illustrate a large dependence on wheel orientation, as the cooling rates differ for the various regions measured (orientations #1, 2 and 3). This suggests that there are temperature differentials both in a circumferential direction (around the wheel) and in a direction on a common plane (along the spoke section). The second set of industrial experiments will determine if these temperature differentials during quenching lead to excessive wheel deformation.
4.1.2 Deformation Analysis

The deformation induced during the heat treatment process was characterized by a detailed geometrical analysis performed on die-cast aluminum wheels before and after the T6 heat treating operation at CAPTIN's facility in Delta, B.C. The two objectives of this analysis are 1) to determine if the location of the wheel in the T6 tray has an influence on the amount of wheel deformation and 2) to determine if there are any deformation-related trends in the wheel resulting from the heat treatment. To satisfy these objectives, the following experimental procedures were performed.

4.1.2.1 Experimental Procedures

Fourteen 17-inch diameter wheels, of the normal thirty-six wheels positioned in a T6 heat-treating rack, were analyzed. Prior to the heat treatment, the wheels had been cast and rough-machined. The wheels were measured using the Mitutoyo Coordinate Measuring Machine BN715 (CMM) with a calibrated accuracy of 0.01 mm at CAPTIN's facility on Saturday August 23, 2003. The wheels were then heat treated on Sunday August 24, 2003 and a second set of CMM measurements were completed on Tuesday August 26, 2003. A lettering scheme using the letters A to D was used to identify the rack levels, where A represents the bottom level (i.e. the first level in the rack to come into contact with the quench water) and D represents the top level. Six of the fourteen wheels were selected from rack level A, six were selected from level D and the remaining two wheels were selected from the second level, level B. No wheels were selected from the third layer (level C).

To eliminate one of the possible variables, all thirty-six wheels were placed in the rack with the same relative orientation (air valve facing towards the bottom of the rack during loading). The placement and numbering of the measured wheels is shown in Figure 4.9. No attempt was made to assess the relative contribution of solutionizing, quenching and aging operations on the overall wheel deformation, as a previous study (CAPTIN ID 1280) had indicated that the quenching operation was the major cause of deformation.

CMM measurements were made at 9 circumferential locations (the 10th location, labeled Datum, could not used) around the wheel as shown in Figure 4.10. Six measurements (#0
through #5) were made at each of the circumferential locations. For the following discussion, the six locations have been given the descriptive labels indicated in Table 4.1. Referring to the coordinate system shown in Figure 4.10, the measurements made at locations #0, #2 and #5 quantified the variation in the Z-coordinate of the Rim Face, Protection Flange and Drum Face on a wheel at a constant radius, while the measurements made at locations #1, #3 and #4 quantified the variation in X and Y coordinates at specific Z-coordinates of the ID Outboard Rim, OD Inboard Rim and the ID Inboard Rim. The results have been divided into two sections; the first will discuss deformation results in terms of wheel position in the T6 tray and the second will discuss the distribution of deformation within the wheels.

### Table 4.1: Location Designations of the CMM Measurements

<table>
<thead>
<tr>
<th>Numeric Labels</th>
<th>Descriptive Labels</th>
</tr>
</thead>
<tbody>
<tr>
<td>#0</td>
<td>Rim Face</td>
</tr>
<tr>
<td>#1</td>
<td>ID Outboard Rim</td>
</tr>
<tr>
<td>#2</td>
<td>Protection Flange</td>
</tr>
<tr>
<td>#3</td>
<td>OD Inboard Rim</td>
</tr>
<tr>
<td>#4</td>
<td>ID Inboard Rim</td>
</tr>
<tr>
<td>#5</td>
<td>Drum Face</td>
</tr>
</tbody>
</table>

#### 4.1.2.2 Effect of Position in the Rack

In order to investigate the dependence of wheel deformation on position in the T6 rack, wheel deformation has been summarized as a combination of radial deformation (wheel concentricity) and vertical deformation (wheel flatness).

**Radial Deformation**

The largest difference between the maximum and minimum change in radius was used to quantify radial deformation. The radius, \( R \), at specific locations around the wheel was calculated using the following expression:

\[
R = \sqrt{X^2 + Y^2}
\]
where \(X\) and \(Y\) are the measured coordinates in the x and y directions at positions \#1, \#3 and \#4 (the ID Outboard Rim, OD Inboard Rim and ID Inboard Rim). The angle, \(\theta\), at the measured location was calculated as follows.

\[
\theta = \sin^{-1} \left( \frac{Y}{R} \right)
\]

Angles calculated with this expression were converted from the 90° quadrants to the corresponding values for a 360° wheel. The angles were also shifted by 90° to align the 0° position with the air valve.

It should be noted that to start the measurements on a wheel, the CMM runs through a pre-programmed routine in which three measurements are made around the rim to locate the centre of the wheel. This "centre" then serves as the origin \((X = 0, Y = 0\) and \(Z = 0\)) for subsequent measurements. In this procedure the centre of the wheel is calculated by taking the intersection point of the perpendicular bisectors of two segments between the three points on the rim. In practice however, this method poses a problem if the wheel is not perfectly round. For example, if the wheel is oval in shape, this procedure will result in an erroneous determination of the centre as illustrated in Figure 4.11 (note: in Figure 4.11, the magnitude of the deformation has been greatly exaggerated to more effectively demonstrate this problem). This potential problem in defining the centre then biases the remaining \(X\) and \(Y\) coordinates, introducing an error. Although there is the potential for erroneous results, the magnitude of the error introduced by the uncertainty in the wheel centre was investigated by re-calculating the centre of the wheel using different sets of points and was found to be small in comparison to the overall deformation. Therefore this will not appreciably influence the results and the value determined from the CMM will be adopted as the centre.

The variation in wheel radius at each location around the wheel was characterized in terms of change in radius as follows:

\[
\Delta R = R_{\text{After}} - R_{\text{Before}}
\]

where \(R_{\text{Before}}\) and \(R_{\text{After}}\) are the calculated radii at that particular location around the wheel before and after the heat treatment process. The amount of radial deformation that was
induced during the heat treatment operation was quantified by a single value for each wheel using the following expression.

\[
\text{Radial Deformation} = \Delta R_{\text{Max}} - \Delta R_{\text{Min}}
\]

where \( \Delta R_{\text{max}} \) and \( \Delta R_{\text{min}} \) represent the maximum and minimum change in wheel radius of all circumferential locations around the wheel.

A radial deformation value was calculated for each wheel at the ID Outboard Rim, the OD Inboard Rim and the ID Inboard Rim locations. To assess the effect of wheel location within the tray, an average value of the radial deformation results for each wheel location was calculated from the three measurement locations. The data, arranged in ascending order and presented in terms of wheel location in the rack is shown in Table 4.2. These results reveal that the wheel located on level D in the corner positions experienced the lowest amount of radial deformation, while the wheel located in level D on the side experienced the highest amount of deformation. Overall, the side positions seemed to consistently experience a relatively high level of deformation.

<table>
<thead>
<tr>
<th>Wheel Location (Layer - Position)</th>
<th>Deformation Value (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Level D - Corner (Wheels #5 - 8)</td>
<td>0.64</td>
</tr>
<tr>
<td>Level B - Middle (Wheel #10)</td>
<td>0.86</td>
</tr>
<tr>
<td>Level D - Middle (Wheel #11)</td>
<td>0.89</td>
</tr>
<tr>
<td>Level A - Corner (Wheels #1 - 4)</td>
<td>0.92</td>
</tr>
<tr>
<td>Level A - Middle (Wheel #13)</td>
<td>1.17</td>
</tr>
<tr>
<td>Level A - Side (Wheel #14)</td>
<td>1.24</td>
</tr>
<tr>
<td>Level B - Side (Wheel #9)</td>
<td>1.27</td>
</tr>
<tr>
<td>Level D - Side (Wheel #12)</td>
<td>1.27</td>
</tr>
</tbody>
</table>

**Vertical Deformation**

The wheel deformation was also characterized in terms of vertical deformation (wheel flatness) using the measurements at locations #0, #2 and #5. The amount of deformation
induced during the heat treatment operation is expressed in terms of the largest difference in the change in Z-direction.

\[ \text{Vertical Deformation} = \Delta Z_{\text{Max}} - \Delta Z_{\text{Min}} \]

where \( \Delta Z_{\text{Max}} \) and \( \Delta Z_{\text{Min}} \) represent the maximum and minimum change in Z-direction. The change in Z-value at any location around the wheel was characterized as:

\[ \Delta Z = Z_{\text{After}} - Z_{\text{Before}} \]

where \( Z_{\text{Before}} \) and \( Z_{\text{After}} \) are the measured Z-coordinate at a location around the wheel before and after the heat treatment process. As in the case of the concentricity data, the change in Z-coordinate data for the three locations has been averaged to assess which position within the rack results in the largest change in wheel flatness. Table 4.3 shows the averaged values for the vertical deformation ranked in ascending order in terms of wheel location in the rack, suggesting a trend similar to that observed in the radial deformation data. The three side positions are ranked amongst the worst in terms of vertical deformation. The bottom layer middle position was ranked the highest, while the middle and corner positions of level D were ranked the lowest in terms of vertical deformation.

<table>
<thead>
<tr>
<th>Wheel Location (Layer – Position)</th>
<th>Average Change in Z-Value (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Level D - Middle (Wheel #11)</td>
<td>0.31</td>
</tr>
<tr>
<td>Level D - Corner (Wheels #5 - 8)</td>
<td>0.45</td>
</tr>
<tr>
<td>Level B - Middle (Wheel #10)</td>
<td>0.48</td>
</tr>
<tr>
<td>Level A - Corner (Wheels #1 - 4)</td>
<td>0.76</td>
</tr>
<tr>
<td>Level A - Side (Wheel #14)</td>
<td>0.94</td>
</tr>
<tr>
<td>Level B - Side (Wheel #9)</td>
<td>1.00</td>
</tr>
<tr>
<td>Level D - Side (Wheel #12)</td>
<td>1.00</td>
</tr>
<tr>
<td>Level A - Middle (Wheel #13)</td>
<td>1.14</td>
</tr>
</tbody>
</table>

These measurements indicate that the amount or extent of deformation in a wheel during the T6 heat treatment is dependent on the position of the wheel within the rack. In addition, the wheels located along the sides and in the middle position of the bottom layer tend to be consistently amongst those exhibiting the largest deformation during the T6 treatment.
4.1.2.3 Distribution of Deformation within the Wheel

In addition to determining the effect of rack position on wheel deformation, the deformation distribution within a wheel was characterized in order to reveal any trends that may exist. Some of the factors that could potentially contribute to this distribution include the geometry of the wheel and the configuration of the tray supports. The angular locations of spokes and wheel support flanges are shown in Figure 4.12.

Variation in Wheel Radius

The variation in wheel radius ($\Delta R$) was calculated for measurement locations #1, #3 and #4 for wheel #1 and has been plotted in Figure 4.13 as a function of the angle around the wheel ($\theta$). This figure reveals that the magnitude of the change in radius for the Inboard Rim is on average larger than that of the Outboard Rim. This was expected if you consider the spoke a neutral plane and thus any deflection of the rim would be a function of the distance from this neutral plane. Therefore, the deflection at the Inboard Rim measurement locations would be greater than that for the Outboard Rim measurement location owing to its larger distance from the spoke. It is evident from Figure 4.13 that the radial deformation at the OD Inboard Rim and ID Inboard Rim (locations #3 and #4) exhibit similar responses, both in terms of magnitude and angular variation. This is to be expected since the two locations are both on the Inboard Rim and relatively close to one another.

The response of the Outboard Rim location is nearly perfectly out of phase with the Inboard Rim response. In others words, as the inboard rim is deformed inwards, the outboard rim is deformed outwards and vice versa. The measurements indicate that the ID Inboard Rim experienced the largest amount of radial deformation. The change in radius as a function of angle around the wheel for this location for all of the wheels has been plotted in Figure 4.14 to Figure 4.17. Figure 4.14 shows a plot of the radial change for wheels #1 – 4, while Figure 4.15, Figure 4.16 and Figure 4.17, display the radial deformation plots for wheels #5 – 8, wheels #10 & 11, and wheels #9, 12, 13 & 14, respectively. The location of the spokes and support flanges has been indicated in these figures and the wheels have been grouped in the plots according to similarity of deformation behaviour within the rack. The scales on all the...
graphs were set equal to allow a comparison between different wheel positions within the rack.

The wheels in the top four corners of the rack and in the middle position of level B of the rack (refer to Figure 4.15 and Figure 4.16) clearly exhibit the least amount of deformation. Moreover, the deformation seems less influenced by the support flanges and more a function of the location of the spokes with the minimum and maximum generally corresponding with the off-spoke location.

Overall, Figure 4.17 reveals that the wheels in the side positions and in the middle locations at the bottom level of the rack (level A) experienced the largest variation in radial change and hence the largest deformation, which is consistent with the previous analysis. Moreover, in the case of the side positions, the magnitude and circumferential distribution of this deformation is consistent for all three of these wheels. The deformation pattern seems to indicate that the support flange has an influence – e.g. the minimum radial deformation corresponds to the support flange locations. This makes sense if the flange results in a local hot-spot about which the deformation in the wheel hinges, which is confirmed by the temperature measurements.

Mechanism for the Development of the Deformation Pattern Observed
In an attempt to understand the mechanism driving the deformation observed in the wheels, the deformation occurring in wheel #9 was analyzed in more detail. Wheel #9 was located in the side position of the middle level and hence was subject to a large amount of deformation. Figure 4.18 and Figure 4.19 show the change in radius and the change in Z-coordinate together with the location of the spokes and support flanges for wheel #9.

For this particular wheel, the maximum peaks in both the change in radius and the change in Z-coordinate correspond to the spoke locations which were not affected by the support flange, while the minimum peaks correspond to the spokes in close proximity to the support flange. The deformation in terms of radial change at locations #3 and #4 (Inboard Rim) are
opposite but not equal in magnitude to the deformation observed for location #1 (Outboard Rim).

In terms of change in Z-direction, locations #0 (Rim Face) and #2 (Protection Flange) experienced a positive change in Z, while location #5 (Drum Face) experiences a negative change in Z. The directions of measured displacements that a section of the wheel would undergo located at approximately 107° (spoke region exposed directly to water) is shown schematically in Figure 4.20. This suggests that stresses in the spoke pull in the protection flange and in response, the inboard flange deflects outward and the Rim Face and Protection Flange move downward. This pattern develops at the on-spoke locations at approximately 110° and 250°. At the support flange and spoke areas (32°, 180° & 323°), the same pattern was observed, but in the opposite direction.

Overall, the support flange influences the deformation pattern of the wheels located in the tray where high deformation occurs, such as in the three side locations. Whereas, the wheels experiencing lower amounts of deformation, such as the wheels located in the corners of the top level, exhibited a pattern of deformation that was more influenced by the presence/absence of spokes than the support flanges.

4.2 Laboratory Measurements

Two sets of laboratory-scale experiments were conducted; the first set was used to characterize the heat transfer coefficient, and the second set of experiments was used to characterize the constitutive behavior of A356 in its solutionized condition. The following two sections present the experimental procedures used for these two sets of experiments and their results.

4.2.1 Heat Transfer Coefficient Measurements

The heat transfer coefficient as a function of surface temperature was determined using a small A356 test specimen. This experimentally determined heat transfer coefficient was used as the base-line thermal boundary condition for the model.
4.2.1.1 Experimental Procedures

A rectangular specimen, 15 mm wide by 22 mm high and 110 mm long, was machined from the spoke region of an as-cast wheel supplied by CAPTIN. The temperature distribution within this sample was measured using four Type K thermocouples during an end quench simulation. Three of the Type K sheathed thermocouples were inserted into 1/16" holes drilled to approximately the centre of the specimen at 10.41, 20.65 and 30.96 mm from the quenched surface, while the fourth thermocouple was spot welded onto the quenched surface, as illustrated in Figure 4.21. Tests were conducted by heating the specimen to the solutionizing temperature (540°C) in a resistance furnace and holding at that temperature for approximately 30 minutes. The sample was then cooled in a 55°C agitated water bath by submerging only approximately 5 mm of the end of the specimen into the water, as indicated in Figure 4.21. It should be noted that a bath temperature of 55°C is lower than that currently used at CAPTIN (70°C); however, this was the bath temperature in use at the time of this set of experiments. An insulating material surrounded the specimen to avoid air-cooling from the sides and to promote unidirectional heat flow. The thermal histories from the four thermocouples during the quench were recorded using a data acquisition system at a frequency of 180 Hz until the specimen reached a temperature of 80°C. In addition, a fifth thermocouple was used to monitor the temperature variation of the quench water, which revealed only a slight temperature increase of 5°C in the two liters of quench water.

4.2.1.2 Results

In order to calculate an accurate function for the heat transfer coefficient, a simple thermal finite element model was developed using the commercial finite element software, ABAQUS. The temperature-time data for the surface thermocouple was used as a specified temperature boundary condition for the simple rectangular thermal model. The model was validated by comparing the predicted and measured temperature histories at 10.43 mm from the quenched end. The model temperature predictions agreed reasonably well with the measured data, validating this model. Using the thermal gradients at the quenched surface

\[
\left( \frac{dT}{dx} \right)_{\text{Surface}}
\]

as predicted by this simple model, the following expression can was used (derived in Section 2.3.1) to solve for the heat transfer coefficient as a function of surface temperature.
Chapter 4: Experimental Measurements

\[ h = \frac{k \cdot \left( \frac{dT}{dx} \right)_{\text{Surface}}}{T_{\text{Surface}} - T_{\text{Bath}}} \]

This heat transfer coefficient, plotted as a function of surface temperature, is displayed in Figure 4.22. The calculated heat transfer coefficient is compared with the results reported by Auburtin and Morin [24] and to the empirical relationship developed by Bamberger and Prinz [29]. Refer to Section 2.3.1 for the complete description and discussion of their empirical relationship.

The measured heat transfer coefficient shows the same basic trend as Auburtin and Morin’s reported results. Both set of curves exhibited a double peak pattern with a very rapid increase in heat transfer coefficient at temperatures below ~150°C, followed by a more gradual decline with increasing temperature after the peak value had been obtained. Both curves correspond quite well at temperatures below 100°C; however, the maximum value for the measured heat transfer coefficient occurred at a lower surface temperature and was lower than Auburtin and Morin’s peak value. At temperatures above 250°C, the measured heat transfer coefficient corresponds better with the empirical relationship, which is only valid at temperatures above 250°C, rather than those obtained by Auburtin and Morin. These results suggest that the calculated heat transfer coefficient is similar to the findings reported in the literature. It is prudent to note that it is virtually impossible to have an exact match of the results between two sources since the heat transfer coefficient is a complex function dependent on several factors including the geometry, orientation, rate of lowering of sample into the bath, agitation, material and quench bath temperature.

Although the results indicate fairly good findings, some experimental error may have been introduced and could affect the results. For instance, some portion of the total heat may have been lost through the sides of the sample via radiation and convection, which suggests that the unidirectional heat flow assumption would no longer hold and a more complex analysis would be required to determine the heat transfer coefficient to account for this. Another possible source of error lies in the assumption that only the quenched surface is in direct contact with the water. In fact, the sample was actually submerged to a depth of 5 mm which
was not taken into account in the simple thermal model. Although there is potential for erroneous results, similar trends consistent with findings from literature indicate that limited error was introduced and therefore can be used as the thermal boundary condition for the thermal model.

### 4.2.2 Constitutive Behavior Tests

Another input property required for the thermal-mechanical model is the complete description of the constitutive behavior of A356 in its solutionized condition. While a significant amount of work has been done to characterize the constitutive behavior of A356 and its derivative alloys in the T6 condition (refer to Section 2.3.2 in Chapter 2), only a limited number of studies have been done with the material in the solutionized condition. Due to this lack of data, a series of isothermal compression tests were performed on samples in the solutionized condition at temperatures and strain rates within the range experienced during the quenching operation.

#### 4.2.2.1 Experimental Procedures

The A356 material used in this study was obtained from an aluminum wheel supplied by CAPTIN in the as-cast condition. Compression test specimens, 15 mm in length by 10 mm in diameter, were machined from the spoke regions of this wheel. This area of the wheel is typically prone to porosity problems; however the presence of porosity in the material is unlikely to have a significant effect on the compression tests results as the pores will tend to be closed by the mode of deformation.

The specimens were solution treated in a resistance furnace at approximately 540°C for 4 hours, which was followed by an immediate quench in 55°C agitated water. The samples were then held at room temperature until they were tested. Testing occurred within one day of the initial heat treatment.
Chapter 4: Experimental Measurements

The heating and deformation tests were conducted in a Gleeble 3500™ Thermo-Mechanical Simulator. To offset any precipitation that may have occurred between the quench and testing due to natural aging or during heat up, the Gleeble was programmed to initially heat the samples to 530°C, hold for 30 seconds to solutionize, cool to test temperature and then deform, as shown in Figure 4.23. The samples were compressed between two hydraulically driven Tungsten-Carbide platens. Graphite sheets were placed between the sample and the platens to reduce friction and thus reduce any barreling effect. Initially, the samples were held at 530°C for 5 minutes; however the holding time was decreased to 30 seconds to avoid any creep deformation due to the pressure that was required to hold the sample between the platens. The initial holding force (0.5 kN) was also lowered to minimize this effect, however due to the high hold temperature it was believed that there was still some creep. The thermal cycle up to the deformation temperature was identical for all tests; therefore the amount of creep should be similar for each test.

The specimens were cooled from the solutionizing temperature to the test temperature at a controlled cooling rate of 5°C/s through contact with water-cooled platens. The cooling rate achieved in this manner is significantly lower than what is typically observed during the T6 quench operation. For consistency, all the specimens were cooled at the same cooling rate. This cooling rate was limited by the fastest time that the sample could be cooled to the lowest test temperature (200°C). The specimens were held between 5 to 20 seconds at the deformation temperature before the actual deformation was started in order to obtain better temperature stabilization.

A total of twenty-four uniaxial compression tests were performed using the Gleeble 3500 Thermo-Mechanical Simulator. A strain gauge was used to measure the change in diameter of the specimen, which was used to calculate the true strain. The tests were performed at a series of temperatures between 200 and 500°C, which is the range of relevance for analyzing deformation phenomena during the quench. The tests were conducted at strain rates of 0.001, 0.1 and 1 s⁻¹ and were deformed to a nominal strain of 0.7.

™ Gleeble is a registered trademark of Dynamic Systems Inc., Poestenkill NY
4.2.2.2 Results

The true stress versus true strain curves for A356 samples deformed at strain rates of 0.001, 0.1 and 1 s\(^{-1}\) are plotted in Figure 4.24, Figure 4.25 and Figure 4.26, respectively. The dashed lines in Figure 4.24 and Figure 4.26 represent data from a second series of tests completed under identical temperature and strain rate conditions to assess experimental reproducibility. Overall there is relatively good agreement between the two sets of tests indicating acceptable reproducibility. The one exception is the pair of low strain rate tests (0.001 s\(^{-1}\)) completed at 250°C. In one of the tests (solid line) the sample was initially over constrained during the heat up stage and hence, the results are suspect and should be ignored.

The behaviour of the material in the solutionized condition is complex, particularly at the lowest strain rate. In broad terms, the two higher strain rate experiments (0.1 and 1 s\(^{-1}\)) can be broken down into two types of behaviour; the first occurring at temperatures below 300°C and the other above 300°C. In the tests conducted below 300°C, significant hardening was observed along with little dependence on strain rate. Above 300 °C, the material exhibited only a small amount of hardening and the steady state flow stress was found to be dependent on temperature and strain rate (decreasing with increasing temperature and increasing with increasing strain rate). The sensitivity to strain rate exhibited in the low temperature data is consistent with the higher temperature behaviour, however, the effect is small and maybe within the experimental reproducibility.

For the low strain rate tests (0.001 s\(^{-1}\)), the behavior is more difficult to describe. At low temperatures, 200 and 250°C, the yield stress exceeds the higher strain rate tests. Moreover, the 250°C test shows softening behavior for strains greater than about 0.2. There is also softening observed, but to a lesser degree in the 300°C test. The high temperature test behavior (above 300°C) is consistent with the behavior observed in the higher strain rate tests, although the material generally exhibits less hardening and tends more to a steady state flow stress value. Note that the effect of hardening or softening due to precipitation is expected to be relatively insignificant at strains lower than 0.1.
These results exhibit a complex deformation phenomenon, in which some tests experience softening behavior while others experience hardening or even steady-state flow stress. This behavior may be attributed to dynamic recovery or precipitation effects; however, more investigations would be required to fully understand this deformation behavior. Although these results are complicated and further investigations are required, these results can be used for input in the mechanical model as strain rate and temperature dependent constitutive data for A356.
Chapter 4: Experimental Measurements

Figure 4.1: Datapaq 11 Data Logger

Figure 4.2: Datapaq Thermal Barrier Unit

Figure 4.3: Photograph of the T6 Heat Treating Tray
Chapter 4: Experimental Measurements

Figure 4.4: Thermocouple Locations Embedded within the Wheel

Figure 4.5: Temperature Measurement Locations to Determine Influence of Wheel Orientation
Figure 4.6: Thermal History of the Wheel during Quenching for Orientation #1

Figure 4.7: Thermal History of the Wheel during Quenching for Orientation #2
Figure 4.8: Thermal History of the Wheel during Quenching for Orientation #3
Figure 4.9: Location and Positioning of the Measured Wheels Top View
(With Spokes Facing out of the Page)
Figure 4.10: CMM Locations for Measurements

Figure 4.11: Schematic Representation of Possible Miscalculation of Circle Centre
Figure 4.12: Angular Locations of Spokes and Support Flanges

Figure 4.13: Percent Change in Radius as a Function of Angle around the Wheel
Wheel #1 (Level A – Corner Position)
Chapter 4: Experimental Measurements

Figure 4.14: Percent Change in Radius (Wheels #1 - #4: Level A-Corners)

Figure 4.15: Percent Change in Radius (Wheels #5 - #8: Level D-Corners)
Chapter 4: Experimental Measurements

Figure 4.16: Percent Change in Radius (Middle Positions)

Figure 4.17: Percent Change in Radius (Side Positions & Level A-Middle Position)
Figure 4.18: Percent Change in Radius for Wheel #9

Figure 4.19: Change in Z-Coordinate for Wheel #9
Chapter 4: Experimental Measurements

Figure 4.20: Deformation Directions Observed in Wheel #9

- Sheathed TC inserted into the center of block (~10 mm apart)
- TC attached to quenched surface

Figure 4.21: Schematic of the specimen showing the location of thermocouples
Chapter 4: Experimental Measurements

Figure 4.22: Heat Transfer Coefficient as a Function of Surface Temperature

- Experimental results
- Auburtin and Morin's HTC Function
- Bamberger and Prinz (T_Bath = 25°C)
- Bamberger and Prinz (T_Bath = 55°C)

Figure 4.23: Thermal History used for Experimental Procedures

- Solution Treatment at 540°C for 4 h
- Quench in 55°C water
- Hold at 530°C for 30 s
- Cooling Rate = 5°C/s
- Start of Compression test at deformation temperature
- Natural Age at 25°C for ~24 h
- End of Compression test
Chapter 4: Experimental Measurements

**Figure 4.24:** Flow Curves at a Strain Rate of 0.001 s$^{-1}$

**Figure 4.25:** Flow Curves at a Strain Rate of 0.1 s$^{-1}$
Strain Rate = 1 s

Figure 4.26: Flow Curves at a Strain Rate of 1 s

Chapter 4: Experimental Measurements
Chapter 5: Thermal Model

A mathematical model was developed to predict the evolution of temperature and stress in a die-cast aluminum wheel during quenching. The model is composed of a thermal model and a mechanical model, which have been developed using the finite element package, ABAQUS, in a sequentially coupled fashion. The thermal model was developed to solve the non-linear heat transfer problem arising from quenching a geometrically complex wheel, while the mechanical model was developed to predict the evolution of strain and stress within the wheel arising from the varying thermal field. This chapter will focus on the specific details related to the development of the thermal model in addition to validation through a comparison of the predicted thermal profiles with those measured at CAPTIN.

5.1 General Thermal Model Formulation

The quenching process for aluminum wheels requires a transient heat transfer analysis to compute the evolution of temperature from 540 to 70°C. The thermal model is governed by a three-dimensional transient heat conduction partial differential equation as follows.

\[
\frac{\partial}{\partial x} \left( k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left( k \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial z} \left( k \frac{\partial T}{\partial z} \right) = \rho C_p \frac{\partial T}{\partial t}
\]

where \( T \) is the temperature, \( k \) is the thermal conductivity, \( \rho \) is the density and \( C_p \) is the specific heat. In present case, the latent heat due to Mg\(_2\)Si precipitation is negligible since there is only a small fraction formed (under 1%) [20]. It is also assumed that the effect of the Mg\(_2\)Si precipitation on the thermal physical properties of the alloy is small and therefore can be ignored [20].

5.2 Thermal Physical Properties

The die-cast wheel experiences a large temperature range (from 540 to 70°C) during the quenching operation. As a result, any significant temperature dependencies present in the thermal physical properties must be included, adding to the overall non-linearity of the thermal problem. For solving this problem, user-defined tables listing the thermal properties with temperature dependencies can be easily implemented within ABAQUS.
Mills [39] reviewed the thermal physical properties of A356 from many sources and recommended the values listed in Table 5.1 for the specific heat and thermal conductivity, which were used in a tabular format as input for the thermal model. The density, reported by reference [40], was held constant during the analysis to conserve mass (a fixed domain analysis is used in the sequentially coupled solution algorithm).

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Specific Heat $C_p$ (J/kg °C)</th>
<th>Thermal Conductivity $k$ (W/m°C)</th>
<th>Density $\rho$ (kg/m$^3$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>880</td>
<td>163</td>
<td>2685</td>
</tr>
<tr>
<td>100</td>
<td>921</td>
<td>165</td>
<td></td>
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</tr>
<tr>
<td>567</td>
<td>1127</td>
<td>134</td>
<td></td>
</tr>
</tbody>
</table>

5.3 36° Thermal Model

The three-dimensional thermal-mechanical model was developed by a process of incrementally increasing the complexity of the model. Initial attempts to simulate the quenching process involved a two-dimensional rectangular bar assuming unidirectional heat flow, followed by a more complex two-dimensional axisymmetric cross-section of the wheel. Finally, it was determined that a three-dimensional model with proper description of the on-spoke and off-spoke geometry was necessary to represent the wheel during quenching. This model is used for the following discussion of the thermal-mechanical model development.

Two different geometries were used to model the wheel during the quench: a 36° section and a 180° section of the wheel. The 36° section represents the smallest geometrically symmetric section that the wheel can be subdivided into. The 180° section represents the smallest geometrically symmetric section that process can be subdivided into (the latter arises from
the manner in which the wheel is placed on the rack). Because it is substantially computationally smaller and quicker to run, the 36° symmetric slice of the wheel was used to assess and modify the boundary conditions and to perform a sensitivity analysis, while the larger mesh (180° slice of the wheel) was used to capture temperature variations occurring along the circumference of the wheel and to generate the thermal history necessary for input to the stress model. This section will focus on all the aspects related to model development for the 36° model.

5.3.1 Geometry
The geometry representing a 36° section of the wheel was discretized into first-order 4-noded tetrahedral elements using the commercial software package, IDEAS, as a geometry/discretization pre-processing program. Tetrahedral elements are often required for complex geometries, such as the die-cast aluminum wheel. The wheel was divided into three sections in which the element size was varied; the rim section, the hub section and the spoke section. The element lengths in the rim, the hub and the spoke sections were 2, 3 and 4 mm, respectively. A finer mesh was required for the rim section owing to its relatively small section thickness. The resulting mesh for the wheel contained 40627 nodes and 180379 elements. The resulting surface mesh is shown in Figure 5.1. The various sections of the wheel, which are relevant for subsequent discussions, have been labeled in Figure 5.1.

5.3.2 Initial Conditions
The initial temperature throughout the wheel was set to 530°C based on the measured temperature data and the reported solutionizing temperature. The temperature of the quench bath was assumed to be at a constant temperature of 70°C throughout the quench.

5.3.3 Boundary Conditions
One of the more important aspects in developing the thermal model is characterization of the thermal boundary conditions. These boundary conditions describe the heat transfer between the surface of the aluminum wheel and the surrounding environment. Heat transfer from the surface of the wheel to the surrounding water has been described by the following standard equation for convection:
\[ q'' = h(T_{\text{surf}} - T_{\infty}) \]

where \( q'' \) is the heat flux across the surface (W/m\(^2\)), \( h \) is the heat transfer coefficient (W/m\(^2\)/°C), \( T_{\text{surf}} \) is the surface temperature (°C) and \( T_{\infty} \) is the quench water temperature (°C).

This convective boundary condition was used to characterize the heat transfer over the entire surface of the wheel, except in the locations where conditions of symmetry have been assumed. For the two symmetry planes occurring in both the 36° and 180° geometries, a zero heat flux boundary condition was employed.

The heat transfer coefficient is a complex function dependent on several factors including the surface temperature of the aluminum wheel, the quench water temperature and the orientation of the wheel all of which contribute to yield a highly non-linear problem. These various dependencies necessitate the use of a user defined subroutine available within ABAQUS. This subroutine is called during the analysis to define a surface-based non-uniform, time dependent heat transfer coefficient. The laboratory scale heat transfer coefficient data, previously described in Section 4.2.1, was used as a starting point or baseline for the thermal analysis on to which the necessary sophistication or complexity was added. The base-line temperature-dependent heat transfer coefficient is presented in Figure 5.2.

### 5.3.3.1 Characterization

Initial attempts to apply the base-line heat transfer coefficient data produced cooling rates significantly greater than those measured experimentally, as displayed in Figure 5.3. This was corrected using a variety of approaches based on a careful assessment of the measured cooling behaviour.

Referring to Figure 5.3, upon examination of the measured thermal histories and comparison to the model predictions with the base-line heat transfer coefficient data, there appears to be four distinct regimes of behaviour for a typical cooling curve. In order of increasing time they are: I) the quench delay, II) initial water contact, III) stagnant water flow and IV) agitated water flow regimes. These different regimes are identified in Figure 5.3 for the measured temperature-time response for orientation #2. In each of these four regimes, the
heat transfer coefficient was adjusted in order for the model predictions to fit the cooling conditions observed. As it turned out, it proved necessary to vary the heat transfer coefficients with time and position in a given 36° model, as tabulated in Table 5.2. In addition, it was found that the various 36° models, which represent the various orientations of the wheel in the tray, required different overall magnitudes for the heat transfer coefficients. (Note: in this context position refers to vertical or z-coordinate (refer to Figure 5.1.) and orientation refers to the circumferential position of the 36° section of the wheel within the rack – e.g. outward face oriented toward the support flange or outward face oriented toward the outside of the rack – see experimental section).

<table>
<thead>
<tr>
<th>Regime 1:</th>
<th>Orientation #1 (Support Flange)</th>
<th>Orientation #2 (Outer Region)</th>
<th>Orientation #3 (Inner Region)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Air Cooling</td>
<td>100 W/m²/°C</td>
<td>2000 W/m²/°C</td>
<td>1500 W/m²/°C</td>
</tr>
<tr>
<td>Regime 2:</td>
<td>Initial Contact HTC</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stagnant Flow HTC Factor</td>
<td>0.125</td>
<td>0.40</td>
<td>0.35</td>
</tr>
<tr>
<td>Regime 4:</td>
<td>Agitated Flow HTC Factor</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>0.25</td>
<td>0.70</td>
<td>0.60</td>
</tr>
</tbody>
</table>

Based upon an examination of the process, the quench delay occurs because the wheel takes a finite time to be submerged in water. The amount of time required for this regime varies depending on the vertical location with respect to the height of the wheel (Z-direction in Figure 5.1). Since the wheel is lowered into the quench tank with the inboard rim facing downwards, those positions located on the tip of the inboard rim experience a very short quench delay, while those located at the tip of the outboard rim experience a longer quench delay. Prior to entering the water, the surface of the wheel is subjected to a combination of air-cooling conditions (convection and radiation) to the surrounding environment. A temperature independent value of 100 W/m²/K [19] was used as the heat transfer coefficient for the quench delay regime in all of the models (orientations) – see Table 5.2. The variable quench delay was implemented in the user-programmable subroutine by having the delay
time a function of the \( z \)-coordinate and the rate at which the wheel was lowered into the water.

The initial contact regime occurs because of a transient in the development of a film of vapor upon initial contact with water. This film tends to insulate the wheel resulting in a relatively low rate of heat transport (higher than air cooling but lower than fully developed boiling water heat transfer). Because of the varying duration of regime I, regime II commences at different times depending on the \( z \)-coordinate. In addition, the duration of regime II (measured from the experimental temperature-time data as the period of time when slower cooling conditions were observed) varied for each orientation. A temperature independent heat transfer coefficient was also applied to this regime. However, in determining this value for the temperature independent heat transfer coefficient, it was observed that the magnitude of the heat transfer coefficient needed to be varied dependent on the orientation of the wheel that the 36° model was simulating. For the orientations where the flow of quench water was restricted or obstructed, such as for orientation #1 (in proximity to the support flange) and for orientation #3 (in a region adjacent to another wheel), the heat transfer was lower and therefore a lower value for the heat transfer coefficient was applied.

The third or stagnant flow regime, accounts for the period of time following the breakdown of the vapor film when the flow of water in the tank is stagnant - e.g. prior to the activation of the quench tank circulation pumps. The magnitude of the heat transfer was found to vary for the different orientations. The approach adopted was to multiply the base-line, temperature dependent heat transfer coefficient data by a factor less than one, termed the HTC factor, to reduce the rate of heat transfer (relative to the laboratory test). The magnitude of this factor was adjusted by trial and error, until the thermal histories predicted by the model were found to agree satisfactorily with those measured with the wheel in the various orientations. The factors used to represent the best fit for the model are presented in Table 5.2. In the most extreme case, the magnitude of the base-line heat transfer coefficient had to be multiplied by a factor of 0.125 to match the measured temperature-time response for the support flange orientation. Some HTC factors used in the model are presented along with the base-line temperature-dependent heat transfer coefficient in Figure 5.2.
The agitated flow regime occurs after the quench tank pumps have been activated and the water flow has significantly increased. (At CAPTIN, the quench tank agitation jets are activated once the wheels have been fully submerged for approximately 3 seconds.) Once again owing to the dynamics of wheel submersion and the breakdown of the transient development of a vapor film, this regime of behaviour was observed to occur at slightly different times depending on position and also orientation. The outer region of the wheel (orientation #2) was first to experience the effects of agitation, while the support flange region (orientation #1) was delayed possibly due to the obstruction from the flange structure. In this regime, the heat transfer can be characterized as being relatively high due to increased flow of water. To account for this increase in heat transfer, the HTC factor used in this agitated flow regime was increased from the stagnant flow regime. As in the previous cases, the effect of agitation was found to vary dependent on orientation of the wheel in the rack. For instance, for orientation #1 the HTC factor was increased to 0.25, while for the outside and inside orientations it was increased to 0.70 and 0.60, respectively. The resulting values that were used in order to obtain the best agreement between predictions and measured data are summarized in Table 5.2.

The bath temperature and the wheel submersion rate are values directly measured at CAPTIN and were not varied for the three orientations. Note that in all simulations, the appropriate heat transfer coefficient was described by the same function of temperature at all points of the surface of the wheel.

5.3.3.2 Sensitivity Analysis on the Boundary Condition Parameters

A sensitivity analysis was performed in which the heat transfer in each of the four regimes identified in the previous section was examined. In each case, the optimum heat transfer coefficient is presented together with the results for two additional cases in which the heat transfer coefficient is perturbed from the optimum value. The optimum refers to the value that best fit (based on trial and error visual comparisons) the results obtained for all of the working thermocouples for a given model orientation. This analysis was performed using the data from a thermocouple in the rim section in orientation #2.
Chapter 5: Thermal Model

The sensitivity of the thermal predictions to variations of the temperature-independent heat transfer coefficient employed in the quench delay regime (regime I) is displayed in Figure 5.4, in which the optimum value used for this regime (100 W/m$^2$/K) is compared to a value of 0 and 200 W/m$^2$/K. The sensitivity of the thermal predictions to changes in the value for the heat transfer coefficient proved to be negligible, and therefore a value of 100 W/m$^2$/K was determined to be acceptable for the model.

A second sensitivity analysis was performed to assess the effect of varying the heat transfer coefficient value in the initial contact regime. This value was varied by ±500 W/m$^2$/K relative to the optimum case of 2000 W/m$^2$/K, as displayed in Figure 5.5. Although the temperature predictions deviated from the optimum model at the end of the initial water contact regime, there were minimal temperature differences in the remaining two regimes. This suggests that the temperature evolution in the rim section of the wheel is in general not very sensitive to variations in the temperature-independent heat transfer coefficient applied for regime II.

In a third sensitivity analysis, it was found that the thermal history of the 36° section of the wheel is sensitive to variations of the HTC factor for both the stagnant and agitated water flow regimes, as displayed in Figure 5.6. The HTC factors were increased and decreased by 0.10 from the optimum values; 0.40 for regime III and 0.70 for regime IV. Although there is a large sensitivity to these HTC factors, the optimum values used in the model for the HTC factors were carefully chosen by trial and error to obtain an optimal fit for the thermal histories for all the thermocouple locations for each orientation.

The next section will present the geometry, the initial conditions and the boundary conditions necessary for the development of the 180° model. The temperature-time predictions for the 36° model will be presented along with the predictions for the 180° model for comparison after the introduction of the 180° model.
5.4 180° Thermal Model

The 180° section model was necessary to capture the circumferential variation in heat transfer indicated by the variation of heat transfer coefficient values used in the 36° model and in the deformation analysis, to capture the stresses leading to wheel ovality, thus it represents a substantial improvement over the 36° section model.

5.4.1 Geometry

Similar to the 36° section, the 180° section was discretized into first-order 4-noded tetrahedral elements and divided in three sections with varying element sizes. The mesh for the 180° section consisted of 194708 nodes and 871950 elements. The resulting surface mesh is shown in Figure 5.7.

5.4.2 Initial Conditions

The initial temperature of the entire wheel was set to 530°C based on the measured temperature data and the reported solutionizing temperature. The temperature of the quench bath was assumed to be at a constant temperature of 70°C.

5.4.3 Boundary Conditions

For the 180° model, all three wheel orientations and their corresponding different cooling characteristics, as defined in the 36° model, were accounted for in this single model. Therefore, the HTC factor and the other model parameters were varied depending on location around the wheel in order to correctly predict the thermal histories of all three orientations.

Figure 5.8 shows a top view of the wheel, which has been subdivided into five regions. The spoke regions of the wheel, which correspond to orientations #1, 2 and 3, were modeled using the same model parameters describing the thermal boundary conditions as presented for the 36° model in Table 5.2. For the areas that lie between these locations, parameters used have been linearly interpolated as a function of the angle around the wheel. Note the 18° section that is located in between the plane of symmetry and the spoke section of orientation #2 was not linearly interpolated, but instead was modeled using the same values.
for the parameter as for orientation #2 - refer to Figure 5.8. The following section presents the results obtained with these parameters for both the 36° and 180° models.

### 5.5 Predictions and Comparisons to Measured Data

Contour plots of the temperature distribution for the 36° model at three different times during the quenching simulation for orientation #2 are presented in Figure 5.9 (b) to (d). Referring to these figures, the temperature distributions show vertical gradients within the wheel due to lowering the wheel into the water during the initial stages of the quench, and through-thickness temperature gradients (note that the orientation of the wheel appearing in the figure is upside down relative to its orientation in the quench rank – e.g. the inboard rim, facing up in the figure, faces down in the quench rack). Slower cooling conditions were predicted for regions of higher thermal mass within the thicker regions, such as the hub and the spoke. For instance, when the rim section had cooled to temperatures below 100°C, there were areas of the spoke and hub that remained quite warm ranging in temperatures from 340 - 380°C, as illustrated in Figure 5.9(d).

Contour plots of the temperature distribution within the 180° section of the wheel are presented in Figure 5.10(a) to (c). These plots show the variation in temperature in the circumferential direction arising from the circumferential differences in heat transfer applied to the model. For instance, a faster cooling rate and, thus lower temperatures, were observed for the region directly exposed to good water transport (orientation #2), while the slowest cooling rate, thus warmer temperatures, were predicted for the region in proximity to the support flange. Once again the effect of lowering the wheel into the quench tank is also observed in the temperature profiles in Figure 5.10(a).

A comparison of the experimentally measured data with the predicted thermal histories allows for an assessment of the models predictive capability. For both models (36 and 180°), the thermal histories were compared at three different locations for orientations #1, 2 and 3. As a reminder, orientation #1 corresponds to a spoke region of the wheel supported by a steel structure, orientation #2 corresponds to a section of the wheel near an outer region of the tray, which is exposed directly to good water transport and orientation #3 corresponds to
another spoke region of the wheel that is adjacent to another wheel in the rack. Although temperatures from up to nine thermocouples were recorded, only three thermal histories have been used for discussion. These include thermocouples in the hub, spoke and rim. The temperature predictions obtained from both models (36° and 180°) are presented together with the measured thermocouple data in Figure 5.11 for orientation #1, Figure 5.12 for orientation #2 and Figure 5.13 for orientation #3 for the hub, spoke and rim sections, respectively.

In general, the temperature histories as predicted by the thermal models are in good agreement with the measured data for the duration of the quench. Overall, the best agreement between predicted and measured thermal histories was obtained for orientation #2, where the highest cooling rates were experienced. Orientation #2 was subjected to good water transport with no flow obstructions in the vicinity. There was also good agreement obtained for orientation #1 which corresponds to a location in close proximity to the support flange, which was found to have a comparatively lower rate of heat transport most likely due to obstruction in the flow. The worst agreement was obtained for orientation #3, corresponding to a region of the wheel adjacent to another wheel. The poor agreement obtained in this region is likely due to the added complexity of interaction with the adjacent wheel, including the possibility of interaction from vapor formation from the adjacent wheel and/or a local increase in the water temperature.

One consistent trend that emerged from these comparisons is that the thermal histories for the rim sections exhibited the greatest level of accuracy as compared to the hub and spoke sections. For the spoke and the hub sections, it is possible that vapor bubbles could become entrapped on the underside of these sections, resulting in a local hot spot, which was not accounted for in the model.

Another trend observed was that the temperature-time response in the quench delay regime (regime I) showed an almost exact match for all three thermocouple locations for all orientations, indicating that the applied constant value for the time-dependent heat transfer coefficient was suitable.
Chapter 5: Thermal Model

The temperature predictions from the 180° model correspond almost exactly to the 36° model predictions with the exception of the hub section. Since the model boundary condition parameters were linearly interpolated as a function of angle around the wheel, this is consistent with what would be expected as conduction effects within the hub in the 180° model would tend to “wash out” or reduce the circumferential variations in the boundary conditions.

For the most part, the cooling rates in Regimes III and IV (slope of the temperature-time curve) corresponded quite well with the measured data. However, there are cases where adjustment in the HTC could yield a slightly better fit, which suggests variability in the heat transfer over the surface of the wheel. However, changes in these regions could cause detrimental effects in other regions and therefore adopting additional spatial variations in the heat transfer coefficient was not implemented.

One drawback of the model is the assumption that a constant value for the heat transfer coefficient can be applied for the initial contact regime. This regime is the most difficult regime to reproduce in terms of temperature predictions. There are several complexities that arise when the tray of wheels are first introduced into the water that are very difficult to incorporate into the model. The approach of adopting a constant value for the heat transfer coefficient seemed to work reasonably well for orientations #1 and 2, but seemed to break down for orientation #3. Once again the presence of a wheel in proximity to orientation #3 is likely the problem.
Chapter 5: Thermal Model

Figure 5.1: 36° Section Wheel Mesh

Figure 5.2: Heat Transfer Coefficient as a Function of Surface Temperature
Chapter 5: Thermal Model

- Measured Data (Orientation #1)
- Measured Data (Orientation #2)
- Measured Data (Orientation #3)
- Model Predictions

Regimes (Orientation #2):
I – Quench Delay
II – Initial Water Contact
III – Stagnant Water Flow
IV – Agitated Water Flow

Figure 5.3: Thermal Profiles using the Baseline HTC for the three Orientations
Chapter 5: Thermal Model

Figure 5.4: Sensitivity to the Constant HTC for Regime I (Rim Section)

Figure 5.5: Sensitivity to the Constant HTC for Regime II (Rim Section)

Figure 5.6: Sensitivity to the HTC Factor for Regime III and IV (Rim Section)
Figure 5.7: 180° Section Wheel Mesh

Figure 5.8: 180° Model details
Figure 5.9: Contour Plots at Various Times during Quenching for the 36° Model
Figure 5.10: Contour Plots at Various Times during Quenching for the 180° Model

a) Analysis Time = 1.723 Seconds

b) Analysis Time = 2.383 Seconds

c) Analysis Time = 4.496 Seconds
Chapter 5: Thermal Model

Figure 5.11: Thermal Profiles for Orientation #1
Chapter 5: Thermal Model

Figure 5.12: Thermal Profiles for Orientation #2

- Measured Data
- Model Predictions (36° Model)
- Model Predictions (180° Model)

**Regimes:**
I - Quench Delay
II - Initial Water Contact
III - Stagnant Water Flow
IV - Agitated Water Flow

a) Thermal Profiles for the Hub Section

b) Thermal Profiles for the Rim Section

c) Thermal Profiles for the Rim Section
Figure 5.13: Thermal Profiles for Orientation #3
6 MECHANICAL MODEL

A mechanical model was developed to predict the evolution of thermal stresses within the wheel as it is cooled from the solutionizing temperature during the quench. In addition, the model can predict local displacements within the wheel for comparison to the results obtained from the deformation analysis in Section 4.1.2. This chapter will present a description of the key aspects related to the development of the mechanical model and compare predictions for the wheel displacement with those experimentally measured.

6.1 General Mechanical Model Formulation

In order to calculate the evolution of stress and strain during quenching, it is necessary to satisfy the differential equations of equilibrium based on a force balance on an elemental volume and to satisfy the conditions of compatibility based on the displacement field and its relationship to strain at every point within the body. These two conditions will be satisfied while solving the finite element mechanical analysis using ABAQUS.

For the development of a sequentially-coupled thermal-mechanical model, it is assumed that the thermally induced deformations during quenching are small and therefore the heat generated due to plastic dissipation is negligible and do not influence the thermal model. It should be noted that the model has a one-way coupling, in which the predictions from the thermal model are used as input for the mechanical model. The temperature-time response at each node in the wheel predicted by the thermal model will serve as the ‘thermal load’ for input to the mechanical model, replacing the mechanical loads that are commonly used in a structural analysis.

For this particular quenching problem, it is assumed that the total strain ($\varepsilon$) has contributions from elastic strain ($\varepsilon^{EI}$), thermal strain ($\varepsilon^{Th}$) and plastic strain ($\varepsilon^{Pl}$) components [22] and therefore can be represented by

$$\varepsilon = \varepsilon^{EI} + \varepsilon^{Th} + \varepsilon^{Pl}$$

In order to predict the local displacements in the wheel for comparison with those measured at room temperature in the deformation analysis (discussed in Section 4.1.2), three steps were
required in the mechanical model. The first step heats the wheel from 25 to 530°C to obtain an accurate prediction for the expansion during heating, which is required because the model geometry is based on room temperature geometry; the second step simulates the quench using the temperature predictions from the thermal model as input for the thermal loads; and the third step cools the wheel from the quench bath temperature of 70°C to 25°C, accounting for any contraction resulting from this relatively minor temperature drop.

6.2 Dilatation Effects

The development of thermal stress is driven by differential thermal dilation or thermal strain within the wheel. The thermal strain at any position within the wheel can be calculated using the temperature at that particular time increment and the coefficient of thermal expansion. Hetu et al. [23] have reported a linearly decreasing temperature-dependent function for the coefficient of thermal expansion (\( \alpha' \)) for A356 as follows.

\[
\alpha'(\degree C^{-1}) = 22.6 \times 10^{-6} - 2.39 \times 10^{-8} \times T
\]

In developing the mechanical model within ABAQUS, a total thermal expansion coefficient relative to a reference temperature \( (T_0) \) is required rather than the coefficient of thermal expansion as provided in the literature. The following expression is used to convert the coefficient of thermal expansion from the differential form \( (\alpha'(T) \), provided in literature) to the total form \( (\alpha(T) \), required for ABAQUS).

\[
\alpha(T) = \frac{1}{T - T_0} \int_{T_0}^{T} \alpha'(T) dT
\]

6.3 Constitutive Behaviour

In developing the mechanical model of the quenching operation, it is essential to have an accurate description of the constitutive behaviour of A356 in the solutionized condition. From the process standpoint, the material within the wheel is subject to significant stresses over a range of temperatures from room temperature to 500°C. Thus, it may be expected that the material within the wheel experiences both elastic and in-elastic deformation over a range of strain rates.
6.3.1 Elastic Properties

Under the assumption of linear elastic behaviour, the elastic deformation of a material subject to loading may be related to the stress in the body as follows

\[ \sigma = D_{el} \dot{e}_{el} \]

where \( D_{el} \) is the elastic or stiffness matrix which is assumed to be independent of deformation. There are two material properties within this matrix for an isotropic material, Poisson’s ratio and the modulus of elasticity. Due to the large temperature range experienced during the quenching process, the modulus of elasticity is temperature dependent. Hetu et al. [23] have reported a constant value of 0.37 for the Poisson’s Ratio for the A356 and a linearly decreasing function of temperature (in °C) for the modulus of elasticity as follows.

\[ E(MPa) = 6.93 \times 10^4 - 106 \times T \]

6.3.2 In-Elastic Properties

The experimentally measured stress-strain behaviour for A356 in its solutionized condition over a temperature range of 200 to 500°C and strain rates ranging from 0.001 to 1 s\(^{-1}\) was presented in Section 4.2.2. The behaviour of A356 in the solutionized condition is very complex because of its potential to form clusters and/or precipitates that can have a significant impact on mechanical properties of the material. If formed, these fine particles/clusters can act to pin dislocations and strengthen the material. If there is sufficient time at elevated temperatures, the fine particles can grow and coalesce, reducing their pinning effectiveness, and thereby resulting in a reduction in strength. Thus depending on the thermal history experienced by the sample prior to and during the deformation step, the mechanical properties may vary with time due to microstructural changes in addition to being a function of temperature and strain.

A publication arising from the work on characterization of the deformation of A356, Estey et al [41], concluded that there is an opportunity for precipitation of Mg-Si to occur at temperatures at or below 300°C and for extended holding times (low strain rates) near 400°C. They also identified the potential for softening due to over ageing in the low strain rate tests conducted at 250 and 300°C and it was shown that precipitation does not occur at
temperatures above 425°C as this temperature is above the Mg-Si solvus. The paper concludes that the test data obtained for 200°C, 400°C and 500°C is likely acceptable for utilization in a mathematical model where little or no precipitation is likely to occur since the test cooling rates were sufficiently fast that little or no transformation occurred, or in the case of the 500°C test, there was no thermodynamic driving force for this transformation to occur.

During the quenching process, there is insufficient time for precipitation of Mg-Si to occur since the wheel is cooled within 10 seconds. Whereas, the compression tests deformation times ranged from 6 seconds for the highest strain rate to approximately 12 minutes for the lowest strain rate. Since there are substantial differences in the thermal history between quench conditions and compression testing conditions, the compression test results in which precipitation effects have been identified to occur have the potential to be erroneous. Although there is not a high degree of confidence in all of the compression test results, this measured data (as presented in Section 4.2.2) has been adopted for use in the present model assuming symmetric tensile and compressive behaviour. In an attempt to minimize the effect of precipitation and subsequent softening, the stress-strain data input into the mechanical model in ABAQUS was limited to a maximum strain of 0.3. In the cases where a softening phenomenon was identified, only the first portion of the stress-strain curve up to the strain in which peak stress was associated was used as input for the mechanical model. Referring to Section 4.2.2, it was anticipated that the effect of hardening or softening prior to a strain of 0.1 would be negligible and therefore the error associated with this effect would be minimized. In fact, the maximum strain predicted by the mechanical model was below 0.03, further reducing the effect of precipitation.

Within ABAQUS, an elastic-plastic approach was adopted for modeling the constitutive behaviour of A356. The stress-strain data was inputted in ABAQUS in tabular form listing the plastic strain and associated flow stress for each temperature and strain rate. In defining these stress-strain data tables, the yield stress was determined using a 0.2% offset and the plastic strain was determined by subtracting the elastic strain (at 0.2% offset) from the total strain obtained from the constitutive behaviour tests. Note that the flow curve was idealized by approximating the curve by a series of linear segments for input in ABAQUS. A strain
rate dependency was implemented into ABAQUS, in which the lowest strain rate (0.001 s\(^{-1}\)) was used as the static input and the two higher strain rates (0.1 and 1 s\(^{-1}\)) were used as the rate dependencies (ABAQUS uses linear interpolation in between the strain rates provide).

6.4 Geometry

The geometry and mesh used for the stress model are identical to those described for the 180° thermal model in order to directly import the temperature-time predictions from the thermal model. Refer to Figure 6.1 for an illustration of the mesh used for this 180° section of the wheel and labelling of the relevant sections of the wheel for subsequent discussions. The mesh consisted of 4-node linear tetrahedron stress/displacement elements available within ABAQUS.

6.5 Initial Conditions

As an initial condition for the mechanical analysis, the wheel was assumed to be strain-free at the beginning of the analysis. Although, the wheel may be in some state of stress prior to heat treatment due to casting, it is unlikely that there is residual stress in the wheel after the wheel is subjected to 4 hours at an elevated temperature during the solution heat treatment and therefore a stress-free and strain-free wheel assumption is reasonable. For an initial temperature condition, the temperature throughout the 180° section of the wheel was set to room temperature (25°C).

6.6 Boundary Conditions

Boundary conditions were applied on the vertical planes bisecting the wheel to simulate the symmetry in loading assumed to present within the wheel. On these planes, displacement in the direction normal to the plane was restricted, as the thermal loads and resulting displacements were assumed to be equal and opposite. The wheel was also fixed at two nodes on the plane of symmetry in the hub section in the \(z\)-direction to avoid wheel rotation. In addition, one of the nodes used to fix wheel rotation was also fixed in the \(y\)-direction (in a radial direction) to avoid rigid body motion, while allowing the wheel to expand and contract due to changes in temperature.
6.7 Mechanical Model Predictions

The temperature-time predictions from the 180° thermal model described in Chapter 5 were employed as input for the mechanical model to simulate the quenching process conditions experienced at CAPTIN. The distribution of stresses within the wheel during quenching and a qualitative description of the mechanisms driving wheel deformation will be presented, followed by a comparison of the predicted and measured change in radius as a function of angle around the wheel.

6.7.1 Distribution of Stresses within the Wheel

The stress distribution in the axial direction was chosen for presentation of the thermal stress results to illustrate the magnitudes of the compressive (negative magnitude) and tensile (positive magnitude) stresses. The temperature distribution and the stress distribution in the axial direction (Z-direction) during the initial stages of the quench are displayed in Figure 6.2 and Figure 6.3, respectively. The evolution of thermal stress is primarily driven by the temperature gradient predicted by the thermal analysis. Figure 6.2 illustrates areas within the wheel where temperature differences are developed between the surface and the interior of the component (radially) as well as from top to bottom (axially) and around the wheel (circumferentially). During the initial stages of the quench, the surface of the wheel contracts, imposing tensile stresses at the surface and compressive stresses in the interior, as displayed in Figure 6.3. Referring to Figure 6.3, the surface stresses are relatively high (reaching 60.6 MPa) and cause in-elastic elongation due to the relatively low yield strengths of this material associated with high temperatures. At a later time as the interior section cools, the interior section of the component is restricted from contracting due to the surface layer that has undergone inelastic deformation. As a result of the inelastic elongation that occurred during the initial stages of the quench, the surface layer stress state changes to compression, while the interior section stress state becomes tensile. At the end of the quench when the wheel reaches room temperature, this produces a wheel in a state of high compressive stress at the surface (as high as -153.6 MPa) balanced by a state of high tensile stress in the interior (as high as 112.3 MPa), as illustrated in Figure 6.4. The stresses remaining in the quenched component are termed residual stresses and may cause distortion in the component, which can be problematic if too large. However, these residual stresses
can be beneficial as they can improve fatigue performance due to the compressive stresses imposed at the surface of the wheel.

To illustrate the regions of the wheel experiencing the largest strains during the analysis, the equivalent plastic strain distribution is displayed in Figure 6.5 and Figure 6.6 for different times during the simulation. During the initial stages of the quench, the largest amount of plastic strain is predicted to occur in regions of the hub and on the tip of the inboard rim. As the quench proceeds (refer to Figure 6.6), larger strains are developed in the hub area and in the areas where the spoke meets the rim section. Throughout the quench, larger equivalent plastic strains were predicted to occur in the regions with faster cooling conditions (corresponding to orientation #2 followed by orientation #3). In general, the regions with faster cooling conditions experience more plastic strain and therefore more plastic deformation, leading to higher stress in those fast cooling regions.

To show the distribution of residual stresses at the end of the analysis, the von Mises stress distribution throughout the 180° section of the wheel is displayed in Figure 6.7. The von Mises stress is an equivalent stress characterizing the stress distribution acting on an element and is a scalar quantity that can be used to indicate the intensity of the stress. This figure shows that there are varying magnitudes of residual stress dependent on both section geometry and on location around the wheel. The largest residual von Mises stresses are found in the hub and in the areas where the spoke meets the rim section. The maximum value for the von Mises stress remaining in the component at the end of the quench is 117.8 MPa for a region in the hub. As displayed in Figure 6.7, the von Mises stress distribution in the rim section, but particularly along the tip of the inboard rim shows a greater amount of stress in the locations around the wheel experiencing faster cooling rates (corresponding to orientation #2 and to a lesser amount in orientation #3). Thus the sections experiencing faster cooling rates generated the largest amount of residual stress, which is consistent with expectation.
6.7.2 Mechanisms for Wheel Deformation

The development of thermal strains and stresses within the wheel is complicated due to both the complex geometry of the wheel and to the large range of temperature gradients experienced throughout the wheel. This section will discuss how the wheel reacts in an attempt to self equilibrate resulting from the development of residual stresses within the wheel during the quench.

To better illustrate wheel deformation induced from quenching, the deformed state is superimposed onto the initial state (or undeformed state) of the 180° section of the wheel in Figure 6.8 to Figure 6.11. The undeformed wheel is indicated by dashed lines while the deformed wheel is indicated by solid lines. The magnitude of the deformation has been exaggerated by a factor of 20 to clearly demonstrate the manner in which the wheel deforms.

Figure 6.8 and Figure 6.9 displays the radial deformation pattern from a top and bottom view of the wheel, revealing that the wheel deforms in an oval-like pattern. Figure 6.8 shows the deformation pattern of the inboard rim and Figure 6.9 shows the pattern of the outboard rim. These figures indicate that more radial deformation is experienced in the inboard rim as compared to the outboard rim. In addition, the largest amount of radial deformation occurred at an on-spoke region corresponding to Orientation #1. Referring to the temperature distribution and the axial stress distributions in Figure 6.2 and Figure 6.3, it is this location (orientation #1) that has the slowest cooling conditions and is last to experience the effect of the moving front of initial tensile stresses on the surface and compressive stresses on the interior due to thermal gradients within the wheel. However, this location also corresponds to the region with less plastic strain and residual stress at the end of the analysis.

Figure 6.10 displays the wheel deformation pattern from a side view as predicted by the 180° mechanical model. This figure shows that there is relatively little vertical displacement (Z-direction) in the hub section due to the boundary conditions. However, vertical deflections were predicted at the same locations where radial displacements were predicted. The largest vertical displacement occurred on the plane of symmetry as displayed in Figure 6.11 (enhanced side view of the symmetry plane). In terms of radial deformation, the inboard rim
experienced a larger radial deflection (deflecting inwards) as compared to the outboard rim (deflecting outwards), as displayed in Figure 6.11. Thus there appears to be a neutral plane or hinge point aligned approximately with the height of the hub about which the deflection occurs. The deflection of the two rims is thus linked and would be opposite to one another. Furthermore the magnitude of the deflection in the inboard rim would necessarily be larger because is further from the hinge-point – see Figure 6.11. Figure 6.11 illustrates that as a section of the wheel is deflected inwards, it is also deflected in a upward direction (positive z-direction) and upon examination of the spoke location for orientation #1, an outwards deflection resulted in an downward vertical displacement.

6.7.3 Comparisons to Measured Data

The change in radius ($\Delta R$) was calculated in a similar manner to that described for the CMM measurements in Section 4.1.2 as follows

$$R = \sqrt{X^2 + Y^2}$$

$$\Delta R = R - R_0$$

where $X$ and $Y$ are the predicted coordinates in the $x$ and $y$ directions and $R_0$ and $R$ are the radii prior to the quench and at the end of the quench. The change in radius was calculated at spoke and off-spoke locations around the wheel within the $180^\circ$ model domain. The spoke and off-spoke locations from the model geometry were shifted to correspond with the angular locations corresponding to the correct wheel orientation - refer to Figure 4.12. The predicted change in radius is compared with the measured change in radius for the wheel located in the same position with respect to the T6 tray as the thermal measurements were performed (side position – bottom level), as shown in Figure 6.12. There appears to be reasonable qualitative agreement between predicted and measured results at angular locations between $\sim 150$ and $215^\circ$, but the predicted results do not appear to correspond very well in terms of magnitude. The reason for the poor performance of the model is unclear. Some possible explanations are discussed below.
6.8 Mechanical Model Discussion

One drawback of the mechanical analysis is the uncertainty in the constitutive data measured for A356 in its solutionized condition. The largest concern is the difference between the state of precipitation for the compression tests and for quenching conditions. During quenching, the time spent in the temperature regime in which precipitation kinetics are high is limited due to the high cooling rates associated with quenching and therefore, it is likely that little or no precipitates can form. Whereas, for the compression tests, sufficient time at elevated temperatures allowed for the formation of Mg-Si precipitates which results in an overall increase in the yield strength of the alloy. One possible explanation why the model is tending to under predict the peaks in radial deformation could be that the constitutive data input to the model, based on the compression tests, has too high of a flow stress. This increase in the yield strength would account for a slightly lower prediction of the magnitude of the change in radius.

The maximum strain rate predicted by the mechanical model for the quenching process was 0.035 s\(^{-1}\), which is lower than the lowest strain rate dependency inputted in ABAQUS (0.1 s\(^{-1}\)). At this point, ABAQUS will use the static stress-strain data inputted (data from the 0.001 s\(^{-1}\) test), which has a greater probability for precipitation to have occurred during compression testing. This poses potential problems as the model would be applying constitutive data with a higher yield stress than is actually the case, which would result in under-predictions for stress and for wheel displacements. To obtain better constitutive data for A356, more tests should be completed to obtain a better representation of constitutive responses that will be experienced during a typical quenching condition in the 250 to 350°C temperature range, which is the critical area where the precipitation kinetics are fast.

While the above arguments are sound, the constitutive data used in the mechanical analysis allows the model to reasonably predict the displacements and the behaviour at locations near the 180° angular location. Perhaps the poor agreement between predicted and measured results at angular locations below ~150° and above ~215° is more a result of poor symmetry assumptions rather than poor constitutive data. Referring to Figure 6.13, this particular wheel geometry has five spokes and cannot be oriented within the two support flanges of the
T6 tray to have a symmetric quenching condition for a 180° section of the wheel. The plane of symmetry used for the 180° model is displayed within this figure, indicating the orientation of the wheel in the tray with respect to the model geometry. The problem arises in applying the thermal boundary conditions as the model geometry plane of symmetry cannot align with a process plane of symmetry. The best agreement between the predicted and measured change in radius results was obtained near the 180° angular location (corresponding to orientation #1 and the support flange) most likely due to proper bounding with respect to heat transfer conditions applied at the two adjacent orientations. Therefore, the 180° section of the wheel may be inadequate for modeling this particular five-spoked wheel design owing to issues of symmetry, requiring a full 360° model.

Given the analysis performed thus far it is unclear whether the large deformations occurring within the wheel are due to the high quench rates occurring in the process or due to some of the differences in heat transfer observed around the circumference of the wheel, both of which contribute to differential rates of thermal contraction within the wheel. To assess the influence of the differential cooling conditions occurring around the circumference of the wheel on the evolution of radial displacements within the wheel, a second simulation was performed in which the heat transfer coefficient was uniformly applied over the entire surface of the 180° section of the wheel. The values used for the heat transfer coefficients in this second analysis were identical to the values used for orientation #2 (corresponding to the fastest cooling conditions), as presented in Section 5.3.3. Figure 6.14 compares the predicted change in radius as a function of angle around the wheel for this second analysis with the predictions from the previous analysis. It can be observed that the change in radius or the overall degree of wheel deformation for the second analysis has been significantly reduced compared to the first analysis. Although the magnitude of the change in radius for the uniform cooling condition has been significantly decreased, there is still a trend in maximum and minimum peaks for the change in radius with respect to the spoke and off-spoke locations.

The von Mises stress distribution at the end of the quench analysis has been plotted in Figure 6.15. This figure shows that there is no variation of residual stresses occurring in a
circumferential direction around the wheel. Furthermore, the magnitude of stresses predicted in this analysis is identical to those predicted in the first analysis in the orientation #2 location in Figure 6.7, as to be expected since these cooling conditions were applied uniformly over the entire surface of the 180° section of the wheel. The maximum values for the von Mises stress for the first analysis and the second analysis were 117.7 and 114.2 MPa, respectively. Although large residual stresses were predicted within the wheel for both models, there is a significant reduction in the degree of wheel deformation in the second analysis. This suggests that it is the circumferential variability in heat transfer that plays the dominant roll in overall wheel deformation and not the severity of the quench.

Based on the wheel deformation results, if the heat transfer differences around the circumference of the wheel can be minimized without compromising the benefits of the quench process, then the overall wheel deformation can be reduced and the number of rejects associated with wheel deformation can be substantially decreased, increasing the efficiency of the process at CAPTIN.

In order to reduce the thermal gradients around the wheel, a better T6 tray design could be implemented. An optimal design would allow for more uniform heat transfer in a circumferential direction around the wheel while avoiding any flow obstructions from support areas. An option is to move to a single wheel quench process where the wheel is either supported by the hub area or by support regions with smaller contact area on the inboard rim. A support system with a smaller contact area would facilitate the flow of water as compared to the two-inch angle irons used in the current design. However, a more cost effective method of reducing the extent of wheel deformation for CAPTIN's wheel manufacturing plant is to simply alter the design of the current tray. If the two supporting angle irons were replaced with two relatively small diameter steel bars located closer to the interior of the wheel, then the heat transfer conditions would be more uniform and this would reduce any local hot spots resulting from the support flange. In addition, a tray re-design with less shrouding running adjacent to the wheels would allow for more uniform distribution of heat transfer. Another option in developing a more open tray design without redesigning the current tray, would be to load the tray with fewer wheels, allowing more
water flow within the tray. However there would still be the issues related to the support flange obstructing the flow of water and creating larger thermal gradients around the wheel and it would also reduce manufacturing efficiency due to the reduced amount of wheels in the T6 tray.
Figure 6.1: 180° Section Wheel Mesh
Figure 6.2: Temperature Distribution at initial stages of the Quench (Time = 1.292 seconds)

Figure 6.3: Axial Stress Distribution at initial stages of the Quench (Time = 1.292 seconds)
Figure 6.4: Axial Stress Distribution at the end of the Quench

-112.3 MPa
+50.00
+42.00
+34.00
+26.00
+18.00
+10.00
+2.00
-6.00
-14.00
-22.00
-30.00
-153.6

Figure 6.5: Equivalent Plastic Strain Distribution at Initial Stages of the Quench (Time = 1.292 seconds)
Chapter 6: Mechanical Model

Figure 6.6: Equivalent Plastic Strain Distribution at End of Quench

Figure 6.7: Von Mises Stress Distribution at end of Quench
Figure 6.8: Top View of Wheel Deformation (Displacements Exaggerated by a Factor of 20)

Figure 6.9: Bottom View of Wheel Deformation (Displacements Exaggerated by a Factor of 20)
Figure 6.10: Side View of Wheel Deformation (Displacements Exaggerated by a Factor of 20)

Figure 6.11: Enhanced Side View of Wheel Deformation
(Displacements Exaggerated by a Factor of 20)
Chapter 6: Mechanical Model

Figure 6.12: Comparison of the Predicted and the Measured Change in Radius

Figure 6.13: Angular Location of Thermal Measurements and the 180° Model Plane of Symmetry

Figure 6.14: Comparison of the Change in Radius for the two Mechanical Analyses
Figure 6.15: Von Mises Stress Distribution at End of Quench (Uniform HTC)
Chapter 7: Summary & Conclusions

7 SUMMARY AND CONCLUSIONS

The research for this project has primarily focused on the development of a mathematical model capable of predicting the evolution of temperature and thermal strains and stresses within a die cast aluminum wheel during quenching, with the overall goal of understanding the mechanisms associated with wheel deformation and the factors that influence wheel deformation. Gaining a better understanding of wheel deformation will ultimately aid in reducing the amount rejected wheels due to off-roundness and increase the overall process efficiency.

This study required a series of industrial measurements to provide thermal history data and wheel deformation data for validation of the mathematical models. In addition, a series of laboratory-scale experiments, essential for model development, were performed to characterize the heat transfer coefficient and the constitutive behavior of A356 in its solutionized condition.

A mathematical model was developed to predict the evolution of temperature, formation of thermal stresses and resulting displacements within the die-cast aluminum wheel during the quenching stages of the T6 heat treatment operation. The sequentially coupled thermal-mechanical model was based on the commercial finite element software ABAQUS. The thermal model was validated against the industrial temperature measurements using embedded thermocouples throughout the wheel. The parameters describing the thermal boundary conditions were systematically adjusted until an acceptable agreement was obtained with the measured data. Overall, an acceptable agreement between all the various thermocouple measurement locations and the predicted temperature distribution within the wheel was obtained.

The mechanical model used the temperature-time predictions obtained from the thermal model as input for predicting the evolution of thermal strains and stresses within the wheel. The distribution of thermal stresses revealed that the quenching process induces a state of high residual compressive stress on the surface and a state of high residual tensile stresses in
the interior of the wheel, increasing the overall fatigue performance of the wheel. The local displacements predicted for a number of locations around the wheel agreed reasonably well with the measured results. This model revealed that the temperature differences occurring in a circumferential direction around the wheel have a significant impact on the distribution of residual stresses and therefore the degree of wheel deformation. Therefore, to minimize the amount of wheel deformation induced from quenching, a reduction in the differences in heat transfer around the wheel is required. An improved quenching system would allow for more uniform heat transfer in a circumferential direction around the wheel while avoiding any flow obstructions from support areas. A possible new tray design would include a more open design with less contact area and reduced flow hindrances from the supporting structure.

Both the stress and displacement predictions obtained from the mechanical analysis revealed that the thermal-mechanical model can be used as a powerful tool to predict the evolution of temperature, strains and stresses within the wheel as well as to predict overall wheel deformation and displacements. Based on an understanding of the mechanism associated with wheel deformation, the quench conditions can be optimize to reduce wheel deformation while meeting the industry standards for strength and fatigue performance.

7.1 Recommendations for Future Work

The results of this research indicate that the thermal model predictions were fairly accurate at the measurement locations; however there was no indication if the temperature distribution was accurate elsewhere within the wheel. Therefore more temperature measurements would be required to further validate the model. This would aid in determining if linearly interpolating the thermal boundary conditions is an accurate approach in estimating the temperature distribution in between the spoke locations of the wheel. Additional temperature measurements could also be made in more locations around the wheel to reveal any variations in cooling conditions at other locations around the wheel. These added measurement locations would either support or prove otherwise the assumption for the conditions of symmetry of the 180° model. It would be interesting to observe the different cooling conditions associated with the five spoke locations around the wheel. Furthermore, temperature measurements could be repeated to ensure repeatability and reliability in the
temperature measurements obtained from the quench process at Canadian Autoparts Toyota Inc.

If computational efficiency could be increased, then a full 360° thermal mechanical model could be developed. This model would be beneficial in terms of wheel deformation pattern predictions and model validation. Perhaps more accurate results for the change in radius as a function of angle around the wheel both in terms of magnitude and general shape of the curve would be obtained.

It is also recommended that more compression tests be completed to better map out the range of constitutive responses that A356 will experience during typical quenching conditions in the 250-350°C temperature range, which is the critical area where the precipitation kinetics are fast. Moreover, ultimately it will be necessary to link a microstructure model (to predict the evolution in precipitation) with the constitutive data. Therefore, the thermal and mechanical models could be weakly coupled via this microstructure model as the thermal history will influence not only the generation of thermal stresses through the temperature gradient (thermal loads) but also through the evolution in microstructure and its impact on the constitutive response of that material to the thermal loads present at anytime.
REFERENCES


References


