

Finite Element Analysis of Multi-Material Die-Cast Tooling by Additive Manufacturing

by

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Author's Declaration

I hereby declare that I am the sole author of this thesis. This is a true copy of the thesis, including any required final revisions, as accepted by my examiners. I understand that my thesis may be made electronically available to the public.

Abstract

The predominant challenge encountered in the high-pressure die casting of aluminum is the deterioration of casting dies because of thermal fatigue or heat checking. This issue largely stems from the inadequate thermal conductivity of the materials used for the dies. In this thesis, the focus is on evaluating the thermal and mechanical behavior of multi-material dies manufactured through additive manufacturing. These suggested dies are made of traditional die material and feature a shell with TPMS core, into which a material with superior thermal conductivity is infused. The investigation is conducted through computational finite element analysis (FEA) to assess the performance by comparing temperature and stress distributions withing test specimens.

The material properties of the hybrid material, which includes a TPMS made of tool steel infused with a high thermal conductivity material, were determined through the process of homogenization. Rule of mixture as well as evaluation by computational calculations were applied to derive the relationship between infill volume fraction and material properties of hybrid structure. Experimental thermal results of the hybrid specimen were compared with the computational results with using material properties derived by homogenization. Upon observing similarities in the results derived by both the approaches, homogenization results were validated. Two case studies are discussed in this work to computationally derive the effectiveness of multi-material tooling. The geometries used in both the case studies present similarities to geometries used in actual die casting experiments and/or productions. In both cases, geometries were given boundary conditions observed in actual die-casting testing and manufacturing.

Notable decreases in both the peak temperature and the temperature gradients within the die body were observed, correlating directly to the volume fraction of the infill material. Furthermore, a significant reduction in cyclic principal stresses was noted in the hybrid tooling configurations, indicating enhanced resistance to thermal cracking.

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I would like to express my deepest appreciation to Prof. Saeed Maleksaeedi for his exceptional guidance, invaluable insights, and unwavering support throughout the course of this master's thesis. His mentorship has been instrumental in shaping my research and fostering intellectual growth.

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To my parents, Rasila and Jaysukh, I am immensely grateful for your enduring encouragement, love, and the invaluable lesson you imparted – to never quit, regardless of the challenges encountered. Your resilience and belief in my abilities have been a constant source of inspiration.

I owe a special debt of gratitude to my wife, Smita, whose unwavering support, patience, and understanding have been my anchor. Her encouragement and companionship made the journey smoother, and her belief in my capabilities has been a driving force.

This thesis is a culmination of the collective support, encouragement, and teachings provided by Prof. Saeed Maleksaeedi, my parents, and my wife, Smita. Each has played an integral role in my academic journey, and for that, I extend my heartfelt thanks.

Dedication

I dedicate this thesis to my loving wife and my family, whose unwavering support and encouragement have been the guiding lights throughout my academic journey.

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List of Abbreviations

3D	—	3 Dimensional
AM	—	Additive Manufacturing
BC	—	Boundary Condition
CAD	—	Computer Aided Design
CIM-X	—	Confidential Infill Material X
CTE	—	Coefficient of Thermal Expansion
DED	—	Direct Energy Deposition
FEA	—	Finite Element Analysis
HAZ	—	Heat Affected Zone
HPDC	—	High Pressure Die Casting
LPBF	—	Laser Powder Base Fusion
MS	—	Maraging Steel
RTD	—	Resistance Temperature Detector
TPMS	—	Triply Periodic Minimal Surface
VM	—	Von-Mises (Stress)

Chapter 1: Introduction

1.1 Motivation

The materials used in high-pressure die casting need to possess specific characteristics to withstand elevated temperatures, as well as the high velocity and pressure experienced during the injection process. The most appropriate materials for high-pressure die casting (HPDC) include H11, H13, and maraging steel. These materials offer excellent thermal shock resistance, durability against abrasion, high heat resistance, and exceptional toughness [1]. Conversely, these materials exhibit notably low thermal conductivity, resulting in increased thermal gradients due to the alternating heating and cooling of the die surface during the injection and lubricant spraying process. The thermal gradient resulting from the low thermal conductivity of die materials contributes to the development of heat checking, ultimately leading to die failure. Due to lower thermal conductivity, melt contact surface of the tooling reaches elevated temperatures during melt injection. At temperatures above soldering critical temperature, soldering occurs causing melt and tool steel to form intermetallic layer leading to die failure.

Materials with higher thermal conductivity demonstrate quicker response times to abrupt heating and cooling of the die surface. Consequently, this leads to a reduction in stresses within the die caused by rapid temperature changes. Additionally, high thermal conductivity ensures effective solidification of the cast, resulting in improved surface finish, enhanced dimensional accuracy, and higher production quality. Enhanced thermal conductivity guarantees quicker dissipation of heat and accelerated solidification of the casting. Utilizing die materials with high thermal conductivity can substantially reduce cycle time and boost production rates.

Tooling composed of a bi-metallic structure with H13 cladding and a copper core has demonstrated effectiveness in reducing response time to external thermal changes [2]. While computational results suggested substantial improvements, physical testing revealed specimen failure earlier than anticipated [2]. The application of a tool steel coating on a copper substrate has been investigated for its resistance to thermal fatigue [3]. While initial findings indicated improved die performance, during extended production runs, claddings and coatings were found to be susceptible to washout, leading to a significant reduction in effectiveness over the long term [3]. A homogenized hybrid structure, which combines tool steel with high thermal conductive material, is essential for minimizing thermal fatigue and enhancing resistance to heat checking.

1.2 Thesis Objective

The objective of this research work is to investigate the impact of high thermal conductivity tooling on the performance of the die casting process and to develop a finite element computational model to evaluate the performance.

The research objective can be divided, and studies as listed below:

- Investigate the influence of tooling material on heat transfer and thermal response in die casting applications.
- Explore the potential of developing hybrid die casting tooling utilizing multiple materials to combine desirable properties, aiming to enhance die performance.
- Establish the ideal composition of tool steel and high thermal conductivity material to achieve optimal thermal and mechanical performance.

- Utilize computational methods to evaluate the performance of a finite element model of a hybrid die casting tool, aiming to optimize the tooling design for enhanced resistance against heat checking.

Chapter 2: Background and Literature Review

2.1 Die Casting Process

The casting process is considered one of the oldest manufacturing processes to produce metal components. Earlier casting process involved using gravity to force molten metal into clay or sand molds. The molds must be destroyed in this process to remove the component, known as casting. Global competition and continuous demand for producing parts at lower costs required the use of permanent molds that can be used for producing components in vast quantities.

In die casting process molten metal is injected into molds, also referred to as dies, by means of centrifugal force or by pressure such as hydraulic pressure. Dies are made of hardened steel that can withstand high temperature such as tool steel, mold steel, martensitic stainless steel or alloy that last for producing 20,000 to 250,000 parts depending on material and process parameters [1]. During the process, the injection velocity of the molten metal can range from 20 to 60 m/s, and the injection pressure can reach as high as 70 MPa; The temperature of the molten metal can vary between 700°C and 970°C, depending on the casting material [4]. The rapid velocity and elevated injection pressure of the melt takes an extremely brief duration of just 10 to 100 milliseconds to completely fill the die cavity. [5]. Due to very high injection pressure and short filling time, it is possible to manufacture complex shapes and thin-walled structures with High Pressure Die Casting (HPDC).

High pressure die casting process can be categorized into two categories as described in the following sections:

2.1.1 Hot Chamber Die Casting

The schematic illustration of hot chamber die casting process is shown in Figure 1 [6]. In hot chamber die casting process, the melt injection system is submerged into molten metal bath [7]. Hydraulic cylinder actuation pushes melt into die cavity through gooseneck attached to it. Because the molten metal has a shorter distance to travel, the entire cycle time is notably brief. Hot chamber process is considered rapid operation with cycle time ranging from less than 1 sec for small parts of few grams to 30 sec for parts of few kilograms [6]. Hot chamber die casting process is generally most suitable for metals with lower melting point, such as lead or zinc alloys [8]. Metals with higher melting point can cause premature damage to the injection system and may need frequent repair or replacement.

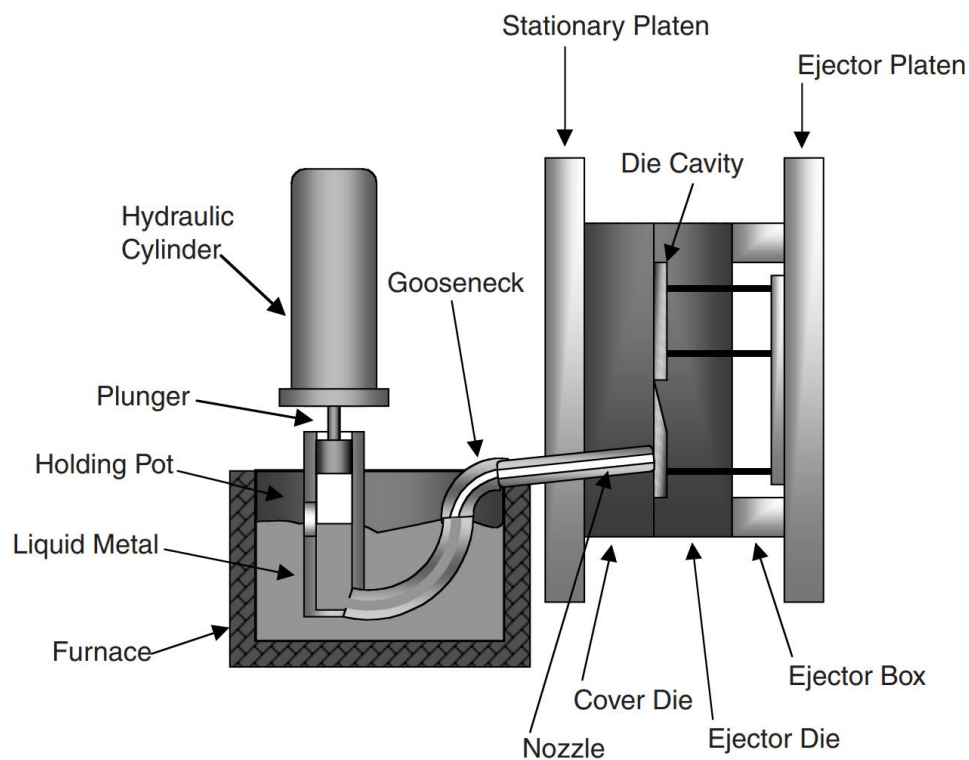


Figure 1: Schematic of hot chamber die casting process [6]

2.1.2 Cold Chamber Die Casting

The schematic illustration of cold chamber die casting process is shown in Figure 2 [6]. In contrast to the hot chamber process, the injection system is not submerged in molten metal. The required quantity of molten material for a single casting is conveyed to the injection system's shot sleeve using a ladle, either through manual operation or by automated means. The injection system spends significantly less time in contact with molten metal compared to the hot chamber process, resulting in lower repair costs for the injection system [6]. The hydraulic cylinder actuation seals off the input opening of the shot sleeve and exerts high pressure to force the molten material into the die cavity. It then continues to maintain this pressure throughout the solidification process. Injection pressure in cold chamber process is very high compared to hot chamber process [9]. Cold chamber process is most suitable for casting metals with higher melting point such as aluminum, copper, and magnesium [10].

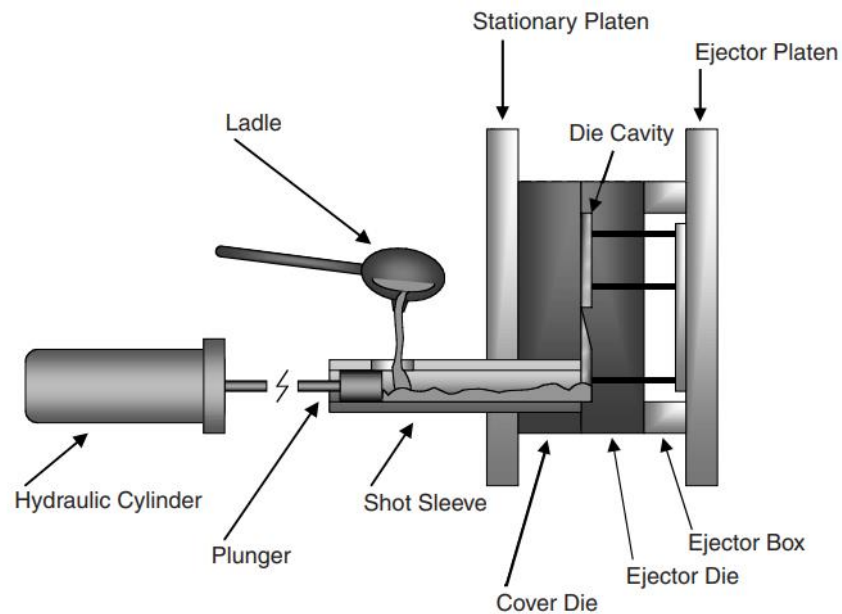


Figure 2: Schematic of cold chamber die casting process [6]

2.1.3 Advantages of High Pressure Die Casting

Some of the key advantages that high pressure die casting offer are listed below:

- **Higher Production Rate:** The production rate achieved by high pressure die casting is significantly higher than the same in gravity casting or low-pressure casting [1].
- **Close tolerance manufacturing:** Very close dimensional tolerances are possible in high pressure casting. This results in much lesser time and money spend in machining operations [1].
- **Better surface finish:** Excellent surface finish is accomplished in casting, and it is possible to produce textured surface with this casting process [11].
- **Thin-walled structure:** Manufacturing of castings with thin wall features is feasible with high pressure process, hence this process is considered near net shape casting process [12].
- **Complex parts:** Casting parts with higher design complexity can be manufactured with this process, therefore number of components in assembly can be reduced [1].

2.1.4 Disadvantages of High Pressure Die Casting

Following are some of the disadvantages of using high pressure die casting process:

- **Porosity:** Due to high injection velocity, air entraps into the casting and results in porosity in finished part [11].
- **Non heat treatable:** Due to porosity presence in casting, heat treatment or welding of casting is very restricted [1].
- **Limited Metal:** This process depends on high flowability of metal and hence is limited to very few high fluidity materials [11].

- **Higher maintenance cost:** High pressure casting machine initial cost as well as maintenance costs are notably higher due to the machine complexity as well as requirement of higher pressure [1].

2.1.5 Casting Alloys

The following table (highlights some of the metals and alloys that can be manufactured using high pressure die casting method:

Table 1: Common casting materials in high pressure die casting

Metal and Alloys	Advantages	Applications	Melting Temperatures
Tin Alloys [13]	Low melting point High fluidity	Bearings Gears, wheels, and weights Postage meter print wheels	170 – 230 °C
Lead Alloys [14] [15]	Very little shrinkage at solidification Resistance to attack by mineral acids Easy to cast Excellent die life	Storage battery parts Fire Extinguisher parts Low-cost bearings Governor Weights	≈327 °C
Zinc Alloys [16]	Excellent mechanical properties Economical advantage Easy post-processing	Parts and frames of industrial machines Scientific equipment Domestic appliances Automotive applications Bearings	375 – 493 °C
Aluminum Alloys [17]	Low density with high strength Resistance to oxidation and corrosion High thermal and electrical conductivity Ease of post-processing	Automotive components Aerospace components Light weight applications Structural components Dairy and food handling equipment	557 – 657 °C
Magnesium Alloys [18]	Good castability Good balance of strength and ductility High specific strength Light weight components	Automotive industries Aviation Computer, communication, and consumer electronics	360 – 685 °C [19]

Copper alloys [20]	High strength and hardness Good wear resistance Good thermal and electrical conductivity	Plumbing hardware, pump parts, valves, and fittings Bearings and bushings Gears Marine Castings Electrical components	812 – 1180 °C
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Some of the components casted with high pressure casting process are illustrated in Figure 3 below:



Figure 3: High pressure die casted components [21]

The Figure 4 below shows die casting tooling.

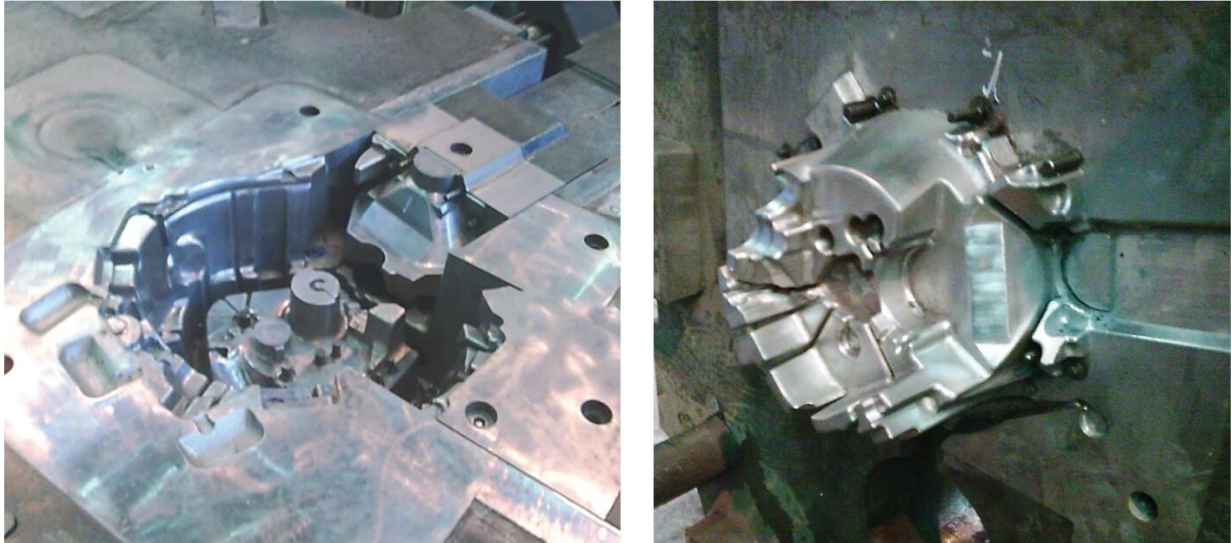


Figure 4: Die casting die halves [22]

2.2 Issues with Die Casting

Dies of high pressure die casting fail over the time due to complex combination of various wear and failure modes. Some of the most common failure modes of dies are listed below [1]:

- Erosion or abrasive wear
- Corrosive wear and chemical attack
- Soldering
- Thermal fatigue

2.2.1 Erosion or abrasive wear

Erosion in die casting is defined as the wash out of the tooling material from the die surface due to impact of melt at very high velocity during injection process [23]. The Figure 5 below shows abrasive wear in die of permanent mold casting.



Figure 5: Erosive wear in die [24]

The maximum erosion is mainly observed in the gating system as its in direct contact with the melt flow. Due to near net shaping possible with die casting, most dies have complex features such as core, pins, ribs, or corners. These features are susceptible to abrasive wear by melt flow. Continuous wash out causes die to lose its dimensional tolerance and requires repair of the die. Abrasive wear rate is a function of melt impact velocity and the angle of melt jet with die surface [23]. Factors such as gating design, casting parameters as well as die material properties can control the abrasive wear rate [1].

2.2.2 Corrosive wear

In die casting, corrosive wear can be described as the dissolution of die material in the molten metal of casting [25]. The Figure 6 below shows corrosive wear in tool steel die.

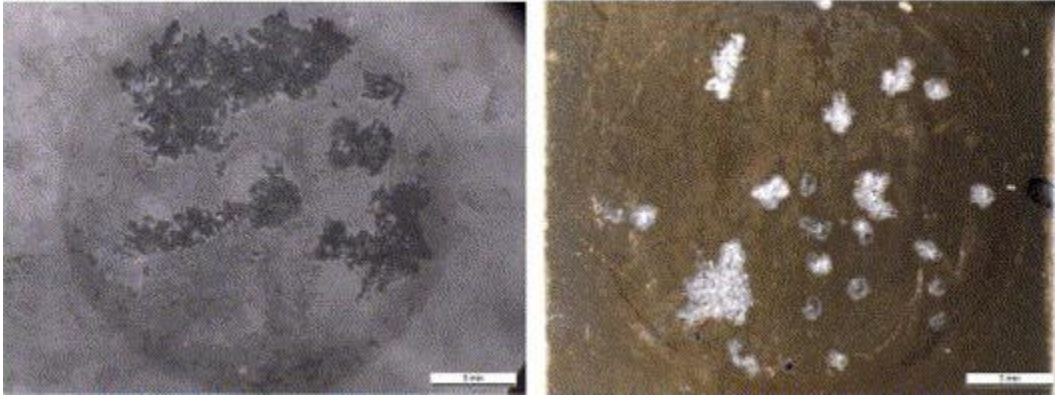


Figure 6: Corrosive wear in die [26]

The most contributing factors for corrosive wear in dies can be listed as below [25]:

- **Dissolution of die material:** Iron and many alloying elements of steel are dissoluble in molten metal such as aluminum [1]. Experimentally, significant surface dissolution was observed when a piece of die steel was placed in bath of molten aluminum. Even higher dissolution rate was detected when melt was stirred [1].
- **Melt Temperature:** The corrosive wear of the die is proportional to the temperature of the melt. Higher corrosive wear was reported with the increase in the melt temperature [27]. The Figure 7 below shows the relation of corrosive wear of H13 quenched and tempered die steel with melt temperature after an exposure for 4 hours in melt [27].
- **Intermetallic layer:** Formation of intermetallic layers on the die surface advances the corrosive wear of die material [25].

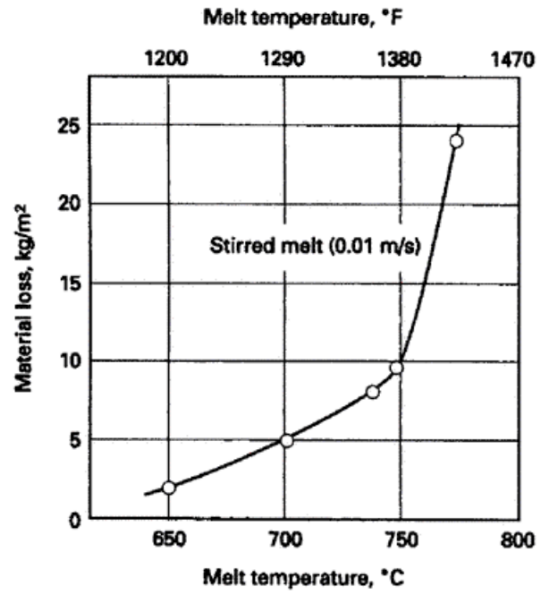


Figure 7: Corrosive wear vs melt temperature [27]

2.2.3 Soldering

Soldering, also referred to as die sticking, is an occurrence of molten metal bonding to the die surface [28]. Soldering reduces the surface finish of the casting resulting in mismatch in assembling, excessive post processing and even scrap of the component. Dies need to be repaired and recoated before putting it into operations causing down time and loss of production. Small features such as thin walls, core pins, ribs, and recesses are more prone to developing soldering defects. The Figure 8 below shows soldering in die.



Figure 8: Soldering on a die casting core pin [29]

High injection velocity of the melt is accounted for the washout of the protective layer or lubricant from the die surface leaving the die steel exposed to the melt [30]. Casting material such as aluminum reacts to the exposed die steel and forms intermetallic layer on the die surface. Soldering is more likely to occur when the die surface temperature exceeds certain limit known as soldering critical temperature [31]. Soldering critical temperature depends on the elements present in the casting alloy as well as the in the die steel. Die surface can be coated with proper coatings containing Titanium or Chromium that can increase the soldering critical temperature and prevent soldering [31]. Since soldering is dependent on the die surface temperature, attempts can be made to reduce the die surface temperature by optimizing cooling channels or by implementing thermal management in tooling.

2.2.4 Thermal fatigue or heat checking

Die materials for high pressure die casting must have certain properties to encounter elevated temperatures as well as high velocity and pressure during injection. Most suitable die materials for HPDC are H11, H13, and maraging steel. These materials provide high resistance to thermal shock, abrasion resistance, heat resistance as well as high toughness [1]. On the other hand, these materials suffer from very low thermal conductivity, consequently higher thermal gradient is observed due to alternative heating and cooling of die surface during injection and lubricant spraying process. Yield strength also reduces at elevated temperature for most die materials as shown in Figure 9.

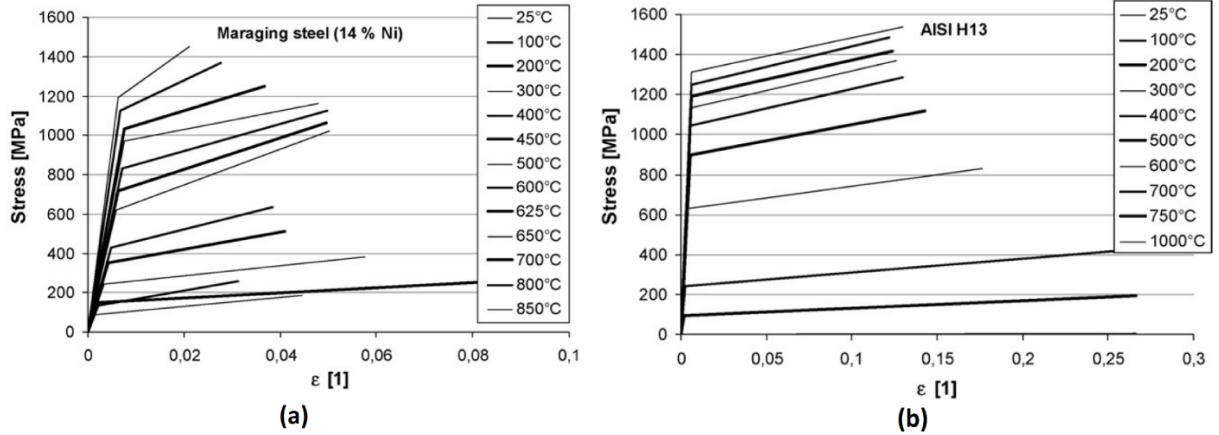


Figure 9: Yield strength of (a) Maraging steel and (b) H13 as a function of temperature [32]

Die surface goes into sudden heating, also known as thermal shock, during melt injection process. Since heat is not quickly able to flow to the interior body of the die, stress gradient is observed, and maximum stress is detected at the surface of the die causing local yielding [33].

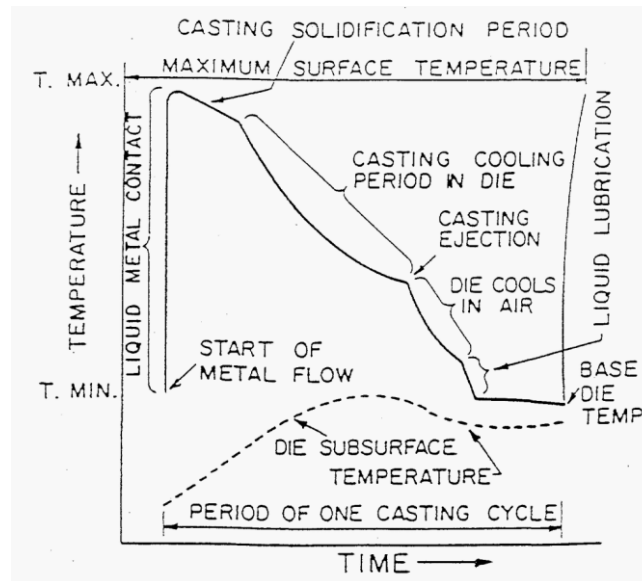


Figure 10: Temperature profile within die over the casting cycle [34]

The temperature profile at the melt contact surface as well as at subsurface of the die over the casting cycle is shown in Figure 10. It is evident from the Figure 10 that, due to lower thermal conductivity of tooling material, the temperature at the melt contact surface reached

extremely high in very short time at the beginning of casting cycle. Meanwhile, at subsurface just below the melt contact surface, the temperature remains very low, and it requires more time for temperature to increase at subsurface. This extreme difference in temperature within tooling die produce localized stresses above yield strength at the melt contact surface.

In addition, subsequent heating and cooling of the die surface leads to hysteresis loop that can be illustrated as in Figure 11 [33]. Point A in Figure 11 (a) represents die surface while point B represents interior of the die material. At the beginning of the casting cycle both points A and B are at low temperature. Upon melt injection, point A on die surface reaches very high temperature in very short time and expands against the rest of the die material and goes into compression as shown by *upshock* OQ in Figure 11 (b). Eventually, due to gradual heat flow to the die body, the whole die goes into expansion and hence point A goes into tension as shown by QR in Figure 11 (b). During cast solidification tensile stress at point R is recovered to some extent. Once the cast is removed, die surface is sprayed with coolant and/or lubricant causing temperature to suddenly drop at the surface and subsequently causing *downshock* as shown by RS. Point S represents peak tension. The surface point A yields under residual and peak tension. Once the entire structure cools down, the point A goes into compression by the surrounding material represented by SP. At initial strain level, residual compression stress is developed at point P. Further casting cycles repeat PQRS as closed hysteresis loop, causing initiation and propagation of cracks due to thermal fatigue.

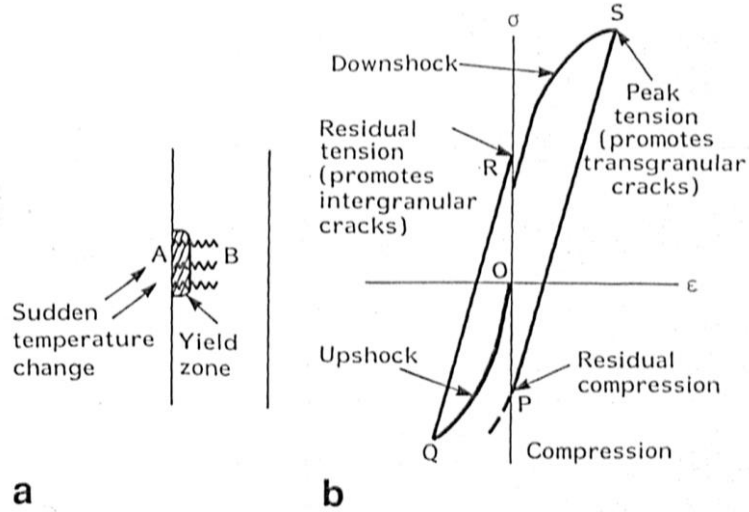


Figure 11: (a) Heating and cooling of die surface, and (b) hysteresis loop [33]

Crack initiation and propagation under thermal cyclic loading such as in die casting can be divided into four stages as below [35]:

- Crack initiation at the surface
- Crack linking at the surface
- Small crack growth in depth direction within crack net
- Complete failure by growth of largest crack

Thermal stress produced in tooling die can be calculated by the equation below [34]:

$$\sigma_{th} = \frac{E\alpha\Delta t}{1-\nu} \quad (Pa) \quad (1)$$

Where, E = Young's Modulus (Pa)

α = Coefficient of thermal expansion ($^{\circ}\text{C}^{-1}$)

Δt = Temperature difference in the tooling ($^{\circ}\text{C}$)

ν = Poisson's Ratio

Based on the equation above, it is evident that using materials with lower values of Young's Modulus or Coefficient of thermal expansion can reduce the thermal stresses produced in tooling. Reduction in temperature difference within die can also reduce the thermal cyclic stresses during casting cycle. Various approaches to achieve reduction in thermal stresses are discussed below:

2.3 Strategies to Reduce Thermal Fatigue

In order to prevent the dies from failing due to one or more of the failure modes discussed above, following strategies can be utilized and economical life of the tooling can be increased.

2.3.1 Geometric considerations

Thermal gradient and stress development can effectively be reduced by implementing certain design considerations while designing dies. Some of the key design considerations for better die life are as below:

- Implementing a well-designed gating system to maintain a safe injection velocity of the melt and the correct injection angle towards the die surface, effectively preventing die erosion.
- Die must not be too shallow and must have enough bulk material to provide sufficient strength and robustness [1].
- Sharp corners and rough contours should be avoided or provided with fillets to prevent localized stress concentration.
- Small features such as ribs, thin walls or small holes should be avoided, if possible, to avoid die sticking in those areas. In addition, proper draft angle should be provided for ease in casting removal.

- Cooling lines should not be too close or too far from the die surface to prevent thermal shocking and ensure proper solidification of the casting.

2.3.2 Die material considerations

For die to perform well throughout its production life, die materials must be resistant to abrasive wear as well as resistant to heat checking. Material for dies should have the ability to be heat treated to produce required microstructure suitable for casting. Good machinability and ability to be polished ensures die manufacturing as per design and has optimized performance. The chemical composition of the die material should align with the material being casted. For instance, incorporating alloying elements like Manganese, Molybdenum, Cobalt, and Chromium into Aluminum product casting dies has been shown to decrease the overall thickness of intermetallic layers and mitigate issues related to die sticking [30]. In addition, die material must have high thermal conductivity to ensure quick response to external thermal loading and reduce thermal gradient. In summary, the die material needs to possess a well-balanced combination of necessary thermal and mechanical properties [36].

2.3.3 Interface considerations

Treating the die surface and optimizing the die-melt interface can substantially mitigate the impact of various failure modes. Water based lubricant spray are widely used in the casting industries. Applying lubricant spray on the die surface between consecutive casting cycles speeds up heat dissipation, improves melt flow, facilitates easy cast removal, and helps prevent die soldering. In summary, employing lubricant spray extends the die's longevity while enhancing production through reduced cooling time, and achieving a better part finish is attributed to enhanced melt flow.

Various coatings and surface treatments can be carried out on die surface to reduce soldering and erosive wear. Metallic coatings of Tungsten, Platinum or Molybdenum has shown improvement in thermal cracking resistance [37]. In oxidizing environment, Tungsten and Molybdenum coatings are prone to oxidation and form oxide layers. After oxidation, molybdenum has shown poor resistance to wash out in production environment, while Tungsten has maintained its effectiveness. Arc deposited coatings were studied to further improve die performance. Improvement in tribological behaviour was observed by using TiAlN, TiN/TiCN and CrN coatings on die surface [38]. These coatings offer toughness, resulting in improved resistance to wear, and they also demonstrate strong adhesion properties. Although, during thermal cyclic loading, these coatings appear to fail prematurely. Duplex and multi-layer coatings have shown good benefits by applying thermal barrier oxide coatings of rare metals [39]. By utilizing this multilayer coating approach, there was a substantial reduction in both the number and depth of cracks observed in the test specimen [39].

Shot peening treatment is another surface treatment carried out on die to reduce erosive wear and thermal fatigue. In this treatment, steel shots are impacted on the die surface at very high speed to produce compresses and dense surface. Development of compressive stress on surface improves thermal fatigue resistance and thermal cracking. Through the application of shot peening, fatigue strength can be increased by two to three times, and wear resistance can be enhanced for both new and existing dies [40].

2.3.4 Die preheating

During the initial stage of the casting cycle, when the melt is injected, the die surface experiences thermal shock due to the significant temperature difference between the cold die

and the hot molten metal. Crack initiation can occur as early as first casting cycle and escalate to complete die failure. To minimize thermal shock, the die can be preheated to a specific temperature. The most effective preheating temperature depends on casting material.

The following table highlights recommended preheating temperature based on cast materials [1]:

Table 2: Die preheating temperature [1]

Casting material	Preheating temperature	
	°C	°F
Tin and lead alloys	100-150	210-300
Zinc alloys	150-200	300-390
Magnesium and aluminum alloys	250-300	480-570
Copper alloys	300-350	570-660

2.3.5 Bi-metallic tooling

Employing a combination of two distinct materials to leverage the superior properties of each, aiming to reduce thermal fatigue and cracking, represents a contemporary strategy within the die casting industry. Copper alloy molds demonstrate exceptional efficacy in plastic injection molding. However, their inability to withstand the abrasive nature of liquid melt restricts their direct usage in high-pressure die casting. Tool steel cladding directly deposited on a copper alloy substrate has been tested for its efficiency and resistance to thermal fatigue [3]. Direct deposition of H13 tool steel on copper alloy substrate is compared with buffer layer of high nickel content

316 SS between H13 and copper alloy [3]. Directly deposited H13 coating became vulnerable after crack formation while buffer layer improved the resistance to thermal fatigue of the coating [3].

In a separate study, different copper proportions in composite tooling test specimens through both computational simulations and practical experiments was investigated [2]. The Figure 12 below shows cross-section of 4 different test specimens for this test where shaded region indicated copper.

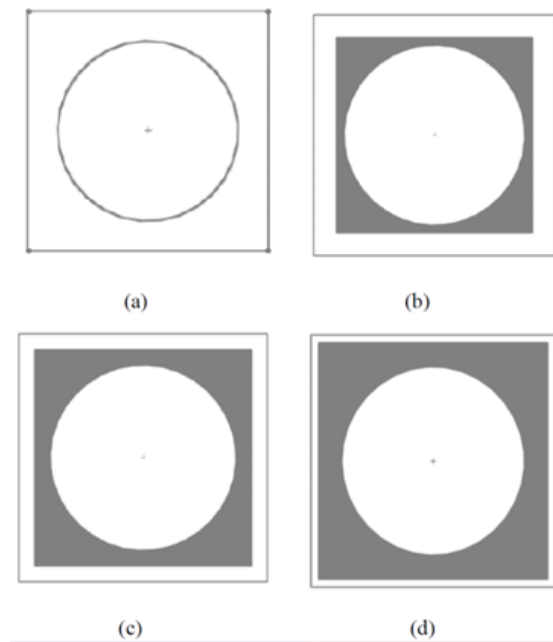


Figure 12: Cross-section of test specimen (a) Full H13, (b) 25% Cu, (c) 50% Cu and (d) 75%Cu [2]

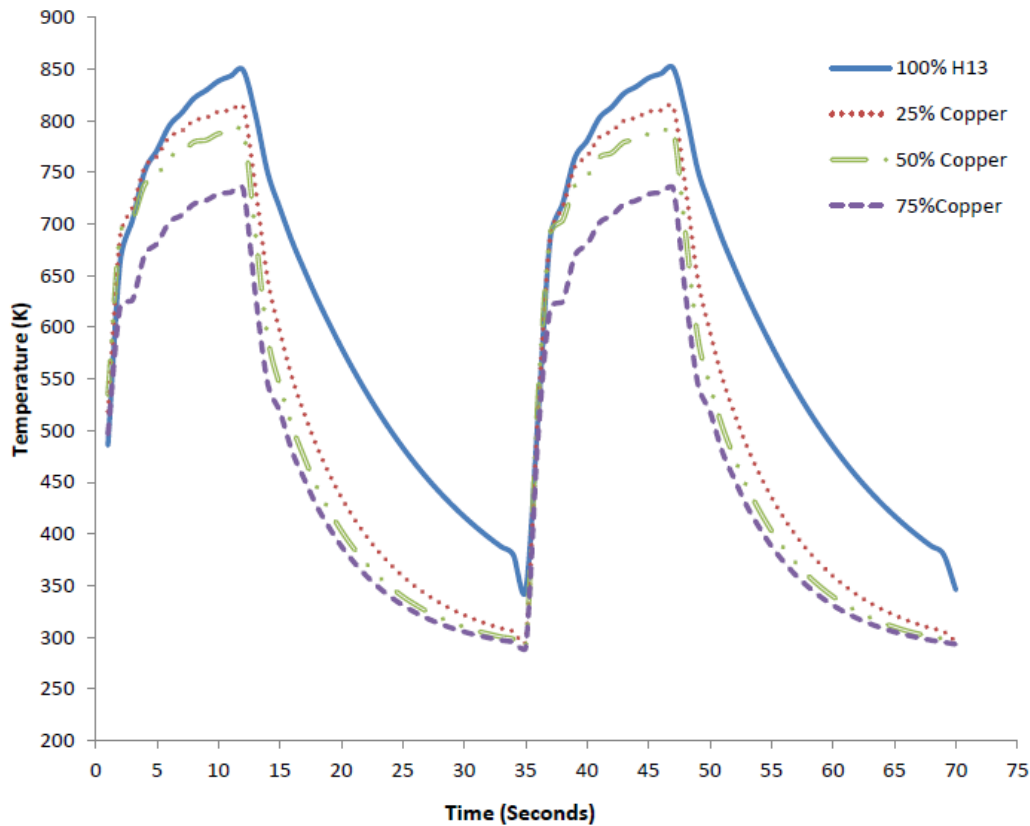


Figure 13: Maximum temperature at the corner of the specimen for two thermal cycles

As shown in Figure 13, adjusting the copper content proved beneficial in optimizing thermal performance and extending the fatigue life for this test specimen geometry. The optimized geometry led to a reduction in the maximum die surface temperature and extended the duration for which the die could withstand thermal cycles.

Bi-metallic tooling with highly thermal conductive core can reduce thermal gradient and improve response time to external temperature change resulting in better die life.

2.3.6 Conformal cooling

Unlike the conventional method of creating cooling channels by drilling straight holes in the die, additively manufactured dies with conformal cooling, evenly enveloping the die material

and enhancing the cooling process. Figure 14 below illustrates the comparison between conventional and conformal cooling channels in die.

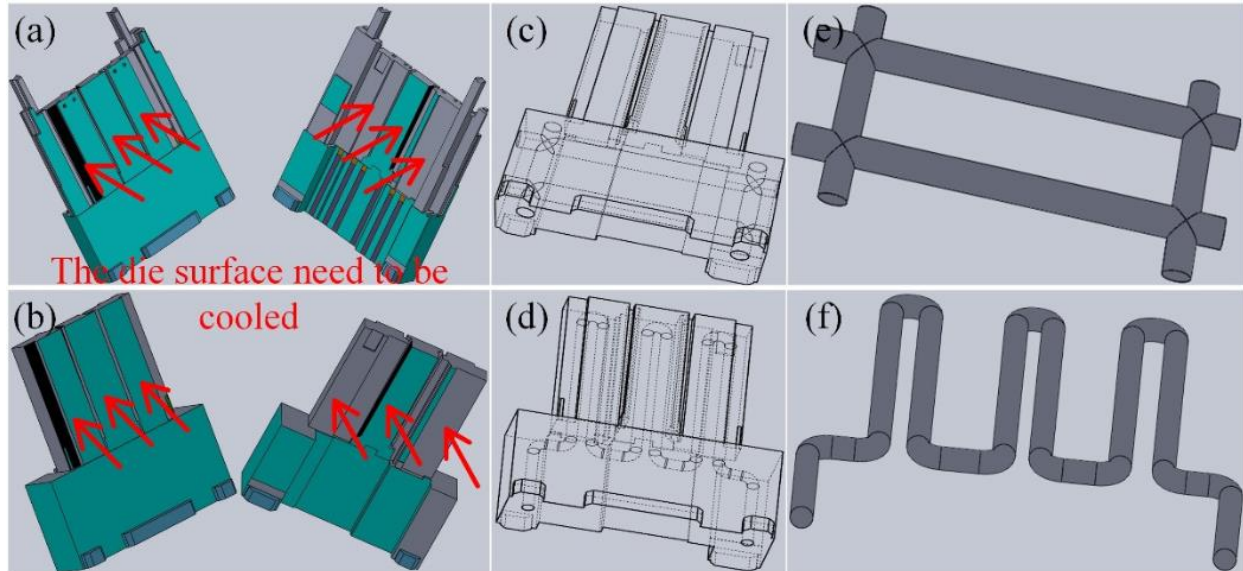


Figure 14: Die cooling by (a,c,e) Conventional channels and (b,d,f) Conformal channels [41]

Enhanced cooling rate and minimized shrinkage defects achieved through conformal cooling contribute to an overall improvement in the product's quality [42]. Reduced need of spray cooling improves production rate. Conformal cooling channels can be distributed closer to the die surface resulting in direct and efficient cooling. Temperature gradient in the die can be significantly reduced with uniformity in cooling [43]. By controlling cooling channel spacing, channel diameter and flow rate, solidification rate can be controlled. Optimization of cooling channel with automation can improve thermal fatigue resistance and reduce thermal cracking.

2.4 Additive Manufacturing

Additive Manufacturing (AM), often referred to as 3D printing, that consists of variety of manufacturing processes that fabricate parts from CAD files by adding material in a layer-by-layer

fashion. In contrast to traditional manufacturing (subtractive manufacturing) methods that involve removing unwanted material from the raw stock, additive manufacturing involves adding the necessary material to form the part. The disparity between these two processes is depicted in the accompanying Figure 15.

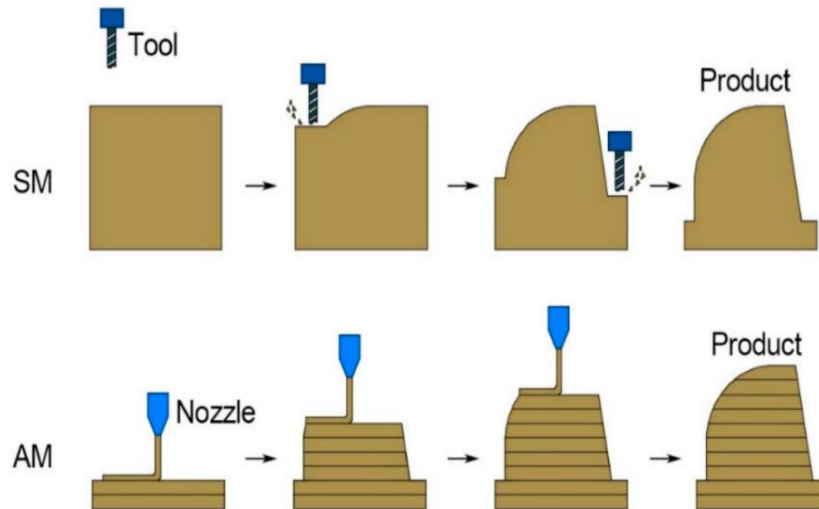


Figure 15: Subtractive vs additive manufacturing [44]

Polymers, ceramics, and metals parts are widely being manufactured with various additive manufacturing processes. The choice of an additive manufacturing (AM) process is contingent on the materials to be manufactured, in addition to the part size and the specified tolerances. Out of numerous additive manufacturing processes, the following are listed and discussed as the two most commonly utilized metal additive manufacturing processes in die casting:

1. Directed Energy Deposition (DED)
2. Laser Powder Bed Fusion (LPBF)

2.4.1 Directed Energy Deposition

During the directed energy deposition process, the directed energy melts the material while it is being deposited onto substrate. Energy from the heat source is focused onto narrow region of the substrate causing the substrate to melt and concurrently the material being deposited. The graphical representation of DED process is displayed in Figure 16.

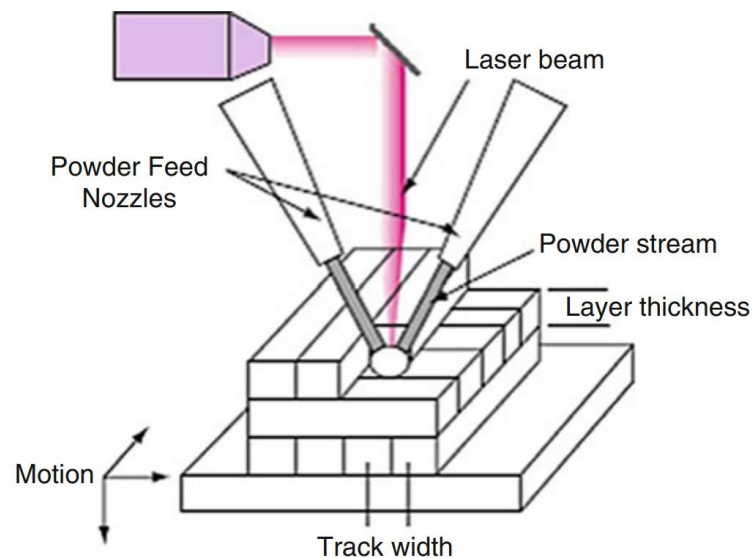


Figure 16: Directed energy deposition process [45]

Deposition head consists of power source, material feeding mechanism, inert gas system and sensors. Typically, standard Directed Energy Deposition (DED) machines operate with 3-axis motion. However, incorporating a rotary table or a robotic arm can extend the system to 4- or 5-axis, tailored to the complexity of the part and production rate. Heat source in DED process can be laser beam, electron beam or electric arc depending on the deposition rate, size and accuracy of the part and cost of the power source [46]. Material in-feed can be either in powder form or in wire form. Part manufacturing takes place in either vacuum or in inert gas atmosphere to prevent oxidation in melt state.

2.4.2 Laser Powder Bed Fusion

In laser powder bed fusion, a laser beam is used as a heat source to selectively melt specific areas of the powder bed, as depicted in Figure 17.

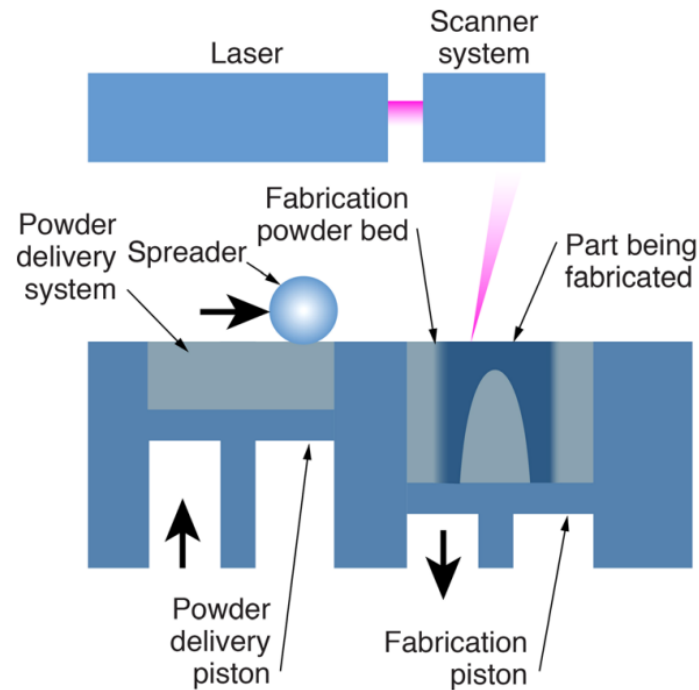


Figure 17: Laser powder bed fusion process [47]

Material in form of fine powder is supplied to the bed by means of vertical movement of powder delivery piston. Powder supplied by powder delivery system is spread uniformly onto bed with the help of roller of spreader blade. Laser scanning system focuses laser to melt desired area on the powder bed. After completing one layer, fabrication piston moves vertically downward lowering the powder bed upon which new powder layer is spread and process is repeated until completion of the part. Part is manufactured in vacuum or inert gas environment to prevent oxidation of the part.

2.4.3 Importance of AM in Die Casting

The importance of additive manufacturing in die casting is highlighted by the discussion of various contributing factors, which are outlined below:

- **Cost:** In cases of extensive production runs, employing die casting or injection molding can be a cost-effective approach. Conversely, for smaller production volumes that demand increased complexity and customization - such as casting dies - additive manufacturing proves to be a cost-effective manufacturing technique [48]. Additive manufacturing provides a heightened degree of customization and accommodates intricate parts without incurring additional costs. In addition, additive manufacturing utilizes less material compared to conventional manufacturing processes to produce the same die.
- **Design/Manufacturing flexibility:** Additive manufacturing, commonly referred to as rapid prototyping, enables swift modifications in design and provides manufacturing versatility for customization [48]. Design alterations can be quickly incorporated into the manufacturing process compared to alternative manufacturing methods.
- **Conformal cooling:** Additive manufacturing offers the incorporation of conformal cooling channels in die manufacturing which would not be possible with conventional manufacturing processes. The capability of additive manufacturing to create complex internal structures enables the customization of cooling channels to fit the shape of the die inserts [42]. Conformal channels ensuring uniform part cooling can lead to a reduction in cycle time and an increase in production rate.
- **Die Repair:** Die failure, often caused by crack formation on the die surface, is a common occurrence in die casting, leading to costly replacements. Additive manufacturing processes

like Directed Energy Deposition (DED) can be utilized to repair cracks on the die surface. High precision, narrow melting and narrow Heat Affected Zone (HAZ) in DED process prevent part distortion [49]. The free formability provided by the Directed Energy Deposition (DED) process not only enables cost and time-effective repairs but also simplifies the addition of new features or modifications to existing dies.

- **Bi-metallic tooling:** In additive manufacturing, applying a tool steel coating to materials with exceptional thermal conductivity enables the formation of bi-metallic tooling, a capability not achievable through conventional manufacturing methods. Bi-metallic tooling with AM combines the advantages of two materials to extend tool life and casting quality while achieving higher production rate [2].
- **Multi-material manufacturing:** Multi-material tooling with complex geometry can effectively be manufactured by more elaborated LPBF process to utilize useful characteristics of different materials in the same tooling [50]. The working principle and step-by-step procedure to print components with two different materials is shown in the Figure 18. The process can be summarised in six steps as below [50]:
 - a) 1st material powder being spread on the worktable with the help of roller.
 - b) Laser selectively melts the regions of 1st powder.
 - c) Micro vacuum sucks out the unwanted powder of 1st material.
 - d) 2nd material powder is dispensed by ultrasonic vibration assisted dispensers.
 - e) Laser selectively melts the 2nd material regions.
 - f) Worktable is lowered and these steps are repeated to subsequently print next layer.

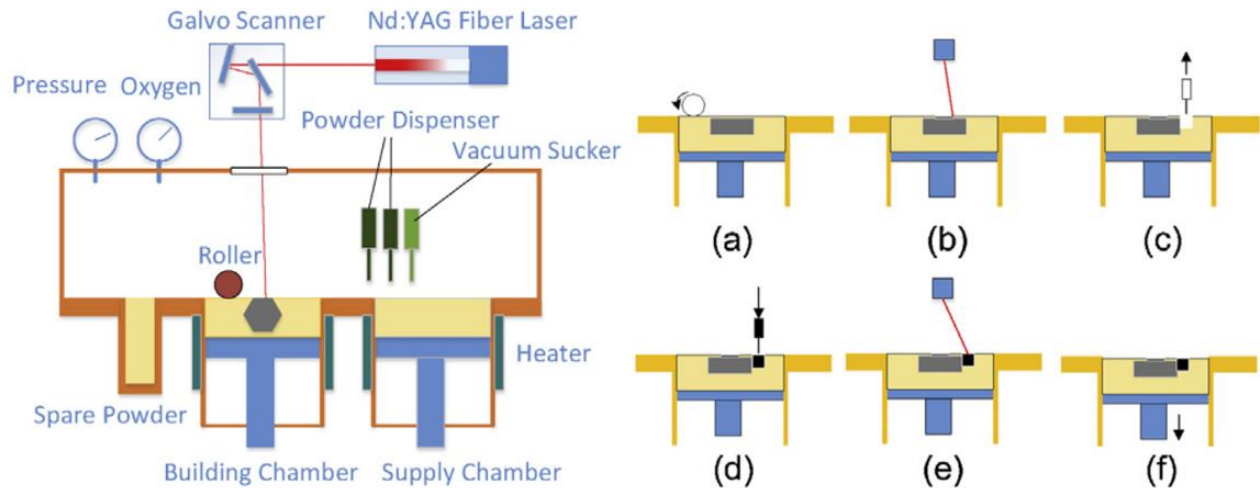


Figure 18: Multi-material Printing with modified LPBF [50]

2.5 Maraging Steel

2.5.1 Maraging Steel in Additive Manufacturing

During additive manufacturing, maraging steel can experience age hardening by precipitation of phases other than carbides [51]. Due to the low content of carbon, maraging steel are hardened by intermetallic precipitation rather than carbide precipitation. During the aging process of maraging steel, the development of clusters with diverse compositions of Ti, Ni, Al, and Mn becomes apparent after just 5 seconds of aging, leading to noticeable age hardening within a brief time frame [52]. In the process of additive manufacturing, as the laser moves to print the second layer, it achieves a near-melting state for the initial layer of maraging steel. This controlled temperature fluctuation prompts the onset of intermetallic precipitation throughout the additive manufacturing process, thus diminishing the necessity for supplementary heat treatment procedures.

Maraging steel created through Additive Manufacturing techniques like Directed Energy Deposition has demonstrated increased yield strength and higher ultimate tensile strength compared to conventionally manufactured part [53]. This is due to the laser's precise concentration on a tiny area, resulting in the rapid solidification of a small molten pool as the laser moves away, thereby generating an exceptionally fine microstructure.

2.5.2 Maraging Steel in Die Casting

Maraging steel is widely used in die casting due to high strength and toughness, specially at elevated temperature. Maraging steel, thanks to its low carbon content and the inclusion of titanium, exhibits enhanced resistance to various forms of embrittlement [54]. Compared to other tool steels such as H13, Maraging steel has higher thermal conductivity and lower linear coefficient of thermal expansion [32]. As a result, less thermal stresses are produced during cyclic thermal loading in maraging steel inserts. Hence, maraging steel exhibits better thermal fatigue resistance. Maraging steel is heat treatable alloy and therefore desired properties can be obtained by performing various processes such as hot-working, aging, heat treating, tempering and so on. For the reasons outlined above, maraging steel is extensively utilized in the die casting industry.

Chapter 3: Material Properties of Hybrid Tooling

3.1 Materials and Methods

3.1.1 Strategy

The performance of the suggested hybrid casting tool will be assessed through finite element analysis by solving a computational model subjected to real-life thermal cycle. Furthermore, finite element analysis will be employed to assess the integrated thermal and mechanical properties of hybrid material tooling. Two case studies with distinct but significant geometries to represent die casting manufacturing process will be explored for the effectiveness of hybrid tooling.

To begin with, a range of tool steel and high thermal conductivity material compositions will undergo testing through finite element analysis. This assessment aims to establish the correlation between material compositions and the overall properties of hybrid tooling. Subsequently, a computational model of the hybrid tooling will be exposed to cyclic thermal loading to replicate real-world casting conditions and will be computationally compared to the performance of existing tooling. Key parameters, including the maximum temperature on the die surface, thermal gradients within the tooling, and the temperature distribution on the die surface, will be gathered and analyzed. Additionally, the thermal data will be employed as input for mechanical simulations, in which the tooling will be constrained as per real-world casting conditions. During the thermal cycling, the thermal expansion and contraction of the constrained tooling die will generate both compressive and tensile stresses that will be collected and compared with standard tooling. A Computer-Aided Design (CAD) tool, such as SolidWorks, will

be employed for designing the tooling geometry, while a Finite Element Analysis (FEA) tool, such as Ansys, will be utilized to computationally determine the temperature and stresses within the tooling [55] [56].

The hybrid tooling, featuring enhanced thermal conductivity, is anticipated to lower the maximum temperature and decrease thermal gradients within the tooling. This is expected to lead to quicker responses to thermal loads and reduced stress within the tooling. Furthermore, the hybrid tool is expected to exhibit improved resistance against heat checking.

The production of a hybrid tool, which falls outside the focus of this research project, will be carried out using additive manufacturing and infiltration process. A tool insert featuring a shell of the desired thickness and a Triply Periodic Minimal Surface (TPMS), commonly referred to as an isotropic lattice, can be generated through additive manufacturing and tool steel as material. In Figure 19, this method illustrates the TPMS cavity infused with a material possessing enhanced thermal conductivity.

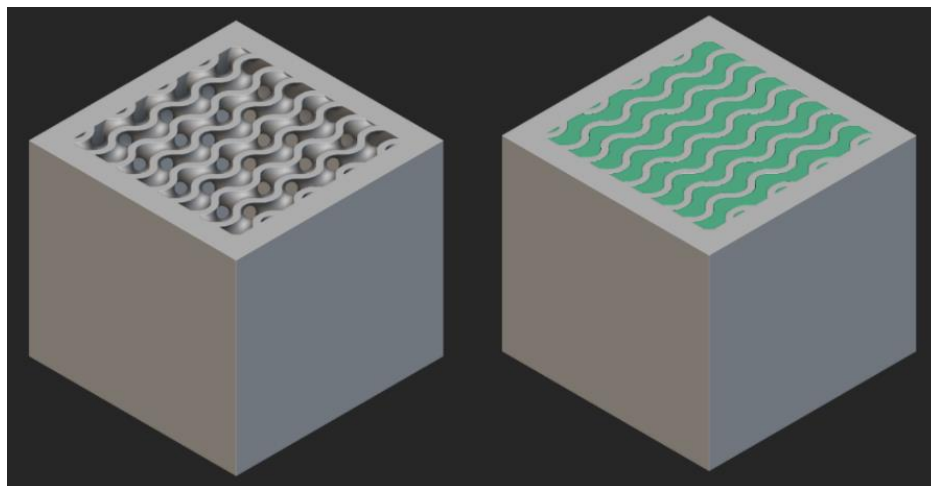


Figure 19: Hybrid tool manufacturing with Additive Manufacturing and infiltration process

3.1.2 Properties of Constituent Materials

Material properties of maraging steel and Confidential Infill Material X, referred to in this work as CIM-X, are obtained from industrial partner, *Exco Engineering*, and are shown in graphs below [57]:

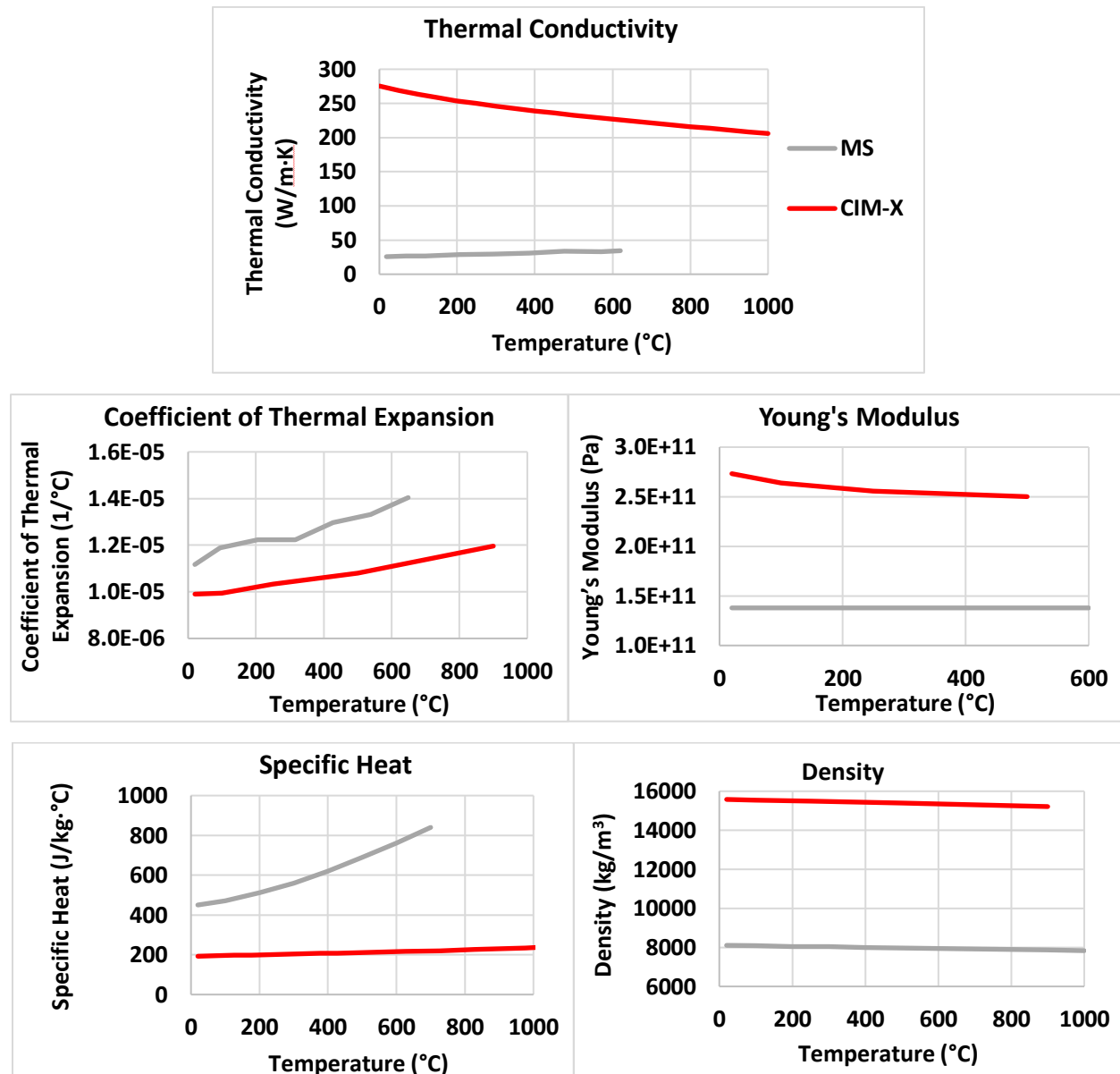


Figure 20: Material properties of constituent materials

3.1.3 Importance of Homogenization

Homogenization of lattice structures is important for Finite Element Analysis (FEA) simulation of parts for several reasons as discussed below:

- **Computational Efficiency:** Lattice structures are complex and have a high level of detail at the micro-level. Performing FEA directly on such detailed structures can be computationally expensive and time-consuming. Homogenization simplifies the structure to a more manageable level, reducing the number of elements and nodes in the simulation model, thus improving computational efficiency.
- **Reduced Model Complexity:** Homogenization allows for the representation of the lattice structure at a macroscopic level, simplifying the geometry and material properties of the structure. This reduces the complexity of the FEA model, making it easier to analyze and interpret results.
- **Effective Material Representation:** Lattice structures often have heterogeneous material properties due to variations in the geometry and arrangement of lattice elements. Homogenization helps in representing these complex material behaviors in a simplified and effective manner. By homogenizing the material properties, the FEA model can better capture the overall structural response.
- **Macroscopic Behavior Prediction:** Homogenization provides a means to predict the macroscopic behavior of the lattice structure under various loading conditions. By averaging out the microstructural details, homogenization enables the analysis of the overall mechanical response of the structure, including its stiffness, strength, and deformation characteristics.

- **Validation and Calibration:** Homogenization facilitates the validation and calibration of FEA models by comparing the macroscopic behavior predicted by the homogenized model with experimental data. This helps in assessing the accuracy and reliability of the simulation results and fine-tuning the model parameters as needed.

Overall, homogenization of lattice structures is crucial for efficient and accurate FEA simulations of parts, enabling engineers to analyze the structural performance and optimize the design of complex lattice-based components effectively.

3.1.4 Methodology for Homogenization

- **Rule of Mixture:** For a hybrid structure consisting of two or more materials, rule of mixture can be utilized to calculate combined properties of hybrid structure based on weighted contributions of each material. Some thermal and physical properties can easily be calculated accurately with the rule of mixture. While, mechanical properties, such as coefficient of thermal expansion and Young's Modulus, are required to be validated with either FEA or experiments to understand the interaction between two materials.
- **FEA:** Mechanical properties of hybrid structure are evaluated by solving finite element model computationally. An unit hybrid cell of diamond TPMS structure with infill is computationally solved to calculate linear thermal expansion under thermal loading. Based on the findings, coefficient of thermal expansion is calculated. Similarly, stress components for known strain values are calculated to obtain Young's Modulus of unit hybrid cell.

- **Experimental Validation:** Calculated as well as computationally solved Properties of hybrid structure are validated with experiment and compared with computational findings. Hybrid structure is subjected to thermal loading and experimental temperature profile is compared with FEA temperature profile. Upon noticing similarities between two temperature profiles, calculated properties of hybrid structure are used in the thesis work.

3.1.5 Application of Rule of Mixture

The rule of mixture can be applied to specific thermal and physical properties such as thermal conductivity, density, specific heat, etc. to calculate properties of hybrid structures through the composition of two materials. To compute the properties of hybrid tooling using the rule of mixture, it's essential to determine the volume fraction of each material. Volume fraction of hybrid structure made with maraging steel TPMS and CIM-X infill depends on the type of TPMS, and cell size and wall thickness of TPMS. In Figure 21, a lattice structure of 4x4x4 Primitive type TPMS is shown with Cell size and wall thickness labelled on unit cell [58]. With a fixed TPMS wall thickness, an increase in cell size results in a higher infill volume fraction. Conversely, when the cell size remains constant, a greater TPMS wall thickness leads to a lower infill volume fraction. In other words, desired thermal and structural properties can be obtained in hybrid tooling by simply changing wall thickness and cell size. Furthermore, through additive manufacturing, a single tooling insert can be produced with a heterogeneous TPMS structure featuring varying cell sizes and wall thicknesses. This variation allows for the acquisition of distinct thermal and structural properties in different regions of the insert.

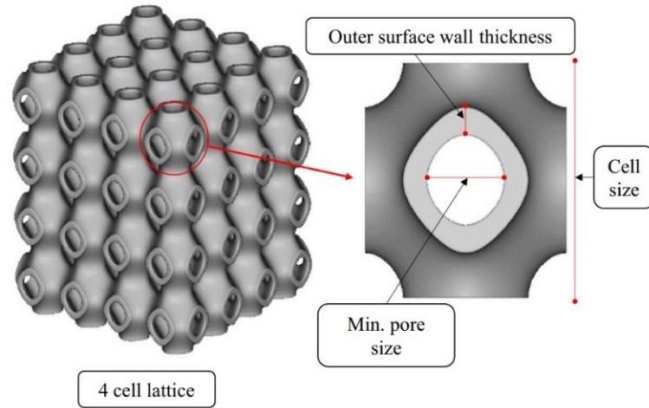


Figure 21: TPMS unit cell and wall thickness [58]

Figure 22 shows 4 different types of TPMS unit cells, 3x3x3 TPMS structures in blue color as well as hybrid structures with high thermal conductivity infill material in grey color [59]. Column (d) in Figure 22 shows 111 section-cut to illustrate the interconnection between maraging steel TPMS and infill material.

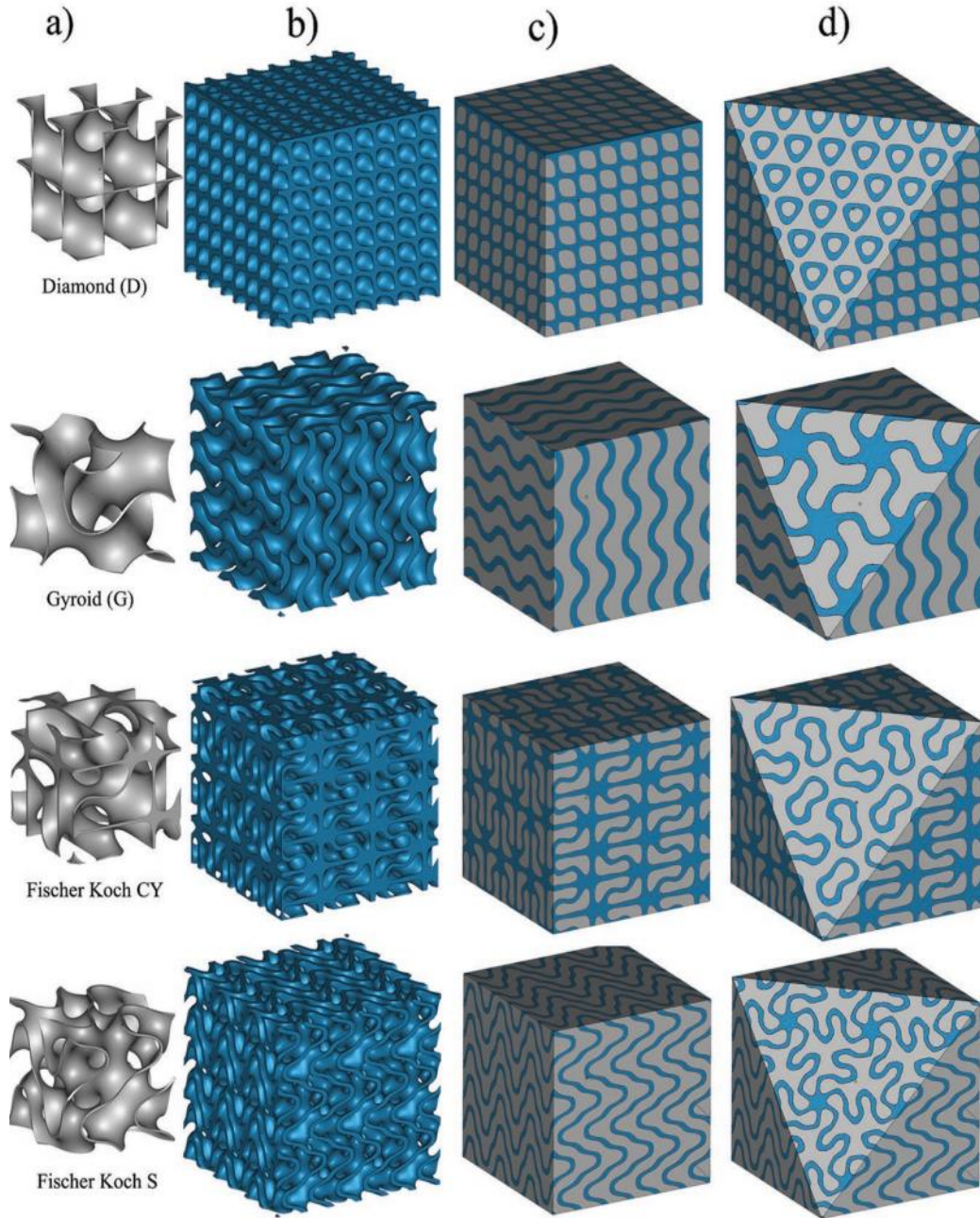


Figure 22: (a)TPMS Unit cell, (b) 3x3x3 TPMS structure, (c) Hybrid structure of TPMS and Infill and (d) 111 section-cut of hybrid structure [59]

The infill volume fraction for Diamond TPMS with various TPMS cell sizes (mm) and wall thicknesses (mm) is derived by calculation and plotted as shown in Figure 23. The selection of

Diamond TPMS for this work is due to the straight down porous structure that diamond TPMS offers. This TPMS allows for easier infiltration process and ensures proper infiltration without pores in the structure. Although, the Figure 23 shows graph with TPMS wall thickness varying from 0.0 to 1.5 mm, the minimum wall thickness achievable with various AM processes range from 0.1 to 0.45 mm [60] [61].

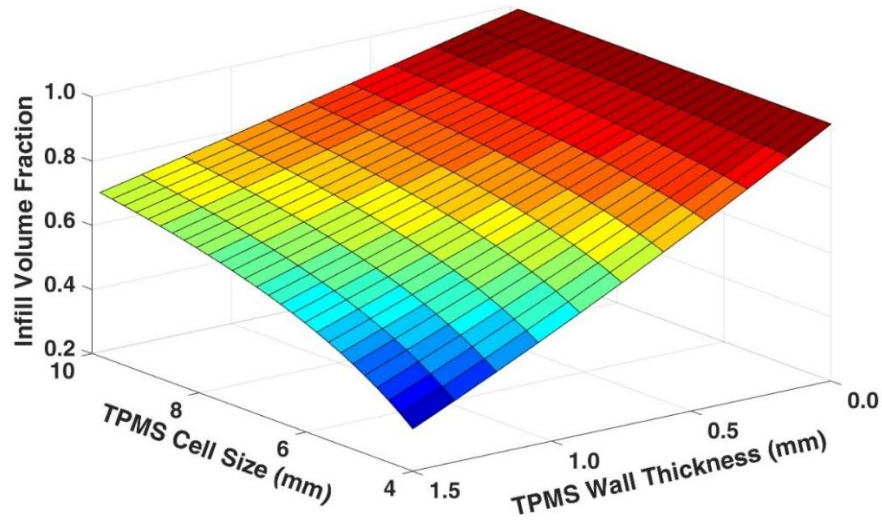


Figure 23: Infill volume fraction vs TPMS cell size vs TPMS wall thickness

Infill volume fraction for different Cell size and wall thickness configurations of diamond TPMS structure is calculated using engineering design tool nTop [62]. Relationship between infill volume fraction, cell size and TPMS thickness for Diamond type TPMS is derived using the calculated data and is presented as an equation below:

$$f_{Inf}(t, c) = 1.001 - 1.989 \left(\frac{t}{c} \right) + 0.5499 \left(\frac{t}{c} \right)^2 \quad (2)$$

Where,

t = TPMS Wall Thickness (mm)

and c = TPMS cell size (mm)

Thermal conductivity, density and specific heat of hybrid structure can be calculated by the equations of mixture law as below:

$$K_{Hybird} = (f_{lattice} \times K_{lattice}) + (f_{inf} \times K_{inf}) \quad (3)$$

$$\rho_{Hybird} = (f_{lattice} \times \rho_{lattice}) + (f_{inf} \times \rho_{inf}) \quad (4)$$

$$Cp_{Hybird} = (f_{lattice} \times Cp_{lattice}) + (f_{inf} \times Cp_{inf}) \quad (5)$$

Where, K = Thermal Conductivity W/m·K

ρ = Density (KG/m³)

C_p = Specific Heat (J/KG·K)

$f_{lattice}$ = Lattice Volume Fraction

f_{inf} = Infill Volume Fraction

3.1.6 Simulations to Derive Properties

Properties such as Young's modulus and the coefficient of thermal expansion for the hybrid structure can be challenging to predict using the rule of mixture and require validation through simulation. The simulations to derive these properties are discussed below:

3.1.6.1 Coefficient of Thermal Expansion

Geometry and Mesh Plot

Hybrid unit cell, for the simulation purposes, is created by creating CAD geometry of diamond TPMS unit cell with Maraging steel properties and infilling the cavities of TPMS with CIM-X as shown in Figure 24.

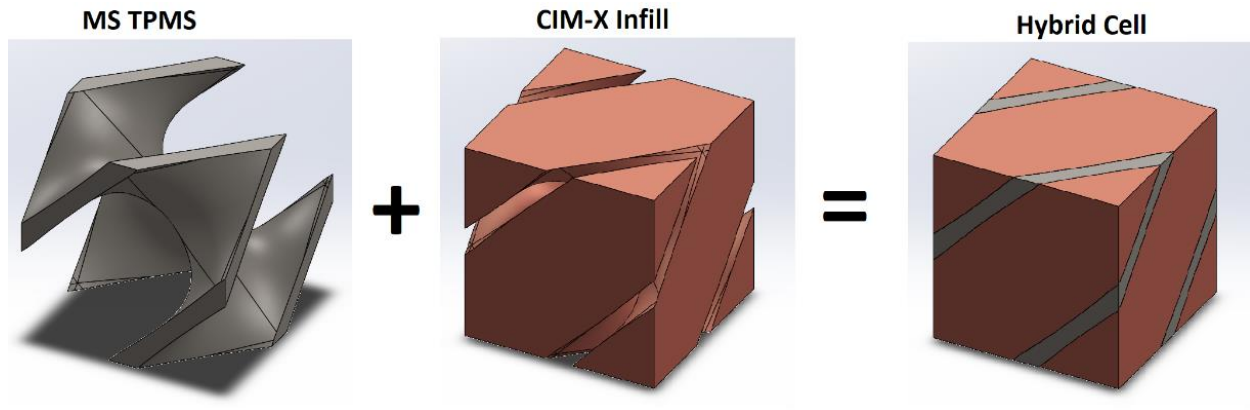


Figure 24: Construction of Hybrid Unit TPMS Cell

Unit cell of Diamond TPMS hybrid structure with uniform cell size of 10 mm and TPMS wall thickness of 1.5 mm, as shown on the left in Figure 25, is computationally solved to calculate displacement under known thermal loading.

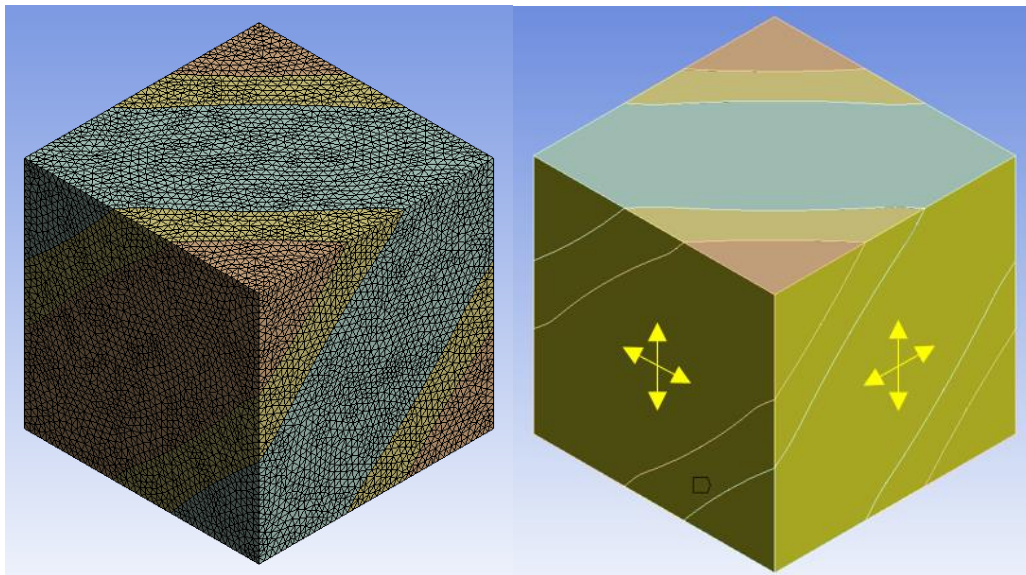


Figure 25: CTE simulation of TPMS: meshed model (left) and boundary conditions (right)

Diamond TPMS unit cell is meshed with second order tetrahedron mesh elements to obtain better fit for curved surfaces of the TPMS. Fine mesh of 0.025 mm sized elements is used to increase the accuracy of this transient simulation.

Boundary Conditions

The diamond TPMS unit cell is kept at room temperature of 25°C at the beginning of thermal loading. At every 5 seconds of time interval, the unit cell is heated up by 25°C until it reaches up to 600°C of temperature. Two materials, MS and CIM-X are given bonded connection on all the contact surfaces for this simulation work.

One corner of this unit cell is fixed. Three adjacent faces of the cell that form this corner, are constrained in normal direction to the plan. Nodes on these three faces are constrained from moving in normal direction of that particular planes. Linear thermal expansion of the unit cell is calculated at the end of thermal cycle.

Results

The directional displacement in Y direction of this TPMS unit cell is shown in the Figure 26 below:

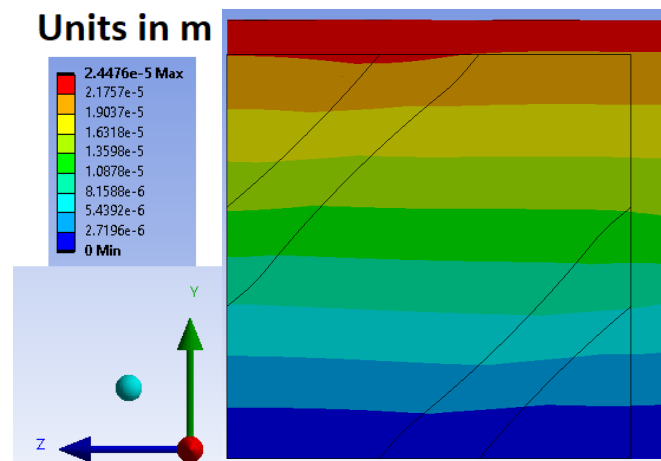


Figure 26: Linear displacement of unit TPMS cell

Linear displacement under thermal loading is calculated computationally. Coefficient of thermal expansion is calculated by dividing the displacement with cell size and temperature difference. The computational results fit closely to the results calculated using the rule of mixture and hence the rule of mixture to calculate CTE of hybrid cell using infill volume fraction is validated.

3.1.6.2 Young's Modulus

Simulation Methodology

Based on the homogenization approach suggested by Steven [63], the relationship between stress and strain for any material in 3D space can be described by stiffness matrix as below:

$$\begin{bmatrix} \sigma_X \\ \sigma_Y \\ \sigma_Z \\ \sigma_{YZ} \\ \sigma_{XZ} \\ \sigma_{XY} \end{bmatrix} = \begin{bmatrix} C_{11} & C_{12} & C_{13} & C_{14} & C_{15} & C_{16} \\ C_{21} & C_{22} & C_{23} & C_{24} & C_{25} & C_{26} \\ C_{31} & C_{32} & C_{33} & C_{34} & C_{35} & C_{36} \\ C_{41} & C_{42} & C_{43} & C_{44} & C_{45} & C_{46} \\ C_{51} & C_{52} & C_{53} & C_{54} & C_{55} & C_{56} \\ C_{61} & C_{62} & C_{63} & C_{64} & C_{65} & C_{66} \end{bmatrix} \begin{bmatrix} \varepsilon_X \\ \varepsilon_Y \\ \varepsilon_Z \\ \varepsilon_{YZ} \\ \varepsilon_{XZ} \\ \varepsilon_{XY} \end{bmatrix} \quad (6)$$

Due to the uniform cell size in all the directions and cubical symmetry that TPMS lattice structure present, the stiffness matrix variables can be simplified as follow: $C_{11}=C_{22}=C_{33}$ and $C_{44}=C_{55}=C_{66}$. In addition, $C_{12}=C_{23}=C_{31}$ and so on. This simplifies the stiffness matrix as below:

$$\begin{bmatrix} \sigma_X \\ \sigma_Y \\ \sigma_Z \\ \sigma_{YZ} \\ \sigma_{XZ} \\ \sigma_{XY} \end{bmatrix} = \begin{bmatrix} C_{11} & C_{12} & C_{12} & 0 & 0 & 0 \\ C_{12} & C_{11} & C_{12} & 0 & 0 & 0 \\ C_{12} & C_{12} & C_{11} & 0 & 0 & 0 \\ 0 & 0 & 0 & C_{44} & 0 & 0 \\ 0 & 0 & 0 & 0 & C_{44} & 0 \\ 0 & 0 & 0 & 0 & 0 & C_{44} \end{bmatrix} \begin{bmatrix} \varepsilon_X \\ \varepsilon_Y \\ \varepsilon_Z \\ \varepsilon_{YZ} \\ \varepsilon_{XZ} \\ \varepsilon_{XY} \end{bmatrix} \quad (7)$$

If a known strain component is applied to the geometry and using FEA tool, the stress components are calculated, the stiffness matrix variables can be solved by solving the equation

above. For uniaxial pure tensile strain of $\epsilon_Y = 5 \mu\text{m}$ the stiffness matrix equation can be simplified as $\sigma_Y = C_{11} \cdot \epsilon_Y$ and $\sigma_X = C_{12} \cdot \epsilon_Y$. Similarly, for absolute shear strain of $\epsilon_{XY} = 5 \mu\text{m}$ the equation can be solved as $\sigma_{XY} = C_{44} \cdot \epsilon_{XY}$.

Young's modulus of hybrid cell can be calculated by the equation below [64]:

$$E = \frac{C_{11}^2 + C_{12}C_{11} - 2C_{12}^2}{C_{11} + C_{12}} \quad (8)$$

Zener anisotropy index [65], measure of isotropy of cubic structures, can be calculated by the following equation as below:

$$A = \frac{2C_{44}}{C_{11} - C_{12}} \quad (9)$$

For isotropic hybrid cell, the Zener anisotropy index is nearly 1. The value farther from 1 indicates anisotropy in the TPMS Hybrid cell.

Geometry and Mesh Plot

The same geometry of 10 mm cell size Diamond TPMS unit cell with 1.5 mm wall thickness as shown on the left in Figure 25 of CTE simulation is used in the simulation to evaluate Young's Modulus. Geometry is meshed similarly to CTE geometry with second order tetrahedron elements of 0.25 mm size.

Boundary Conditions

The Figure 27 above shows boundary conditions used in the simulation to evaluate Young's modulus of the hybrid Diamond type TPMS unit cell. For the pure tensile strain component of this simulation, as shown on the left in the figure, the bottom face of the cubic cell

is fixed in all the directions. While the elements on the four sides of the cubic cell are free to move in the plan but are restricted to move in the direction normal to plan. The elements on the top face of the cell are given displacement of 5 μm in Y+ direction.

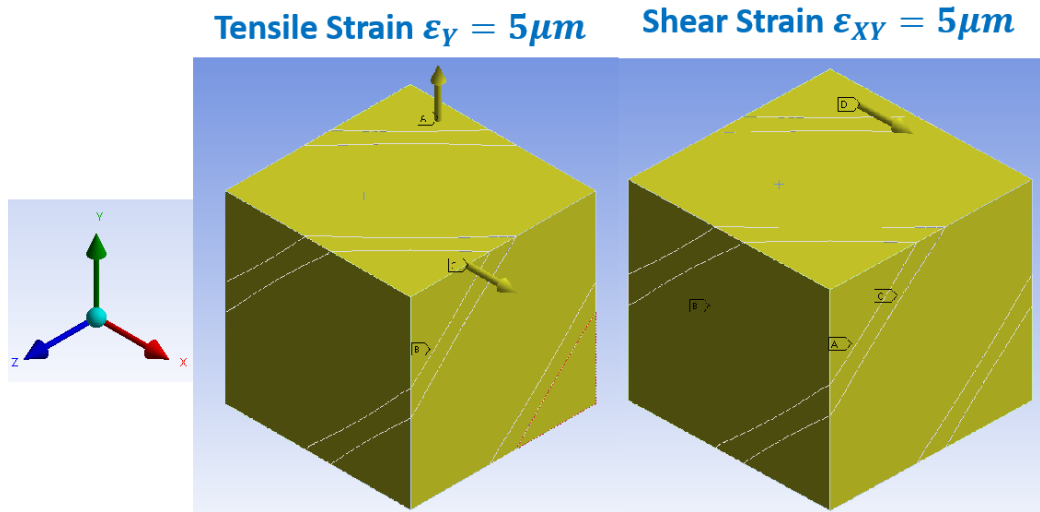


Figure 27: Young's Modulus simulation boundary conditions: tension (left) and shear (right)

For the absolute strain simulation, elements on the bottom face of the cube are constrained in all the directions. The elements on two faces parallel to XY plan are free to move in planner directions but constrained in normal to plan direction. Whereas the elements on two faces parallel to YZ plan are fixed in Y and Z directions but are free to move in X direction. The top face elements are given displacement of 5 μm in the X+ direction as shown on the right in the figure.

Results

The Figure 28 below shows tensile and shear stress components in unit cell of hybrid TPMS structure with undeformed wireframe model.

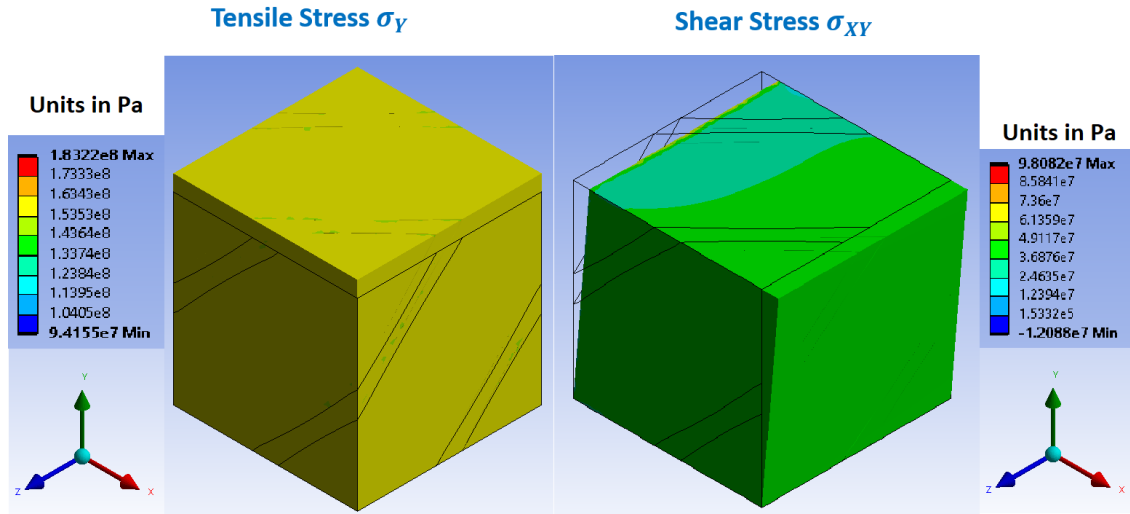


Figure 28: Tensile and shear stress in unit cell of TPMS

The Figure 29 below shows computationally calculated values of Young's Modulus for various cell size and wall thickness TPMS unit cells in grey dots along with red line indicating values obtained by the rule of mixture. Red dots indicate values of pure MS and CIM-X.

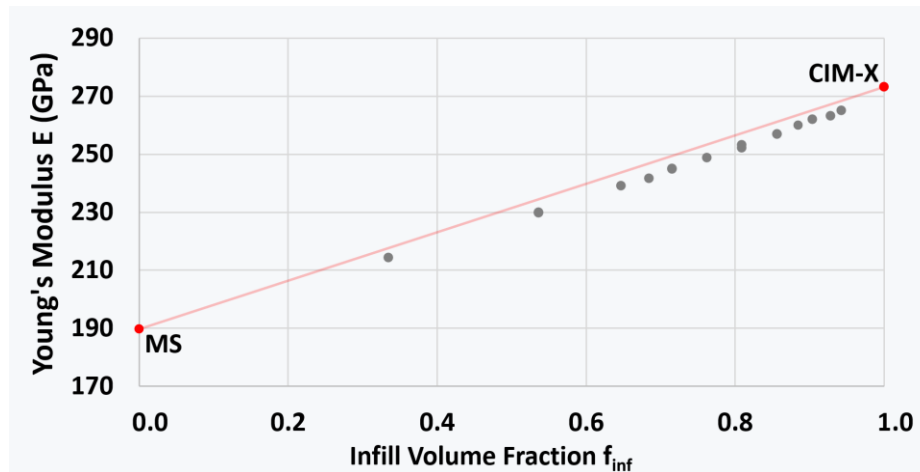


Figure 29: Young's Modulus result comparison

The Young's modulus calculated by the approach mentioned above show a very good match with the values calculated by the rule of mixture. Hence, in this research work, the rule of

mixture is validated to calculate the Young's modulus for hybrid TPMS cell with known infill volume fraction.

Similarly, the Zener Anisotropic Index A is calculated for different cell size and wall thickness. The calculated values of A for the Diamond TPMS unit cell are found to be nearly equivalent to 1. Therefore, Diamond TPMS structure is isotropic structure and hence it is used widely in this work for the two case studies discussed in the following chapters.

Similar to calculating room temperature material properties of hybrid structure, material properties at elevated temperatures can also be calculated using the rule of mixture and constituent properties of materials at elevated temperatures.

3.2 Results and Discussion

3.2.1 Equations of Homogenization

Based on the material properties of maraging steel as TPMS and CIM-X as infill material, the equations for properties of hybrid structure at room temperature can be calculated as below:

$$\text{Thermal conductivity } K = 246.29(f_{inf}) + 25.84 \quad W/m \cdot K \quad (10)$$

$$\text{Density } \rho = 7477.3(f_{inf}) + 8103.7 \quad KG/m^3 \quad (11)$$

$$\text{Specific Heat } C_p = -257.63(f_{inf}) + 450 \quad J/KG \cdot K \quad (12)$$

$$\text{CTE } \alpha = -2E-06(f_{inf}) + 1E-05 \quad K^{-1} \quad (13)$$

$$\text{Young's Modulus } E = 84.219(f_{inf}) + 184.92 \quad GPa \quad (14)$$

3.2.2 Validation of Homogenized Thermal Conductivity

To ensure the accuracy of the calculations, experimental validation is essential for verifying the material properties of the hybrid TPMS structure. The thermal performance of the hybrid structure, based on the calculated material properties and analyzed using a computational model, is assessed in comparison with experimental thermal data. The validation of homogenized thermal conductivity with experiment is discussed below:

3.2.2.1 Geometry

In this experiment, a square bar measuring 10 mm x 10 mm x 110 mm, composed of maraging steel, is heated at one end to 160 °C. Additionally, a similar-sized square bar is used, with maraging steel shell measuring 1 mm on three sides and 1.6 mm on the fourth side, featuring a TPMS structure at the center, as depicted in Figure 30. The hybrid test piece incorporates a Schwarz D TPMS structure, featuring a uniform cell size of 8 mm and a wall thickness of 0.5 mm. The infill volume fraction in this structure is 0.85. Plain maraging steel specimen is machined from raw material into final size. The hybrid test specimen is manufactured with LPBF process in the form of shell and TPMS with maraging powder. The maraging steel powder has a particle size of 15 to 45 microns. Infill material, that has lower melting point than maraging steel, is melted and infiltrated into the diamond TPMS with proprietary process.

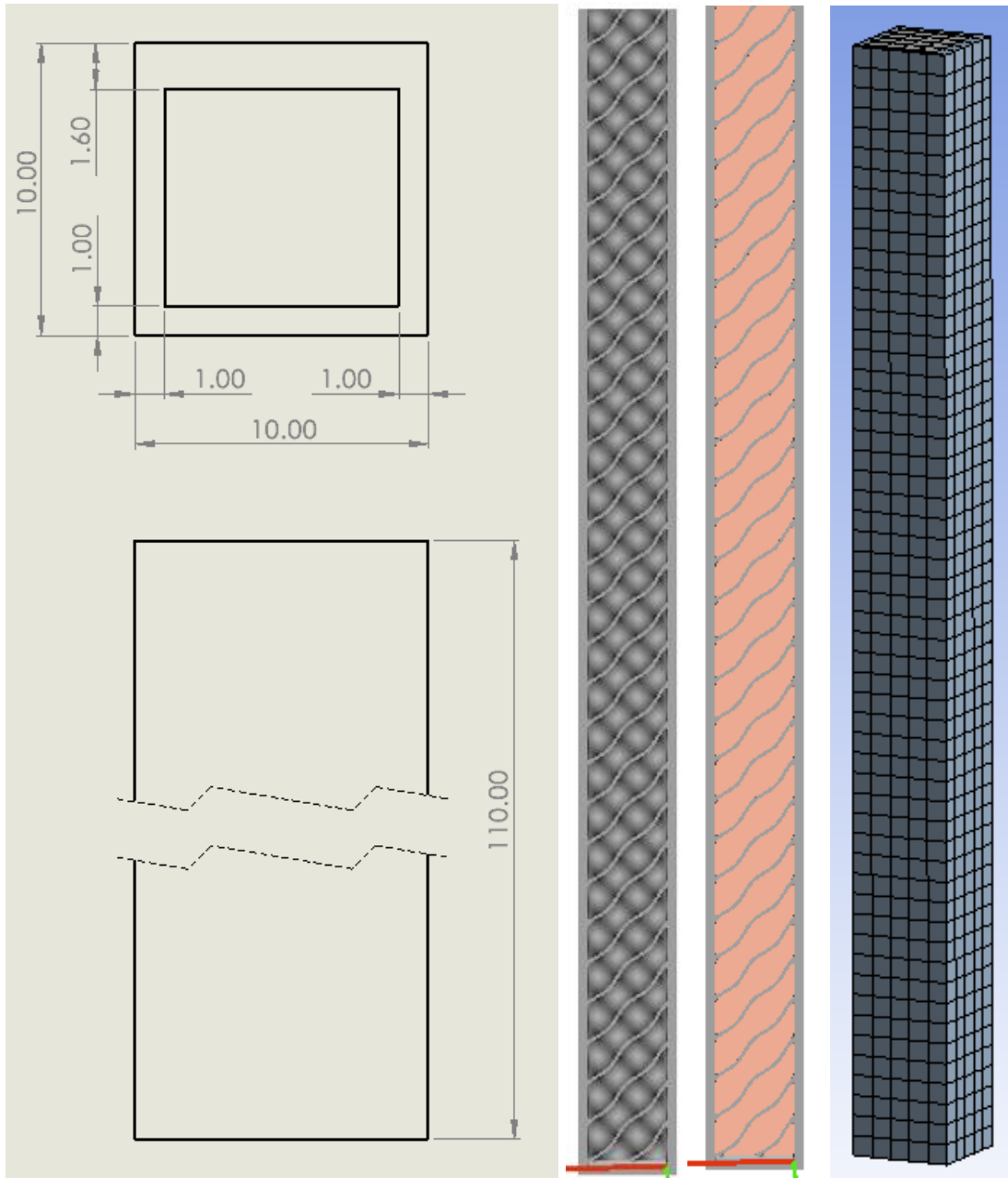


Figure 30: Geometry of hybrid test piece (mm)

3.2.2.2 Simulation work

Mesh Plot and model

Mesh plot for this simulation work can be seen in the Figure 30 on the right. Hex-dominant elements of 2 mm in size are used to mesh this geometry for both the plain and hybrid cases.

Hex-dominant elements of second order mesh provide better accuracy as well as offer good fit for nearly uniform square bar like geometry.

Boundary Conditions

Initial temperature of the test specimen is kept at 22°C to match with the room temperature observed during the experiment. The bottom face of the test specimen is kept heated at a constant temperature of 160°C throughout the heating phase. Remaining five surfaces of this square bar are given convection cooling by ambient air at 22°C and with convection coefficient of 14 W/m²°C. Average temperature of the nodes on the top surface of the specimen is recorded and is compared against the experimental values. The contact surfaces between the maraging steel clad and hybrid TPMS core are assumed to have bonded connections in this simulation work.

3.2.2.3 Experimental Setup

Figure 31 below depicts the experimental arrangement for homogenization validation. Two test specimens, one plain and the other hybrid, are subjected to heating at one end by contacting a copper plate that is being heated using a heat plate set at 160 °C. Consistent contact is maintained by employing load springs to press the test pieces onto the copper plate. On the opposite ends of the test specimens, two PT100 RTD sensors are affixed, and a data logger operating at a 1 Hz frequency is utilized to capture temperature measurements. The heating process is conducted for a duration of 30 minutes, following which the heat plate is switched off, allowing the test pieces to cool in the surrounding air.



Figure 31: Experimental setup

3.2.2.4 Test Results

The comparison between the experimental test results and those generated by the computational model is illustrated in Figure 32 below. The continuous lines depict the experimental results, while the dashed lines represent the calculated hybrid and plain Maraging steel results. By examining the data presented in the graph, it becomes evident that the results achieved through calculations employing material properties obtained via homogenization align closely with the experimental findings. Consequently, this validation confirms that the utilization of calculated properties for the hybrid structure is a viable and effective approach for subsequent simulations in this research work.

A minor divergence observed in the hybrid test specimen can be related to the processing of the specimen. Hybrid test specimen is manufactured by additive manufacturing process, and after de-powdering infill material is melted and infiltrated in TPMS cavity. Both the ends of hybrid test specimen are machined after infiltration. In FEA, the contacts at two ends of specimen are perfect contacts. On the other hand, the contacts in experimental specimen are not perfect contacts due to the above-mentioned process related factors. In addition, error in de-powdering could lead to lose powder in structure. Similarly, error in infiltration could lead to pores in hybrid test specimen.

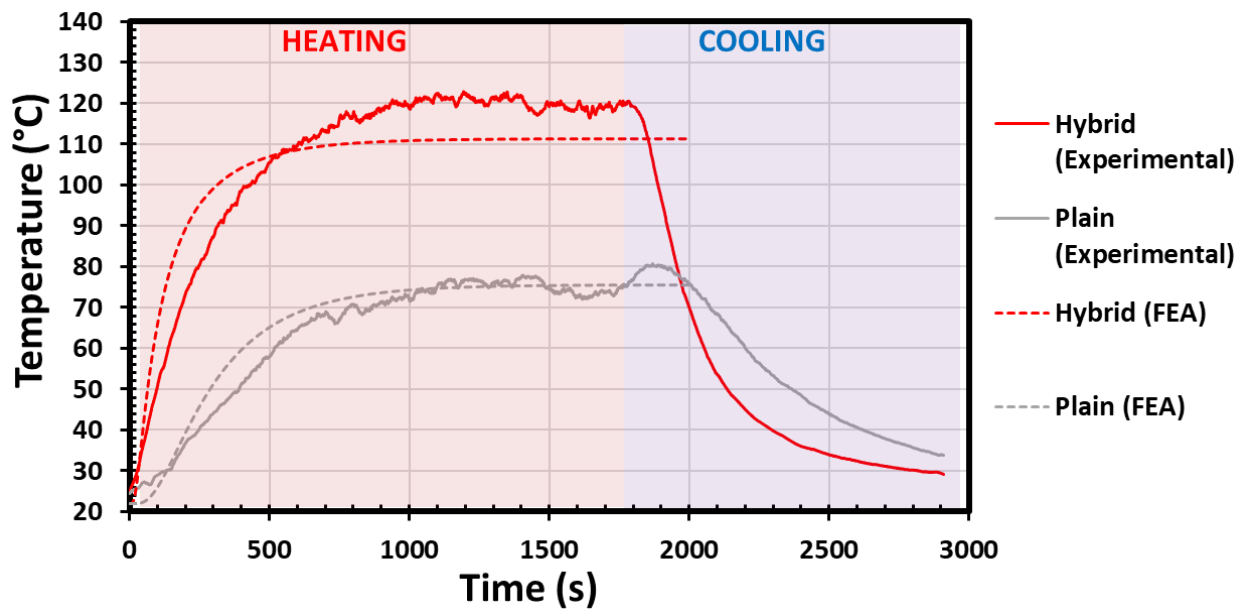


Figure 32: Homogenization result comparison

3.2.3 Homogenization Results

Utilizing the homogenization process outlined above in this chapter, the temperature-dependent material properties of the hybrid TPMS cell for a given infiltration ratio can be calculated by the rule of mixture and represented graphically as follows.

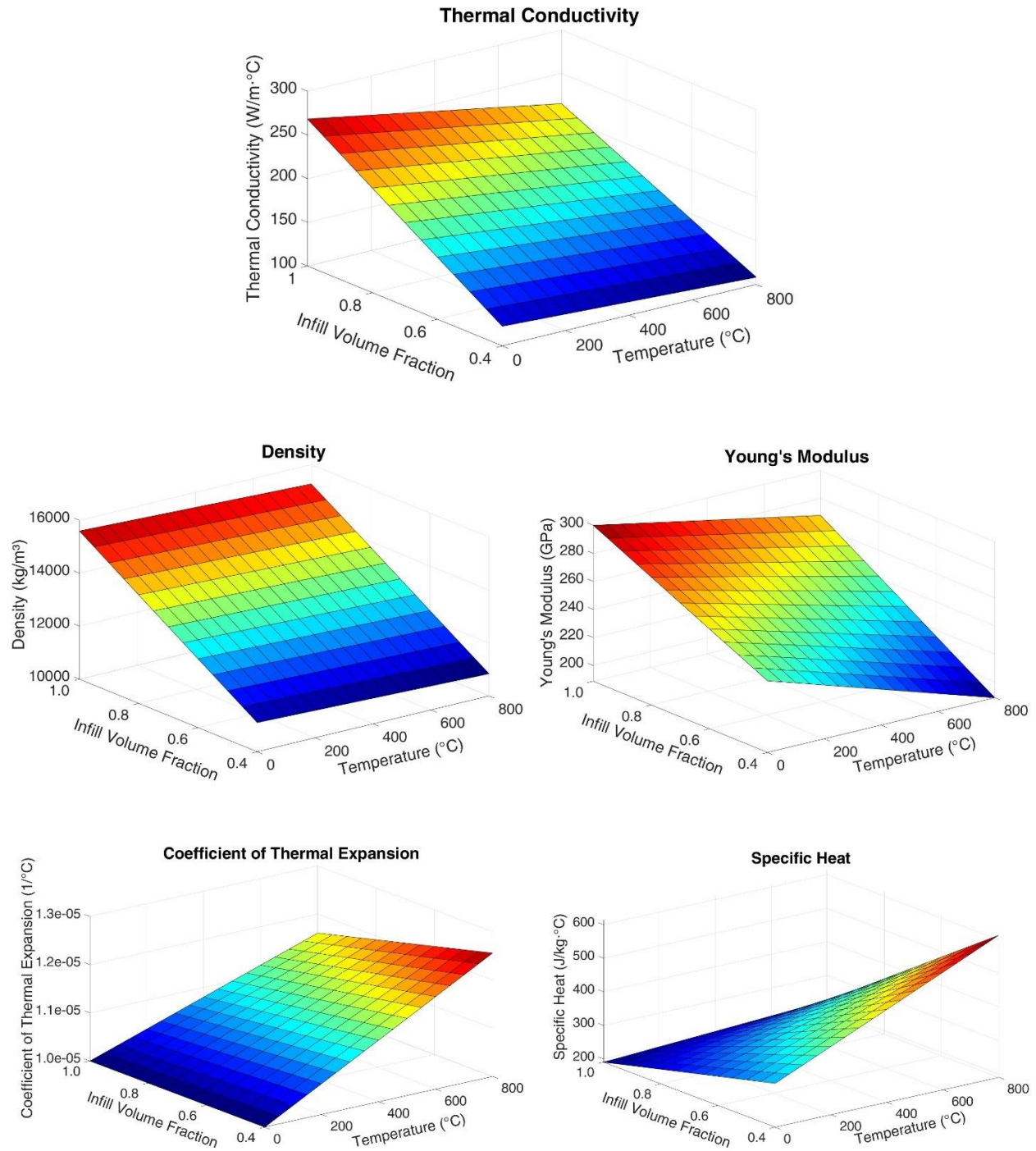


Figure 33: Calculated material properties of hybrid cell

Chapter 4: Case Study 1

4.1 Background

The initial geometry chosen for investigating the impact of multi-material tooling through additive manufacturing is derived from a design commonly employed in Wallace Experiments, also known as Dunk tests [66]. The Wallace experiment emulates the thermal cycles encountered in the casting processes observed in real-world scenarios. Wallace test setup and specimen geometry are illustrated in Figure 34 below:

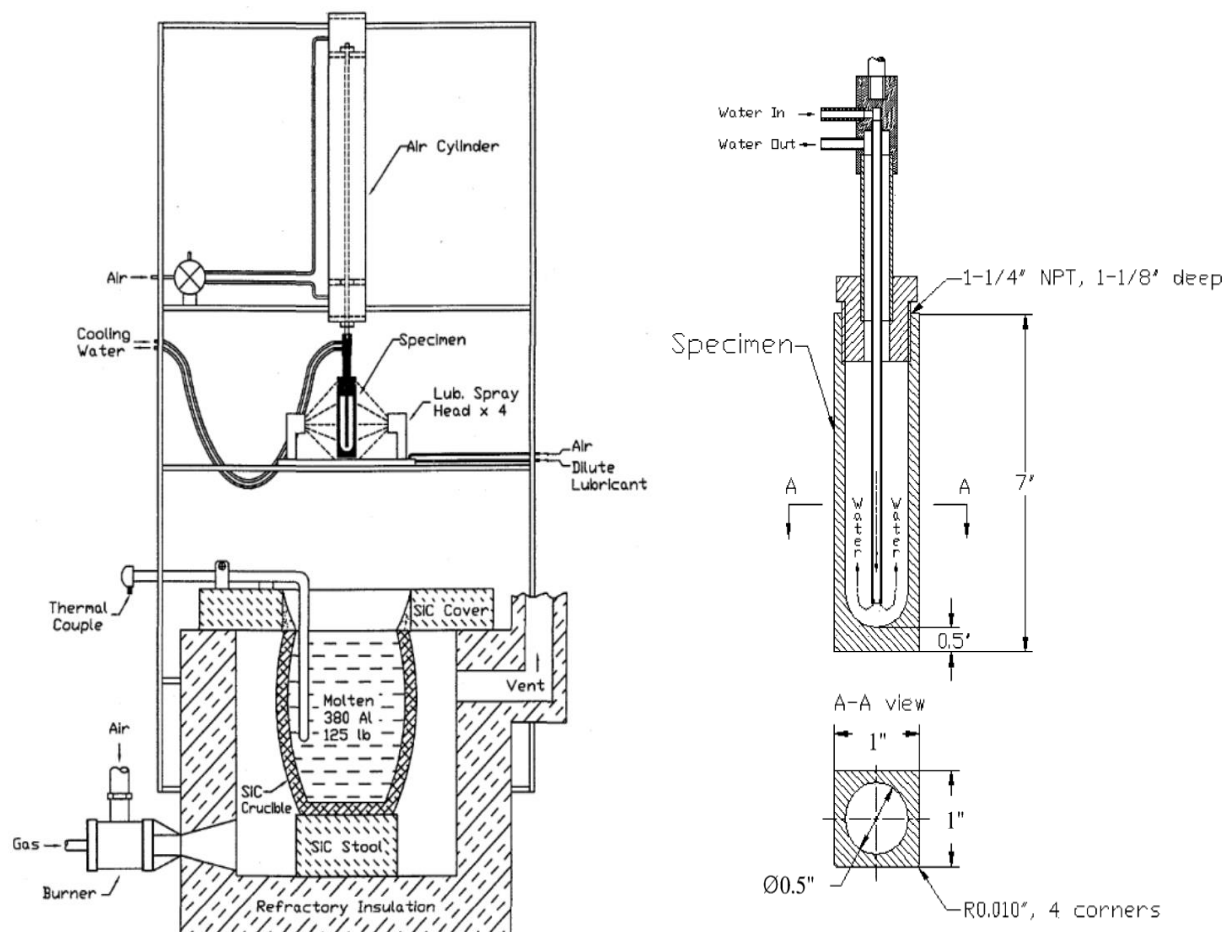


Figure 34: Wallace experiment setup and specimen geometry [67]

The Wallace experiment procedure can be summarized as below [2]:

- Water at room temperature always circulates through the specimen consistently at a rate of 0.85 gallons per minute.
- The test specimen is submerged in a hot aluminum bath for a duration of 12 seconds and then extracted to undergo a cooling period in ambient air for 22 seconds.
- Prior to the subsequent immersion, a water-based lubricant is sprayed onto the specimen for a duration of 1 second. This sequence is repeated numerous times.
- After several hundred cycles, a specimen segment, positioned at an equal distance from both ends and measuring 3 inches in length, is examined for the presence of cracks.
- Following an assessment of the quantity and depth of cracks, the process is reiterated for another set of several hundred cycles.

4.2 Geometry

For the simulation purpose in this work, the Wallace test specimen geometry is simplified in metric unit measurement system as shown in Figure 35.

For simulation purposes, symmetry is incorporated, and only one-eighth of the geometry is simulated to minimize computational time. For hybrid tooling, the CIM-X is infilled in TPMS lattices between maraging steel clads. Minimum clad thickness for the hybrid tooling is kept 2 mm to allow for dimensional accuracies. Thin walled structure poses challenge in additive manufacturing, as well as finishing by machining need extra material left on outer clads.

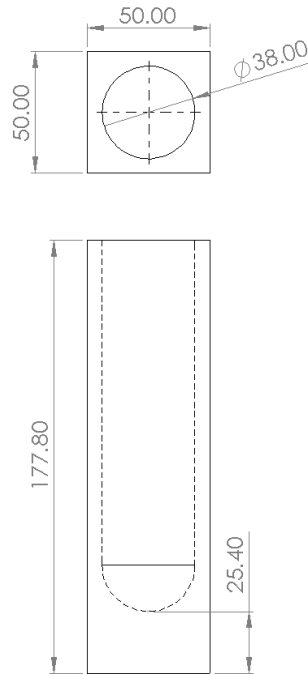


Figure 35: Geometry for case study 1

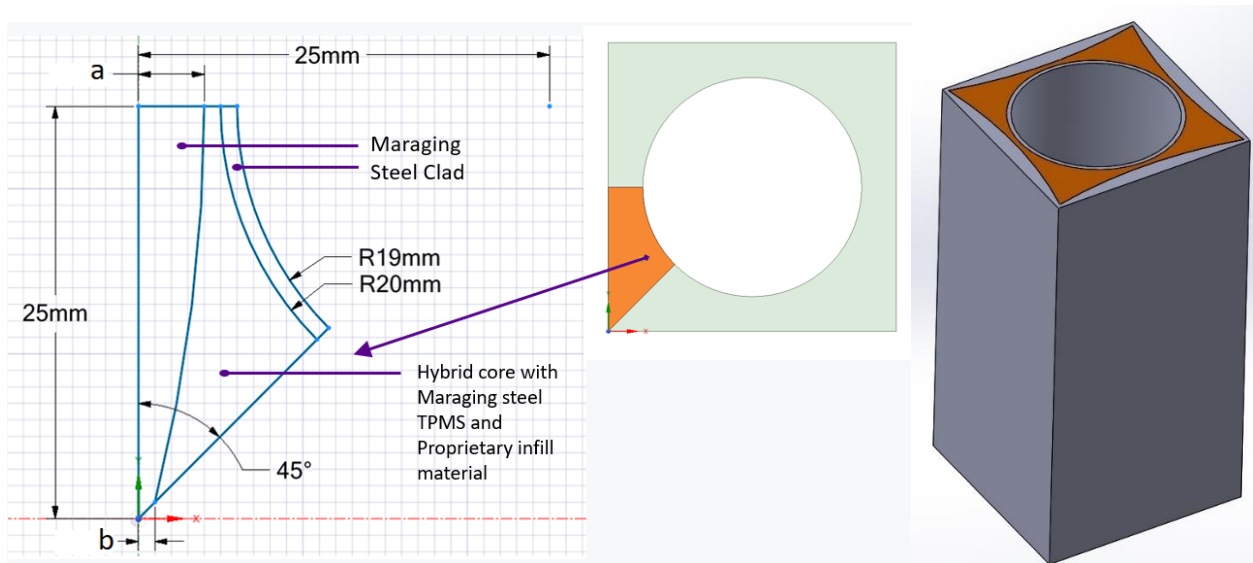


Figure 36: Hybrid geometry for case study 1

In Figure 36, the dimensions a and b play a pivotal role in determining the thickness of the outer maraging steel (MS) cladding. Varied values of these dimensions are employed in

computational optimization to enhance the design of hybrid tooling, aiming to decrease the maximum temperature on the hot side and mitigate temperature distribution across the hot side of the tooling.

The cross-section of the optimized hybrid tooling geometry is shown in the Figure 37 below:

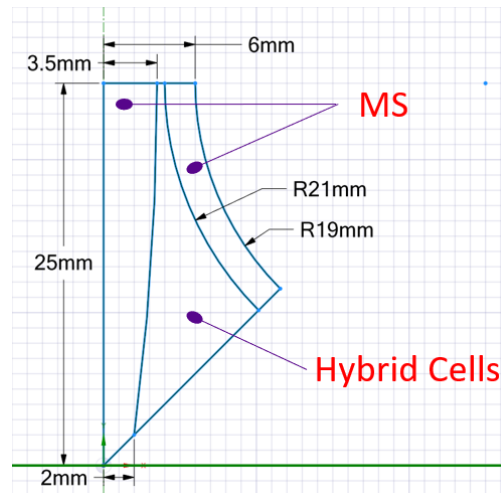


Figure 37: Optimized hybrid geometry

4.3 Material Properties

This work explores two scenarios involving hybrid tooling as below:

- I. Hybrid #1: TPMS Cell size = 3 mm and Wall Thickness = 0.5 mm
- II. Hybrid #2: TPMS Cell size = 3 mm and Wall Thickness = 1 mm

Below are the material properties for Maraging steel, as well as for the two multi-material hybrid configurations:

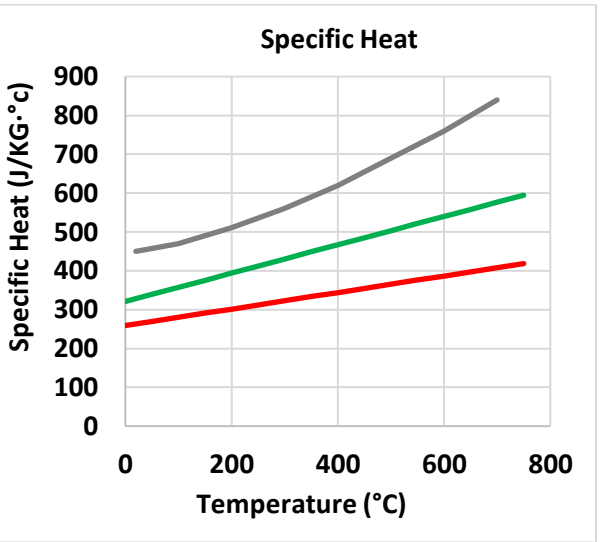
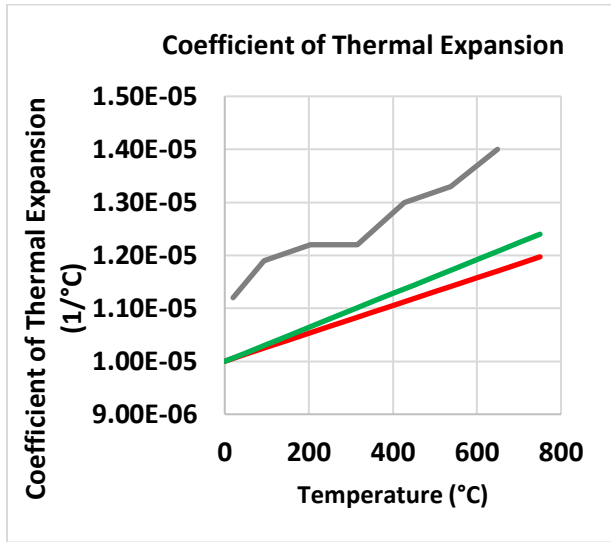
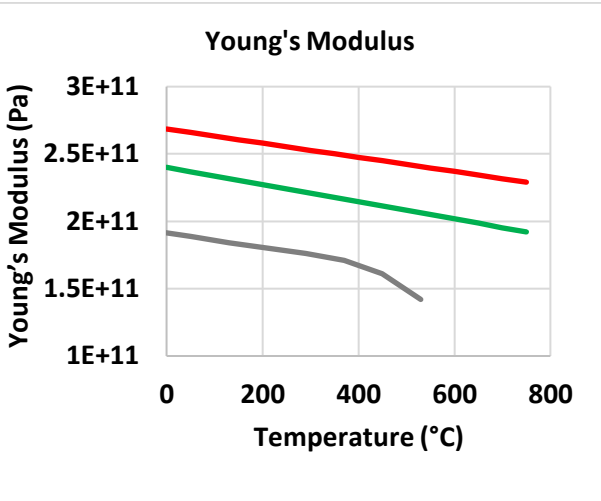
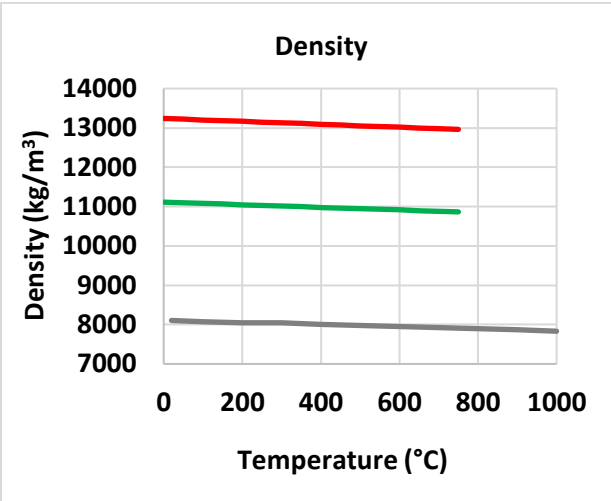
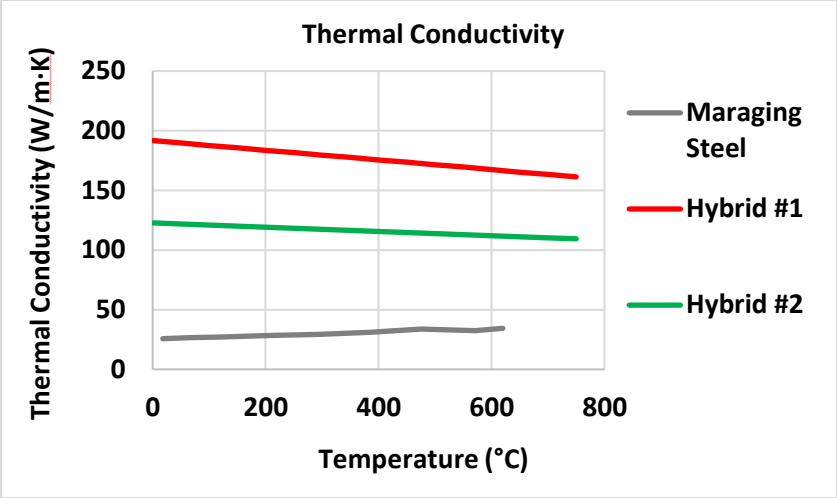


Figure 38: Material properties for simulations

4.4 Mesh Plot

Table 3: Mesh plot for Wallace geometry

	Top Portion (Area of Importance)	Bottom Portion (Insignificant region)
Mesh Type	Hex-dominant (Better accuracy)	Tetraheadron (Better fit for curved surfaces)
Mesh Size	0.8 mm to 1.0 mm sized elements (fine mesh)	1.0 mm sized elements (coarse mesh)
Aspect Ratio	1.0003 (high quality mesh)	1 to 500 (poor quality mesh)
Ordre	Second order mesh (better capture of deformation)	Second order mesh (better capture of deformation)

To ensure the reliability of the computational outcomes, a mesh sensitivity test is conducted on the provided geometry. The mesh refinement process is iteratively implemented until the obtained results exhibit a high degree of similarity or near-identity, contributing to increased accuracy and reliability in the computational analysis. The final mesh plot is described in the Table 3 above as well as illustrated in Figure 39 below:

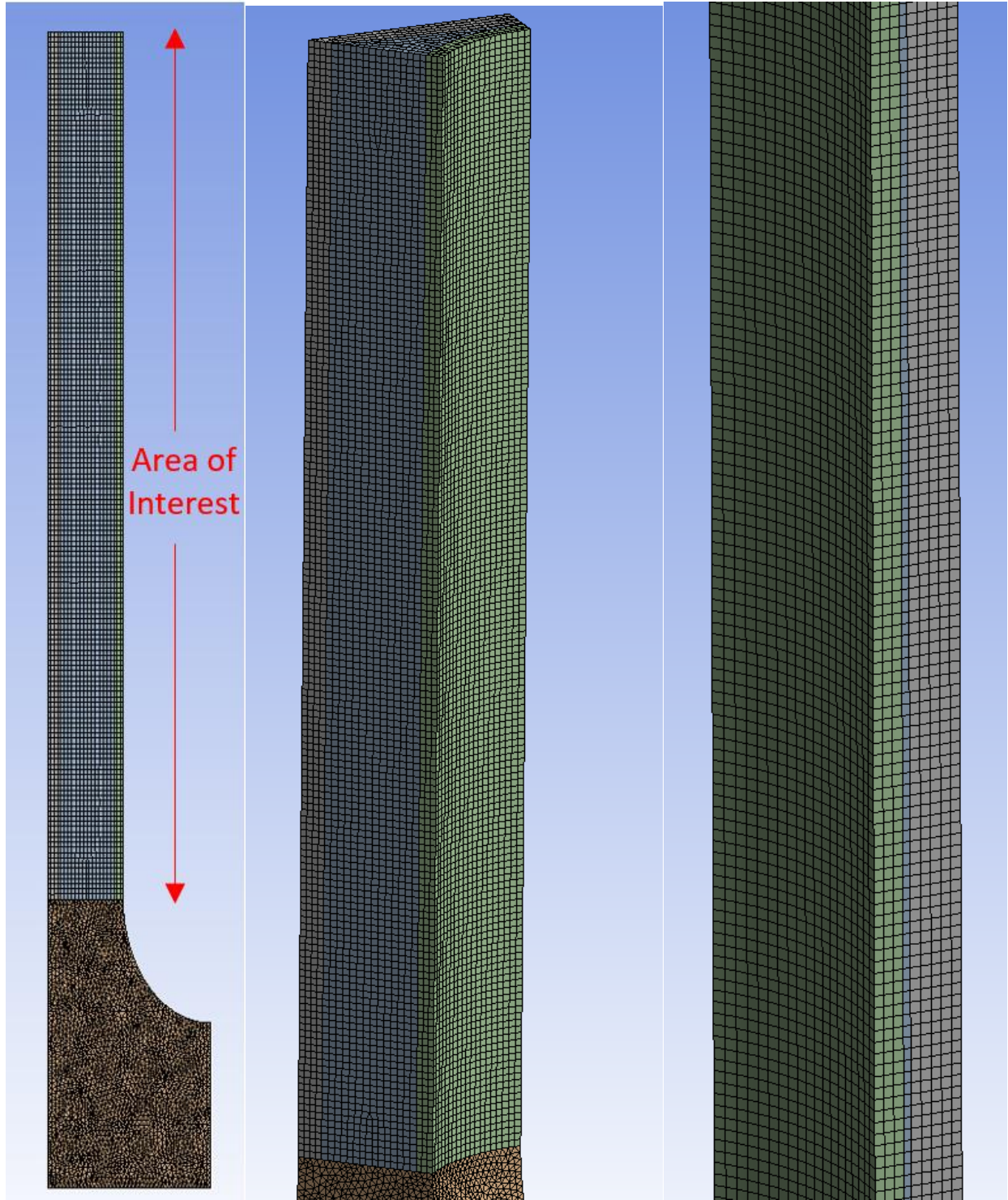


Figure 39: Mesh plot for 1/8th Wallace geometry

4.5 Boundary Conditions

4.5.1 Thermal Boundary Conditions

The cross-sectional view of the Wallace geometry is depicted in the Figure 40. The surface area delineated by the red line undergoes exposure to hot aluminum during immersion and is denoted as the hot side in this study. Conversely, the region outlined by the blue curve encounters circulating cold water and is identified as the cold side. Due to the presence of symmetry, there is no heat transfer across the area delineated by the green lines.

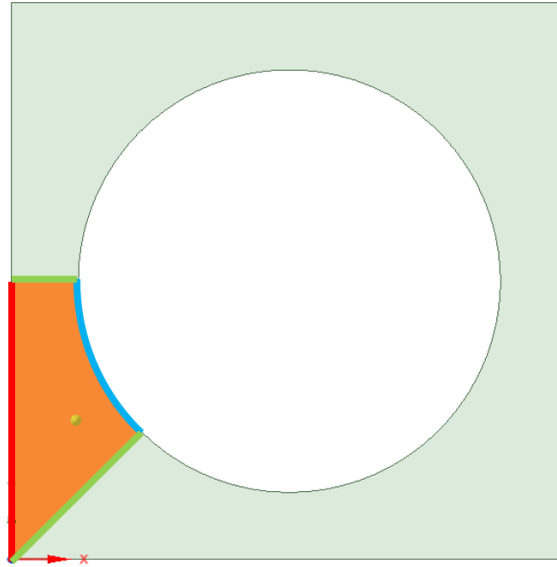


Figure 40: Cross-section of Wallace geometry illustrating thermal boundary conditions

The thermal boundary conditions for a single thermal cycle are succinctly outlined in the Table 4 provided below. In the table, T_{∞}^H and h_H indicate melt temperature and convection coefficient at the hot side of the specimen. Subsequently, T_{∞}^C and h_C indicate water temperature and convection coefficient of water cooling respectively. The simulation process is iteratively

conducted across three thermal cycles to allow temperatures to stabilize. The results, crucial for achieving heightened accuracy, are extracted specifically from the third cycle.

Table 4: Thermal boundary conditions for Wallace geometry

Cycle Steps	Duration (s)	Hot Side (Outer Surface)		Cold Side (Water Circulation)	
		T_{∞}^H (°C)	h_H ($W/m^2 \cdot K$)	T_{∞}^C (°C)	h_C ($W/m^2 \cdot K$)
Step 1: Heating	25	630	4,016	20	7,112
Step 2: Air Cooling	2	20	24	20	7,112
Step 3: Spraying	4	20	4,184	20	7,112
Step 4: Stabilizing	49	20	24	20	7,112

The presented thermal cycle emulates a real die casting cycle. The initial temperature of the test specimen is set at 20°C. During the heating phase, the specimen is immersed in a hot aluminum bath. In the subsequent step (Step 2), the specimen is extracted from the aluminum bath and allowed to cool down in ambient air for 2 seconds. In Step 3, cold water is sprayed onto the outer side of the specimen. Before the commencement of the next cycle, the specimen undergoes a cooling phase in ambient air for stabilization. Throughout the entire process, cold water circulates continuously inside the specimen. Contact surfaces of the Maraging steel shell and Hybrid TPMS cells are given bonded connections.

4.5.2 Mechanical Boundary Conditions

The mechanical boundary conditions for this study, as illustrated in Figure 41, can be enumerated as follows.

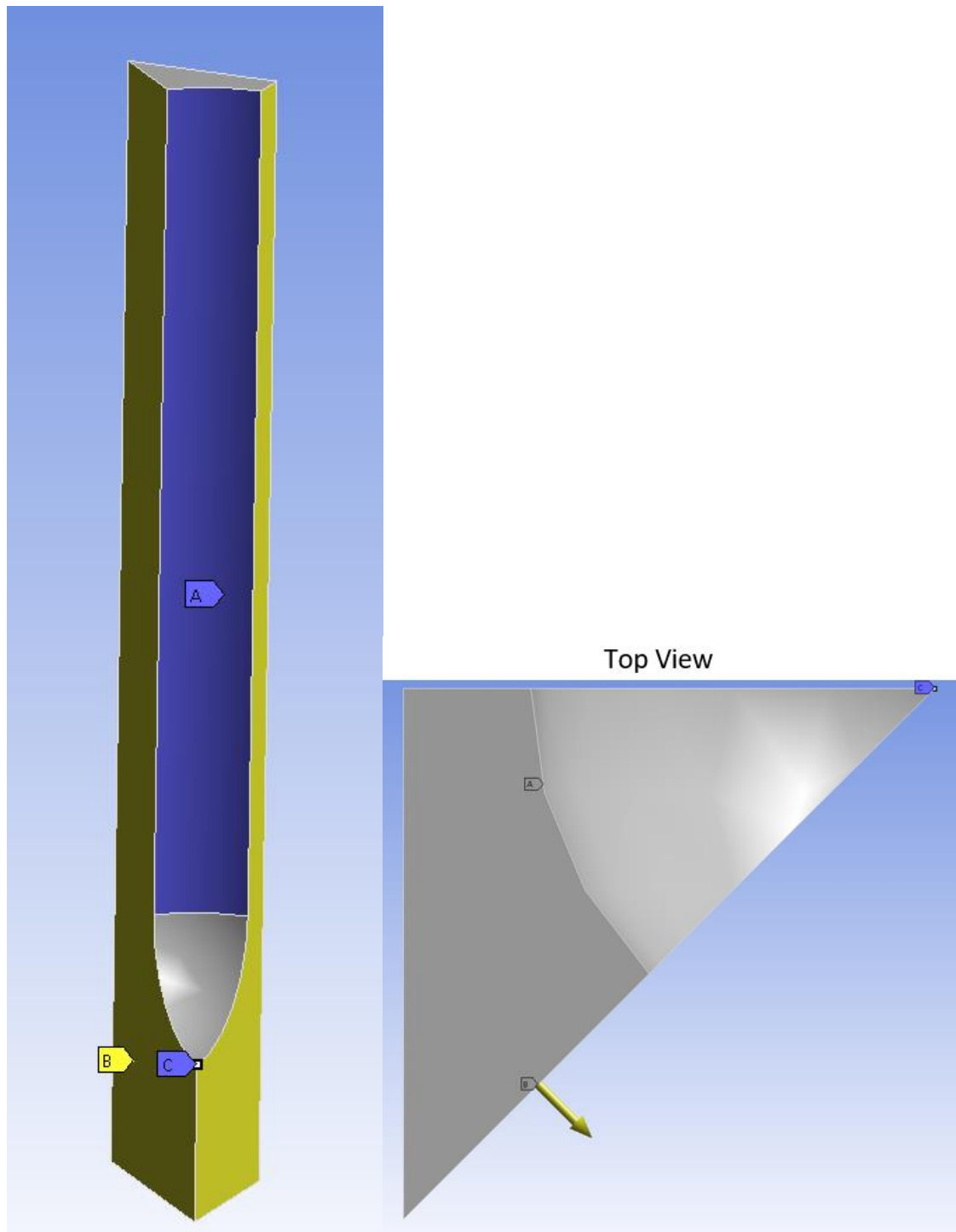


Figure 41: Mechanical boundary conditions for Wallace geometry

- A. The internal surface of the specimen, which encounters the circulation of cold water, is provided with cylindrical support. The elements on this surface have the freedom to move

both radially and axially in response to thermal expansion or contraction. However, due to the 1/8th symmetry, twisting of the elements on this surface is precluded, and the elements are constrained from moving in the tangential direction.

- B. The side surfaces, indicated by the yellow shading in Figure 41, are restricted in the normal directions. Nonetheless, the elements situated on this surface have the liberty to move within the plane directions.
- C. A vertex designated as C is fixed to the ground in all directions, thereby providing stability to the geometry.

The outcomes of thermal simulations are utilized to impose thermal loading on the mechanical model, enabling stress calculations and deformation analyses.

4.5.3 Analysis Settings

Rather than conducting 2D simulations for the cross-section perpendicular to the specimen's length, 3D simulations are employed for the following reasons:

The application of a plane stress or plane strain scenario is not suitable for this geometry due to its nonuniformity along its length. Both stresses and strains are observed along the direction of the specimen's length. Therefore, a 3D simulation is conducted, incorporating outcomes acquired from a cross-section perpendicular to the specimen's length and positioned at the center within the geometry of interest as shown in Figure 42.

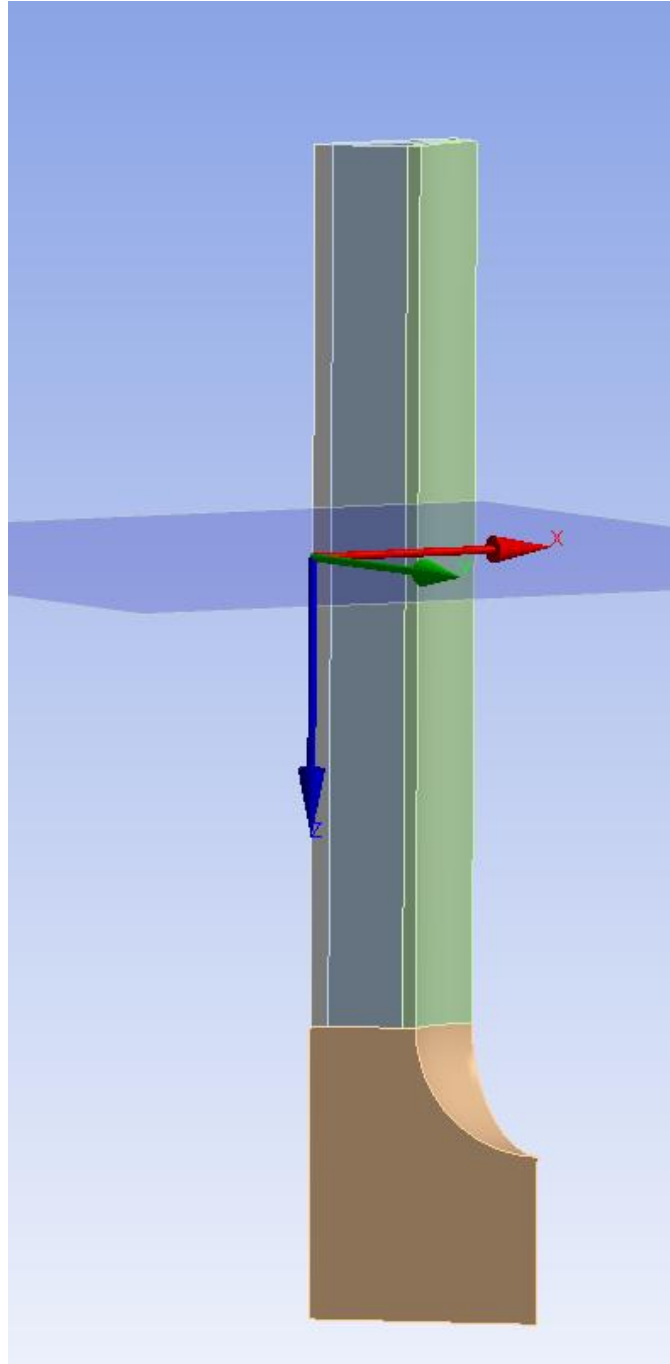


Figure 42: Area of interest for deriving results

4.6 Thermal Results

Maximum temperature derived at the third casting cycle is shown in the Figure 43 below as well as temperature ($^{\circ}\text{C}$) contours at the end of 3rd heating cycle ($t=185$ sec) are shown in the

Figure 44. Temperature increases during the heating phase of 25 seconds and is observed to be maximum at the end of heating cycle. Spray cooling brings down temperature at faster rate and afterwards specimen is cooled down by air cooling until it reaches desired temperature.

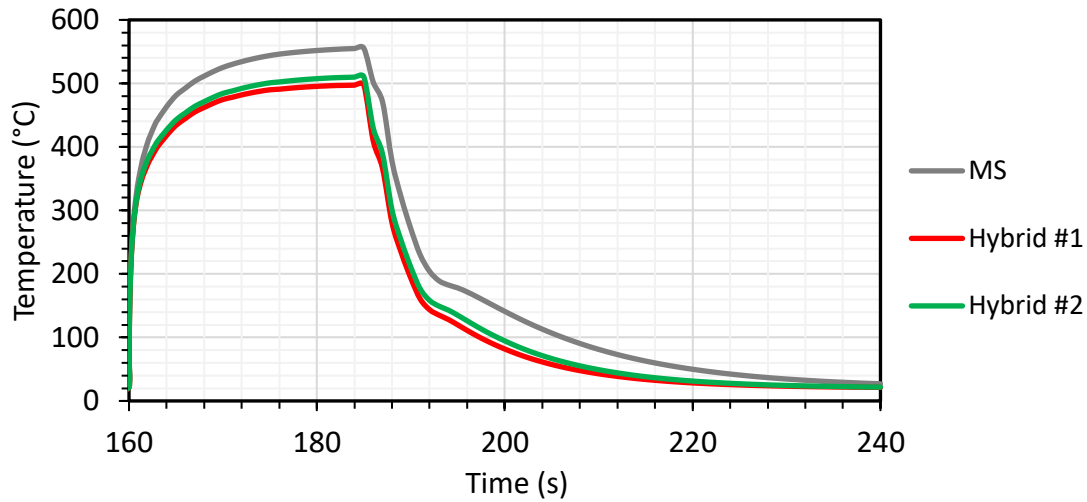


Figure 43: Maximum temperature across the cross-section

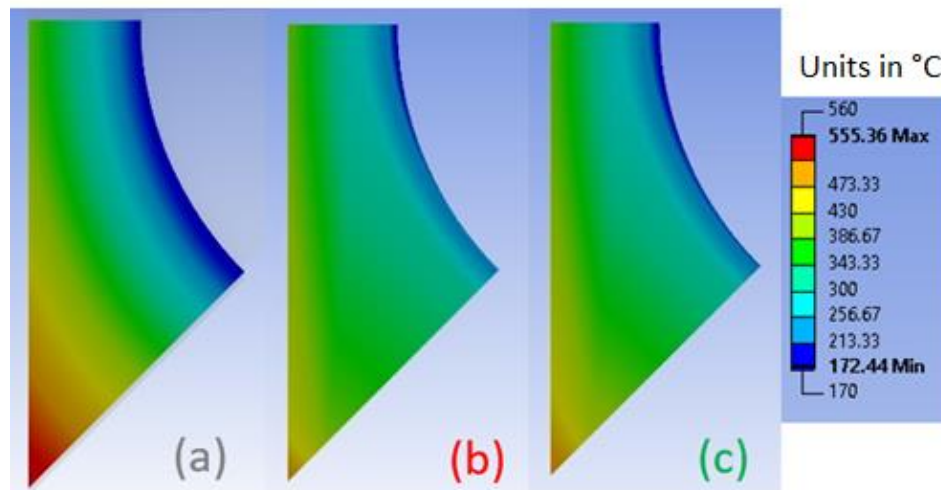


Figure 44: Temperature(°C) contours at 185 sec for (a) MS, (b) Hybrid #1 and (c) Hybrid #2

Three important parameters for internal stresses within specimen as temperature gradient across the geometry, average temperature difference between hot and cold side and

temperature distribution on the hot side are illustrated against time as shown in Figure 45, Figure 46 and Figure 47 respectively.

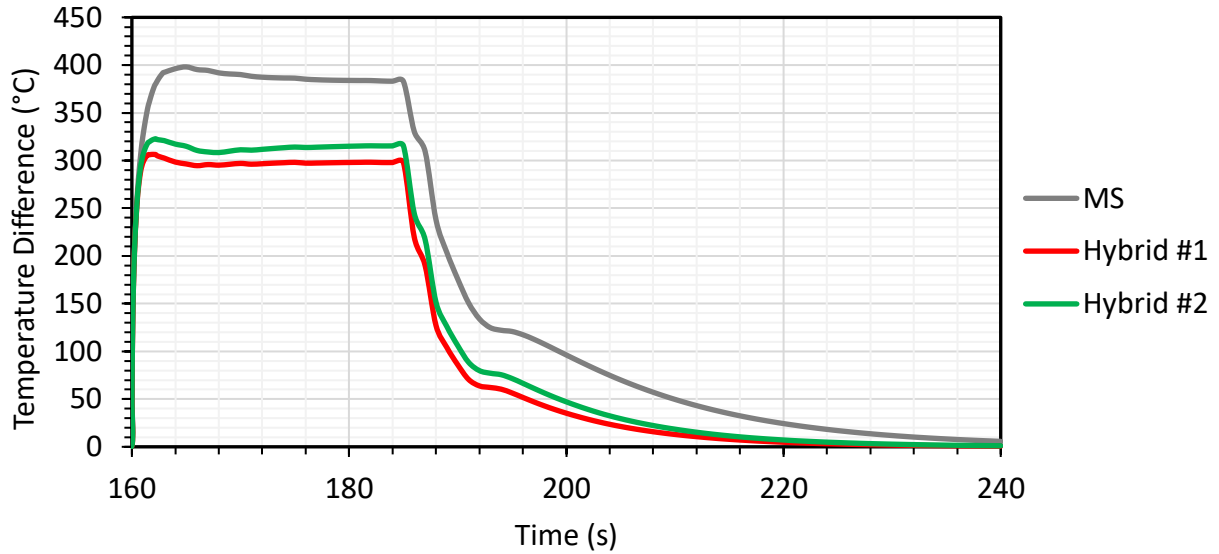


Figure 45: Temperature gradient across the cross-section

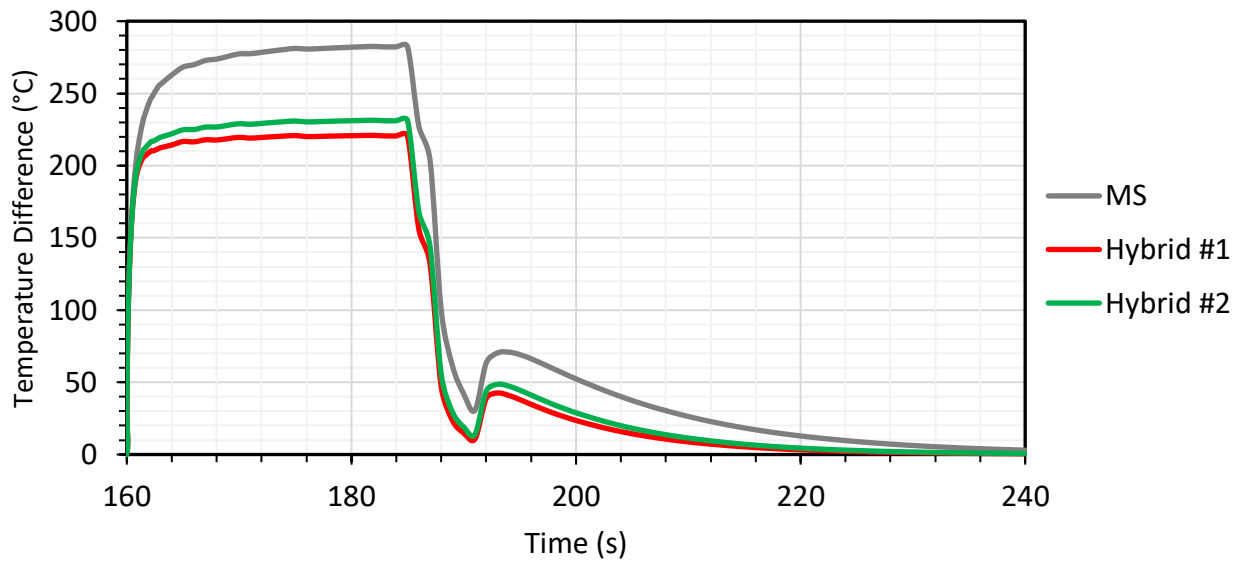


Figure 46: Difference between average temperatures at hot and cold sides

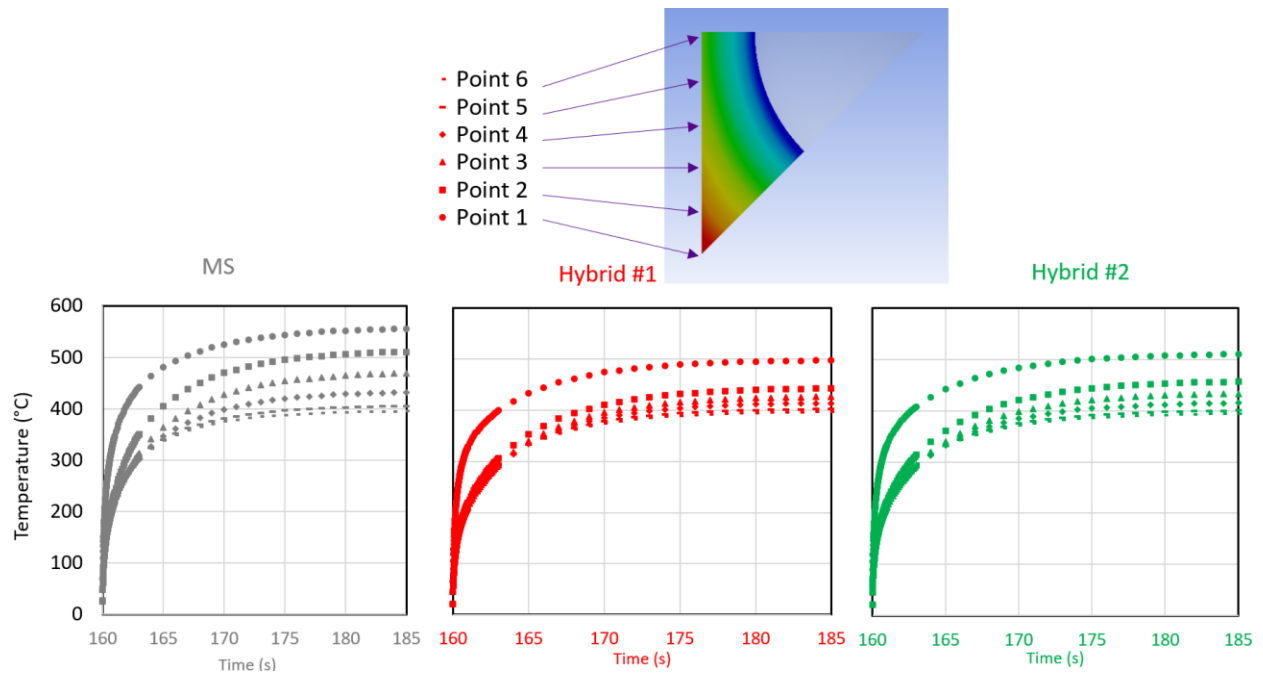


Figure 47: Temperature distribution on the hot side

4.7 Mechanical Simulation Results

Maximum Von-Mises (Equivalent) stresses for third casting cycle are compared in Figure 48 and stress contours at the end of heating cycle are shown in Figure 49.

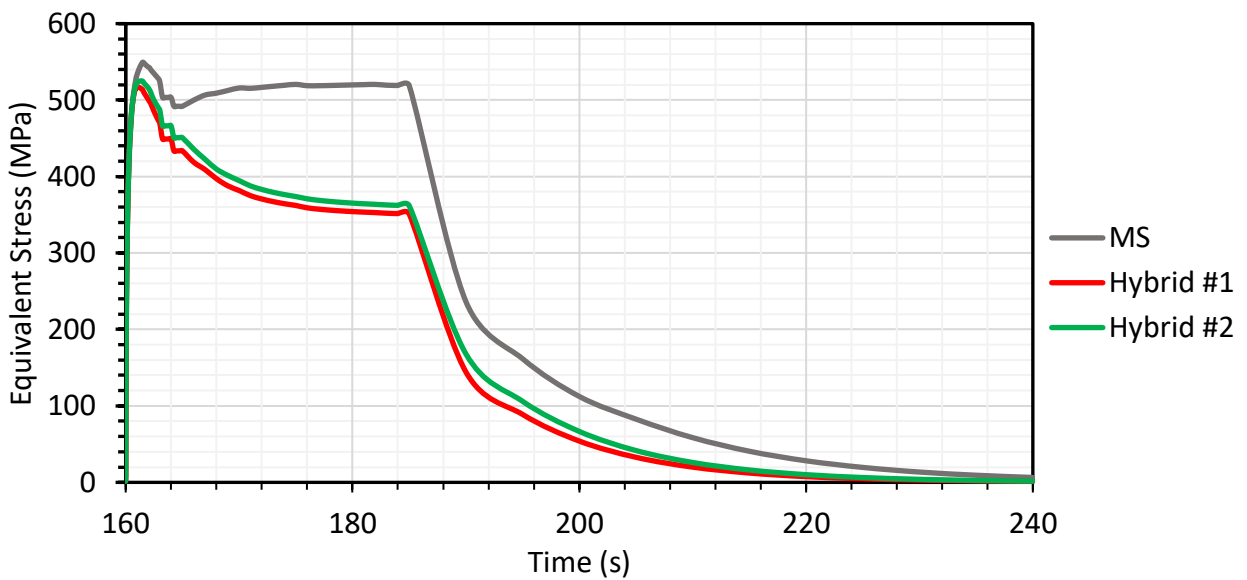


Figure 48: Maximum Von-Mises stresses

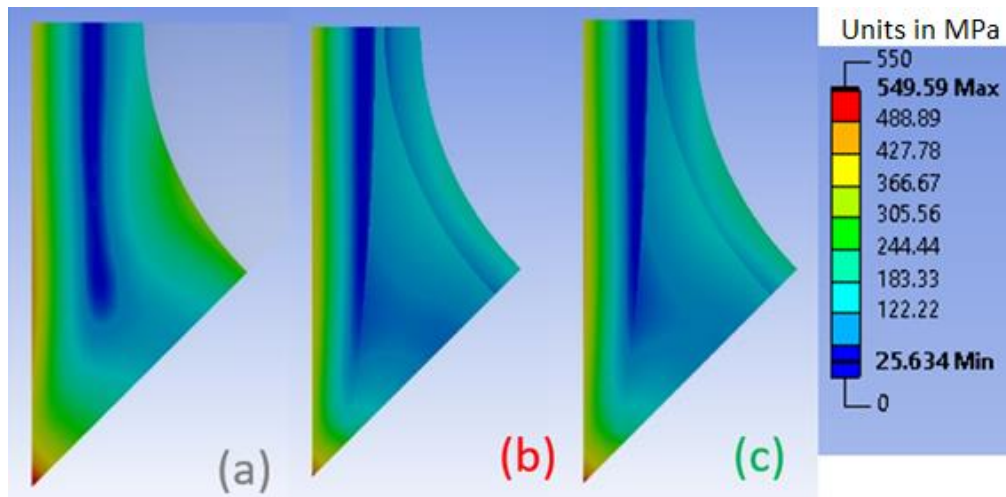


Figure 49: Von-Mises stress (MPa) contours at 185 sec for (a) MS, (b) Hybrid #1 and (c) Hybrid #2

Cyclic compressive and tensile stresses, contributing factors for die failure, are projected against time as shown in Figure 50 and the stress contours at the end of heating cycle are illustrated in Figure 51.

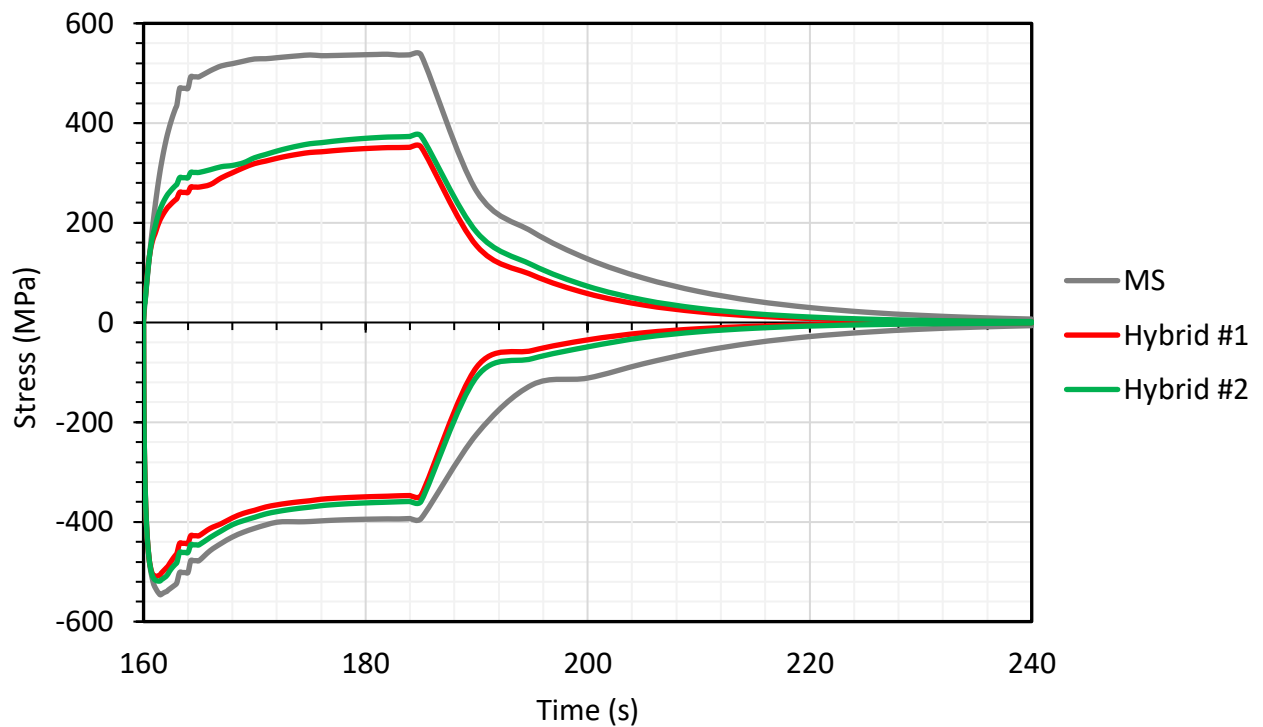


Figure 50: Maximum and minimum principal stresses

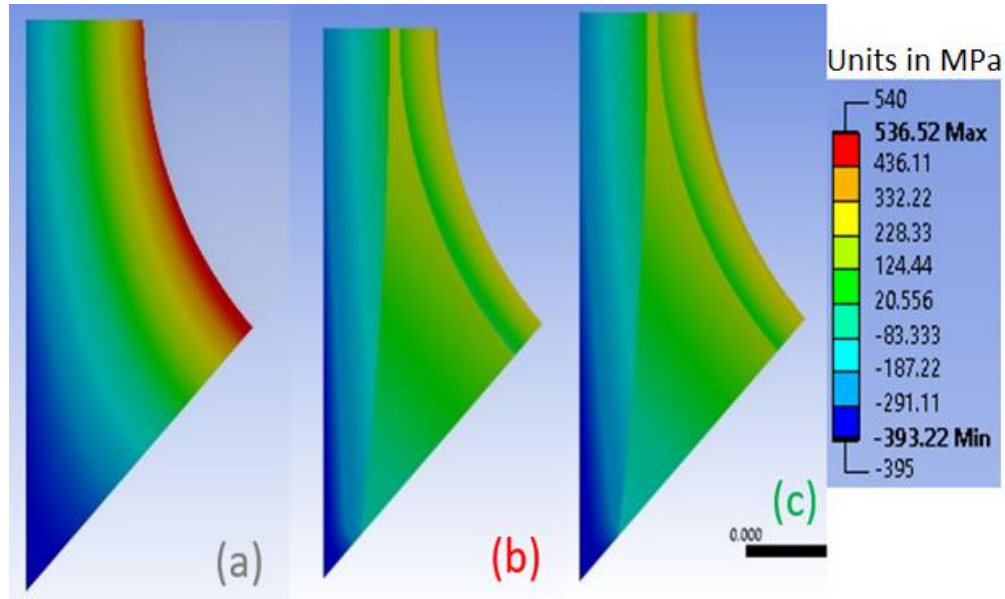


Figure 51: Principal stress(MPa) contours at 185 sec for (a) MS, (b) Hybrid #1 and (c) Hybrid #2

4.8 Discussion

The thermal findings indicate that the utilization of multi-material tooling can mitigate the two primary factors influencing crack development and propagation: namely maximum temperature, and temperature distribution. The following observations can be drawn from the thermal and mechanical results presented above.

- In both hybrid scenarios, as illustrated in Figure 43, there is a reduction of at least 50°C in the maximum temperature. The thermal contours displayed in Figure 44 highlight this decrease in maximum temperature, particularly at the specimen's corners. This reduction is crucial in preventing crack initiation by ensuring that compressive stresses do not reach the yielding point.
- Observations from Figure 45, Figure 46 and Figure 47 clearly demonstrate that the hybrid tooling insert exhibits a more favorable response to thermal loading. This is not only

reflected in the approximately 75°C reduction in temperature difference between the hot and cold sides of the insert but also in the substantial decrease in temperature distribution on the hot side.

- Figure 48 illustrates slightly improved results in Von-Mises stresses during the up-shock phase for hybrid specimens compared to the homogeneous maraging steel specimen. Notably, due to enhanced heat transfer, a significant difference in Von-Mises stress is apparent by the end of the heating cycle, with a nearly 150 Mpa difference. In hybrid test pieces, stresses decline at a much faster rate, whereas in plain test pieces, stresses remain elevated due to poor thermal response, as depicted in the contours in Figure 49.
- The cyclic compressive and tensile stresses, which are significant factors contributing to crack initiation and growth, are graphically represented against time in Figure 50. While there is a slight improvement in compressive stresses, the tensile stresses see a substantial reduction, decreasing by as much as 150 Mpa during the heating cycle. The distribution of principal stress within the geometry exhibits significant improvement, as evidenced by the contours in the Figure 51.

Chapter 5: Case Study 2

5.1 Background

The second case study delves into the intricacies of the real casting process, exploring the efficacy of multi-material tooling for an insert experiencing thermal cracking challenges. An approximation closely resembling a real-life casting insert is depicted in the Figure 52 below. The insert observed premature failure attributed to crack formation at the saddle area, induced by thermal shock. During the heating phase of melt injection, in the saddle area, thermal expansion causes development of compressive stresses. In cooling phase of coolant spraying, sudden temperature drop develops tensile stresses in the saddle initiating cracks. These cracks develop further with the progression of casting cycles.

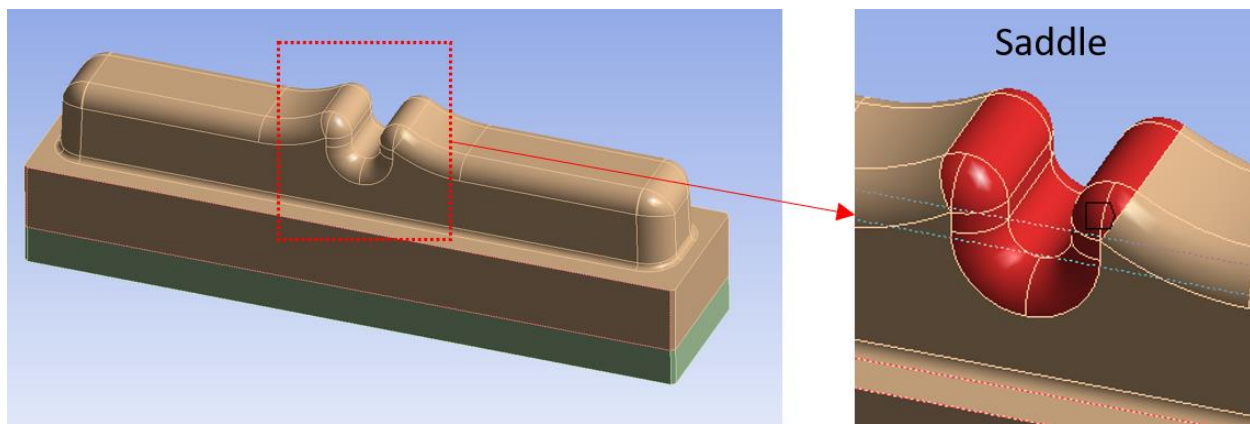


Figure 52: Die casting insert highlighting crack formation zone

Computational calculations are performed for three distinct scenarios as listed below, and their effectiveness in reducing thermal cracking is compared in this work.

1. Conventionally manufactured insert with drilled cooling lines
2. Additively manufactured insert with conformal cooling lines

3. Additively manufactured hybrid insert with conformal cooling lines

5.2 Geometry

The geometry consists of insert as well as a base plate, upon which insert is mounted by means of 4 screws. The basic dimensions (in mm) for this insert are shown in the drawings below:

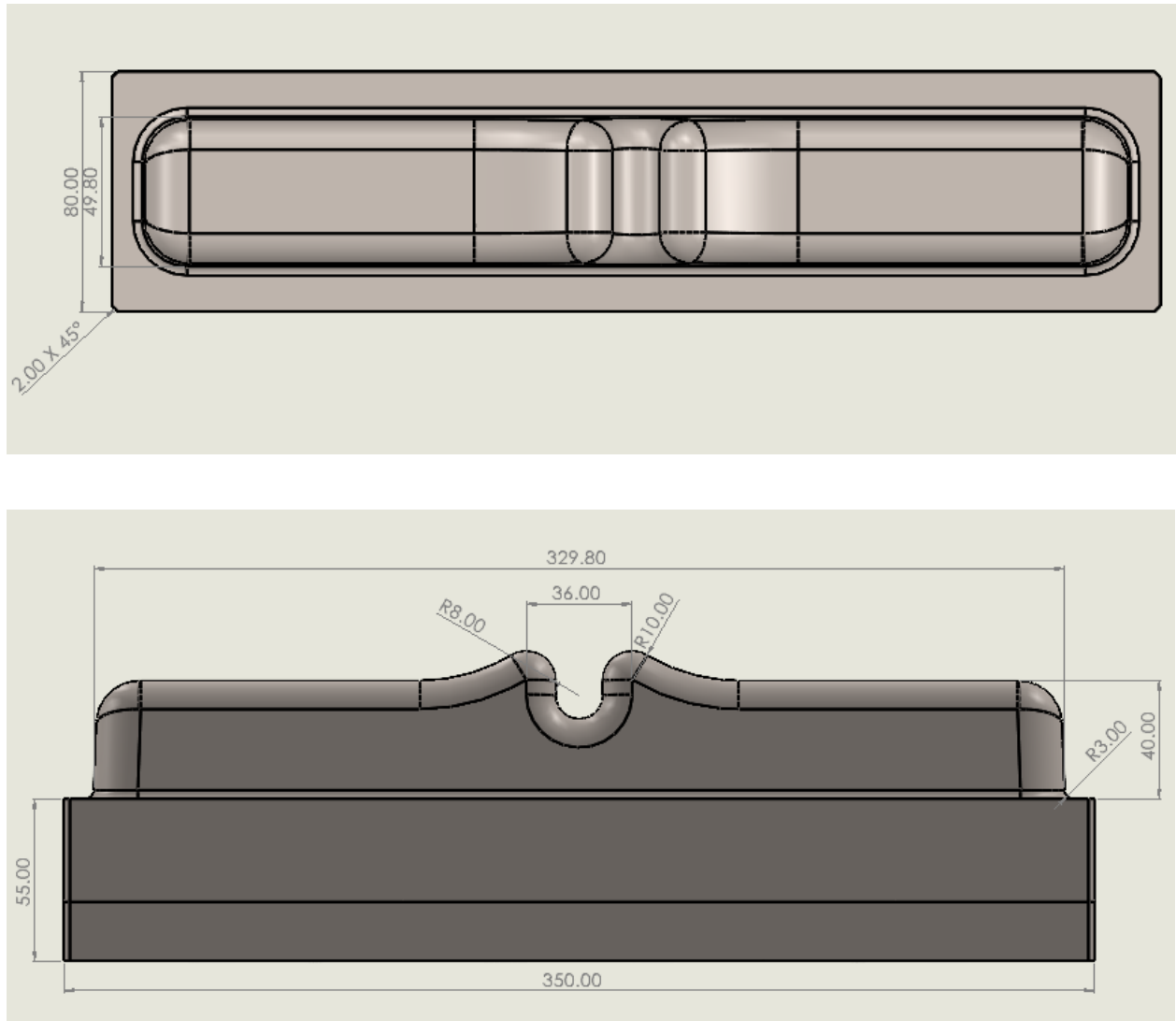
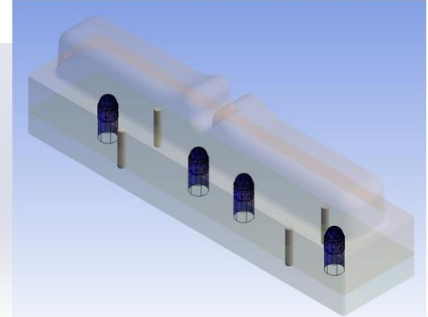
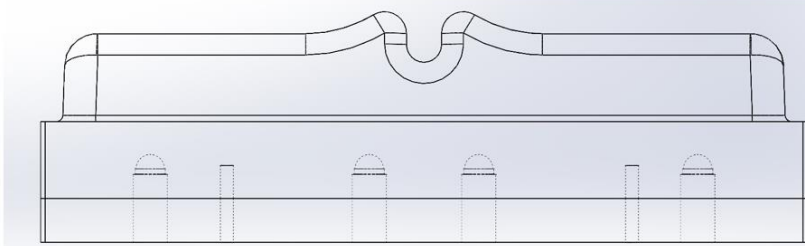


Figure 53: Basic dimensions of die insert

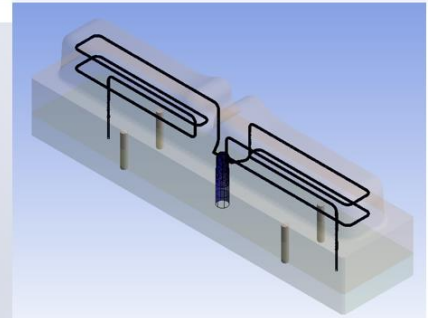
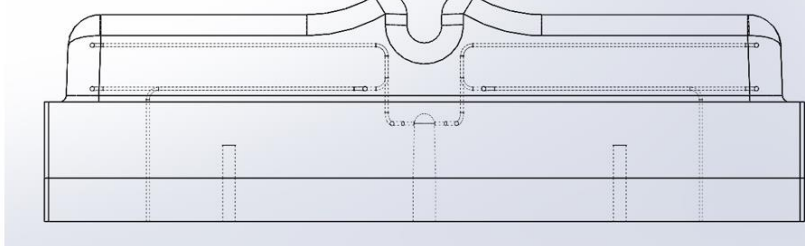
These three inserts, along with their corresponding configurations of cooling water lines, are visually represented in Figure 54, showcasing a front view wireframe drawing on the left and

3D view featuring cooling lines highlighted in blue color on the right side of the figure. Additionally, Figure 55 presents (1/4th insert geometry) a cross-sectional view to illustrate the Maraging steel outer layer with hybrid structure underneath along with cooling lines. These visual depictions are not proportionally scaled.

Conventional cooling



Conformal cooling



Hybrid with Conformal cooling

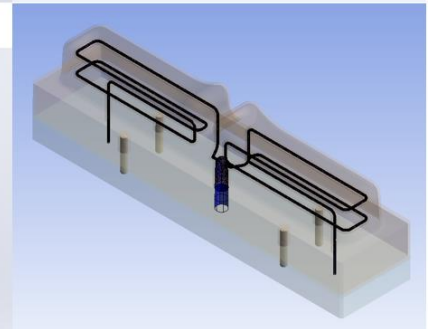
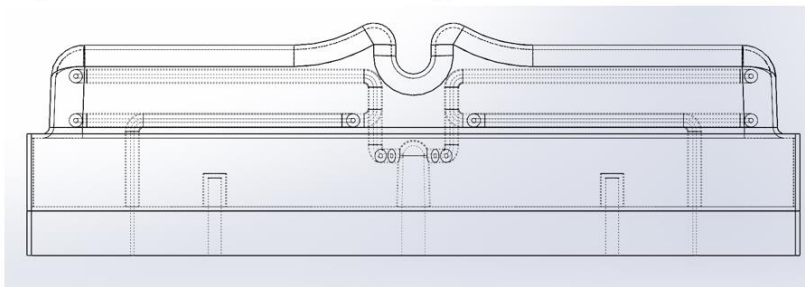


Figure 54: Tooling inserts: 2D wireframe (left) and 3D models (right)

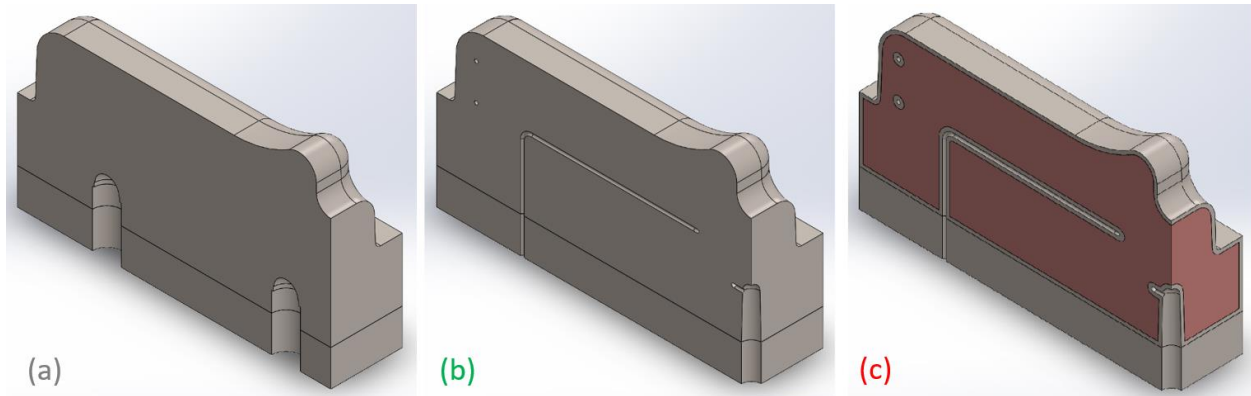


Figure 55: Cross-sectional views of tooling inserts: (a) conventional, (b) conformal and (c) hybrid

5.3 Material Properties

Material properties used in this simulation work are the same as in case study 1. The material properties data is shown in “4.3 Material Properties” above. Only the Hybrid #1 scenario has been calculated in this case study and referred to as Hybrid.

5.4 Mesh Plot

A detailed discussion regarding the mesh plot for this specific tooling insert, coupled with the accompanying mounting fixture plate, is provided in the *Table 5* presented below:

Table 5: Mesh plot for tooling insert

	Insert (Area of Importance)	Fixture Plate (Insignificant region)
Mesh Type	Tetraheadron (Better fit for curved surface)	Hex-Dominant (No significance)
Mesh Size	2.5 mm to 5 mm (fine mesh)	25 mm sized elements (coarse mesh)
Aspect Ratio	1 to 1.16 (high quality mesh)	N/A
Ordre	Second order mesh (better capture of deformation)	Second order mesh (better capture of deformation)

To capture the thermal response more accurately in the saddle area, the mesh size for the insert is set at 2.5 mm in the corresponding region. The mesh plot depicting this insert can be observed in the Figure 56 below:

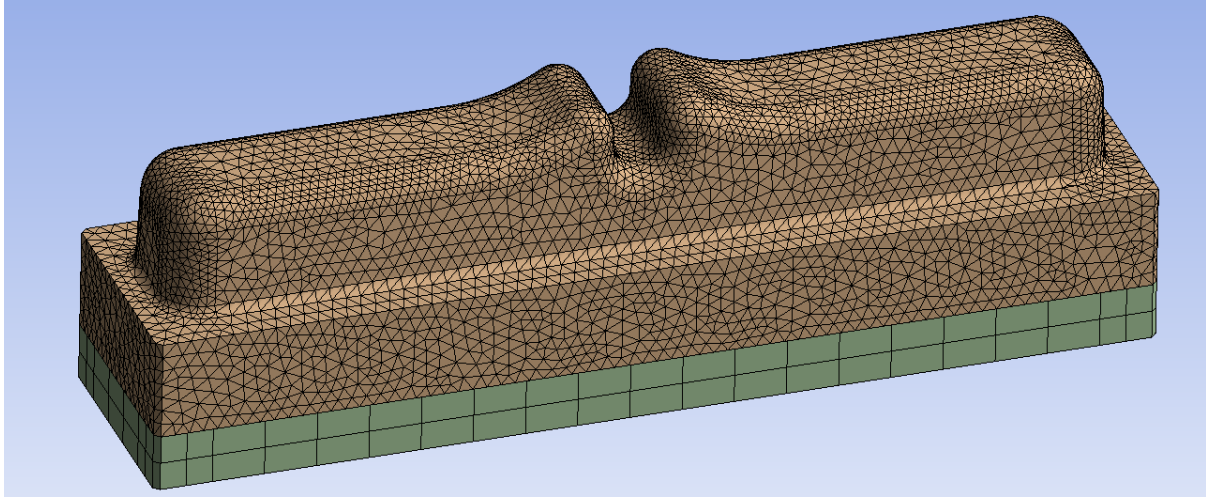


Figure 56: Mesh plot of the tooling insert

5.5 Boundary Conditions

5.5.1 Thermal Boundary Condition

In Figure 57 presented below, the thermal boundary conditions for the tooling insert are illustrated. The yellow-shaded region in the image denotes the surface in contact with the molten material. The blue region interfaces with the sides of the adjacent inserts. It is assumed that there is no heat transfer between the two inserts, consequently, the blue shaded region is treated as having zero heat flux throughout the cycle. Following that, the cooling lines are employed to consistently circulate water, facilitating the continuous removal of heat from the insert. The initial temperature of the insert is set at 20 °C. Contact surfaces between Maraging Steel shell and Hybrid TPMS core is given bonded connection. The computational repetition of the thermal cycle continues until both temperature and stress results reach a stabilized state.

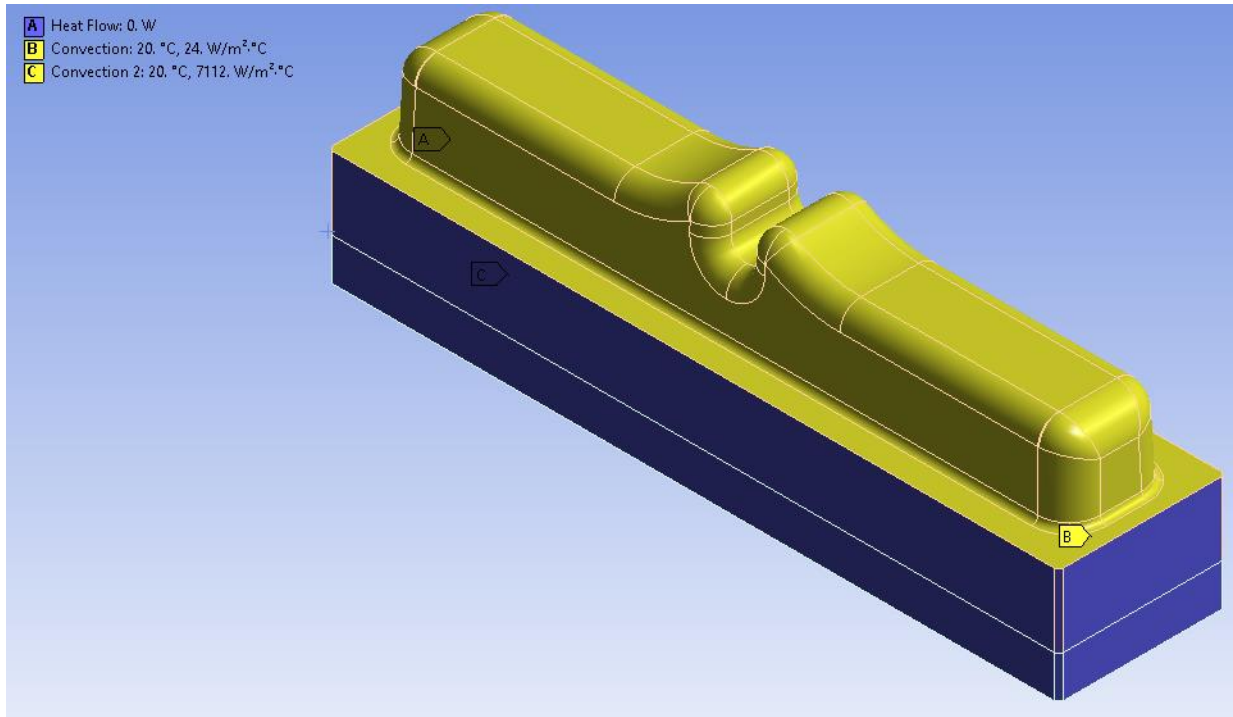


Figure 57: Visual of thermal boundary conditions for tooling insert

The Table 6 below highlights thermal boundary conditions for this simulation.

Table 6: Thermal boundary conditions for tooling insert

Cycle Steps	Duration (s)	Melt Contact Surface		Cooling water lines	
		T_{∞}^H (°C)	h_H (W/m ² · K)	T_{∞}^C (°C)	h_C (W/m ² · K)
Step 1: Melt injection and solidification	25	630	4,016	20	7,112
Step 2: Die opening and cast removal	2	20	24	20	7,112
Step 3: Coolant spraying	4	20	4,184	20	7,112
Step 4: Stabilizing	49	20	24	20	7,112

5.5.2 Mechanical Boundary Conditions

The mechanical boundary conditions for the tooling insert are depicted in Figure 58 below. Four beam joint connections, symbolizing bolts, are employed to secure the insert onto the fixture plate. One vertex of the fixture plate closer to a bolt joint is fixed to ground the geometry, preventing any movements of the vertex. Additionally, a thermal cyclic load is applied to the insert.

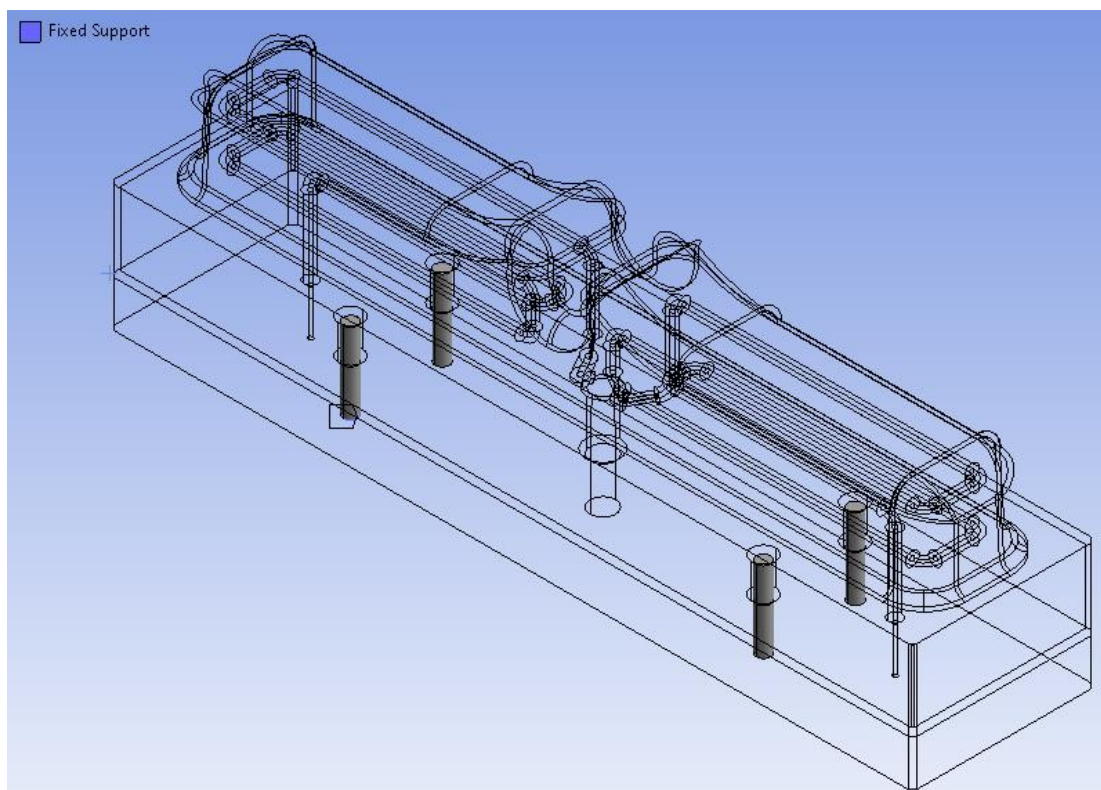


Figure 58: Mechanical boundary conditions for tooling insert

5.5.3 Analysis Settings

The computational calculations prioritize the saddle region of the insert, focusing on both temperature and stress development. Since this area is susceptible to crack initiation and propagation, the results will be obtained and compared specifically for this region.

5.6 Thermal Results

Maximum temperature at the saddle during one full casting cycle, after thermal stability, is represented in Figure 59 for the three different tooling insert cases. In addition, temperature profile at the end of heating cycle can be seen by contour plots at the saddle, throughout insert and at the cross-section in Figure 60, Figure 61 and Figure 62 respectively.

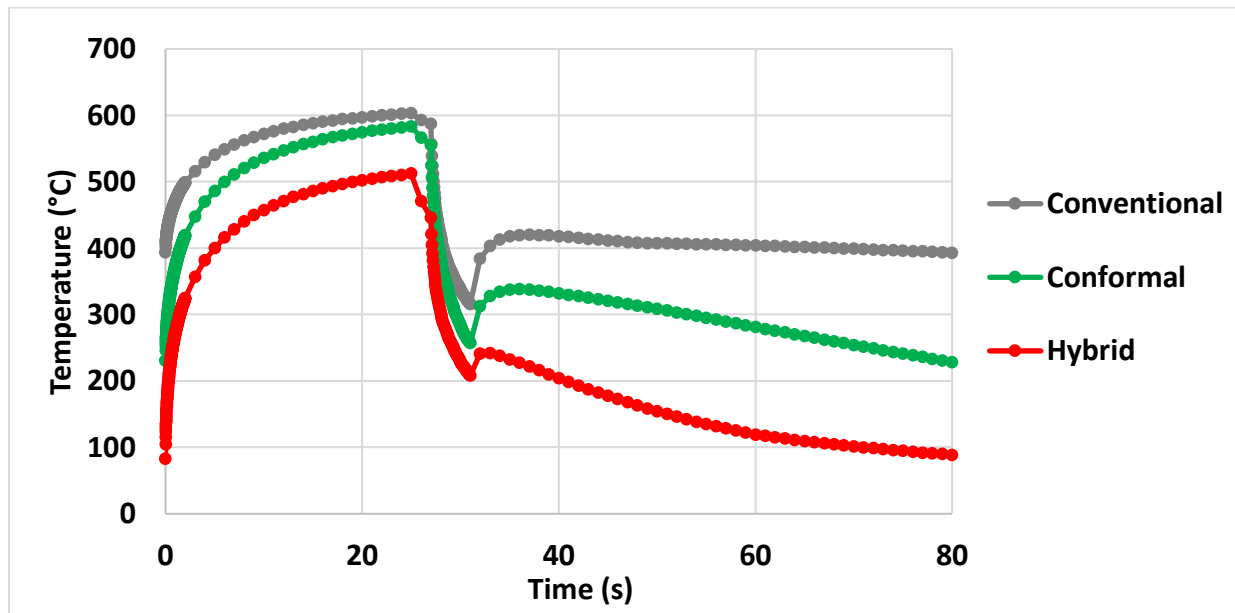


Figure 59: Maximum temperature at the saddle

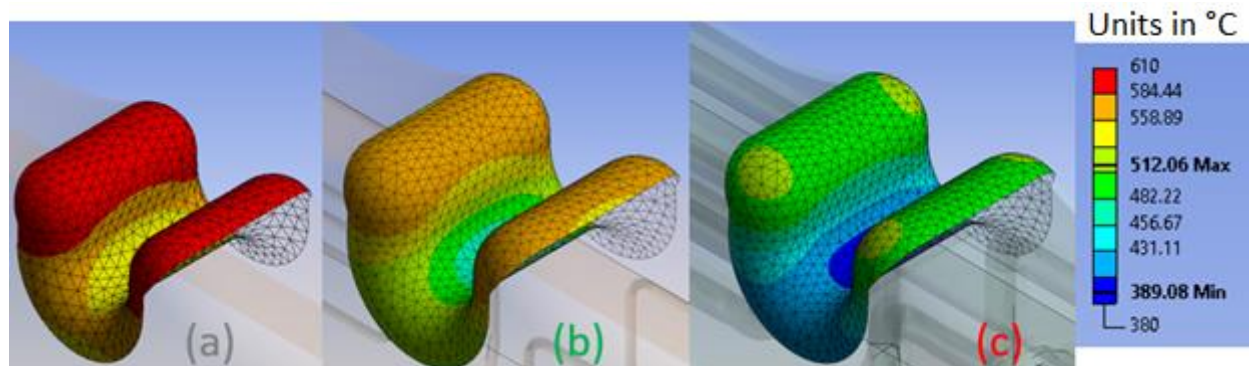


Figure 60: Temperature(°C) contours at 25 sec at saddle region for (a) Conventional, (b) Conformal and (c) Hybrid insert

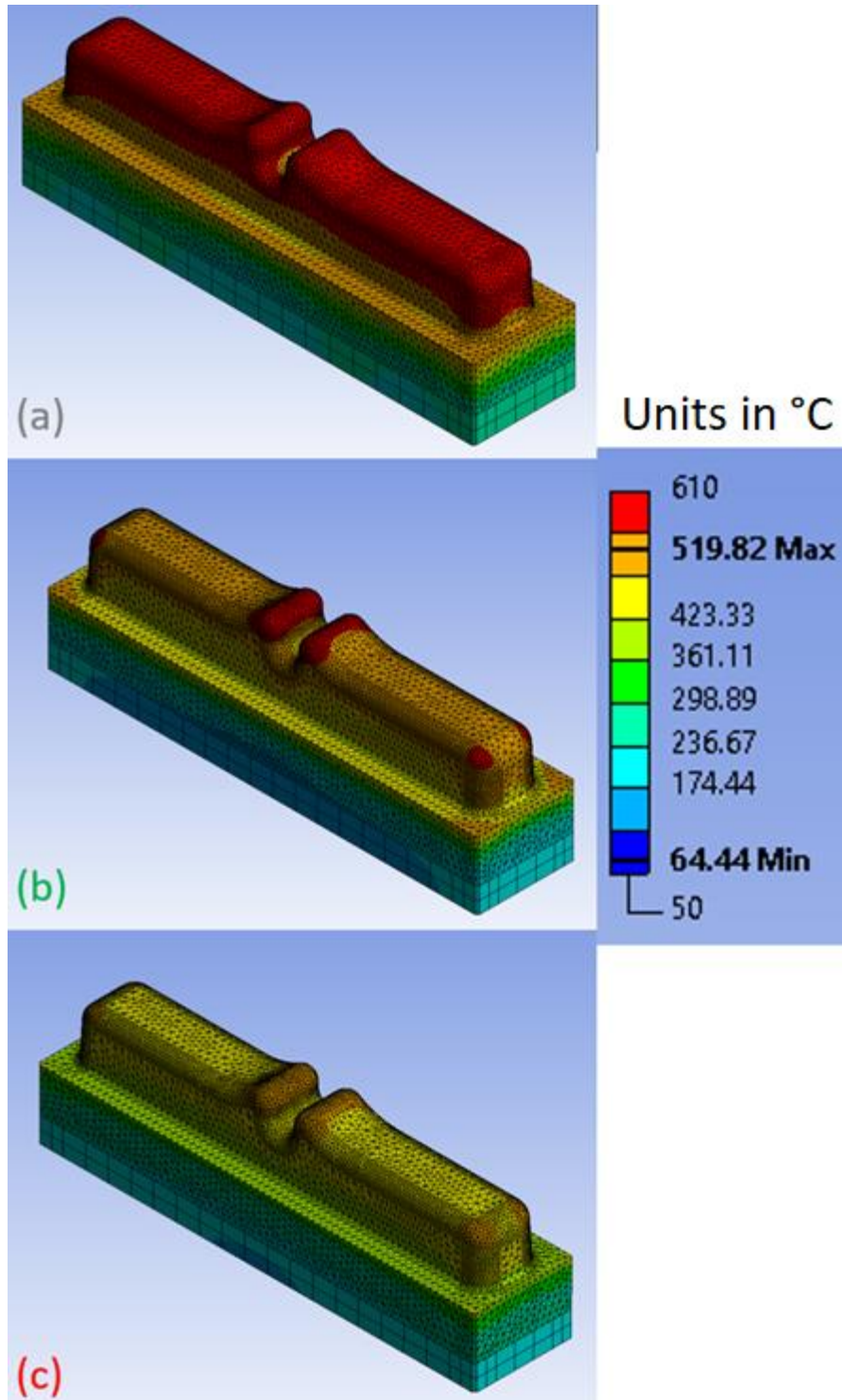


Figure 61: Temperature(°C) contours at 25 sec of the insert for (a) Conventional, (b) Conformal and (c) Hybrid insert

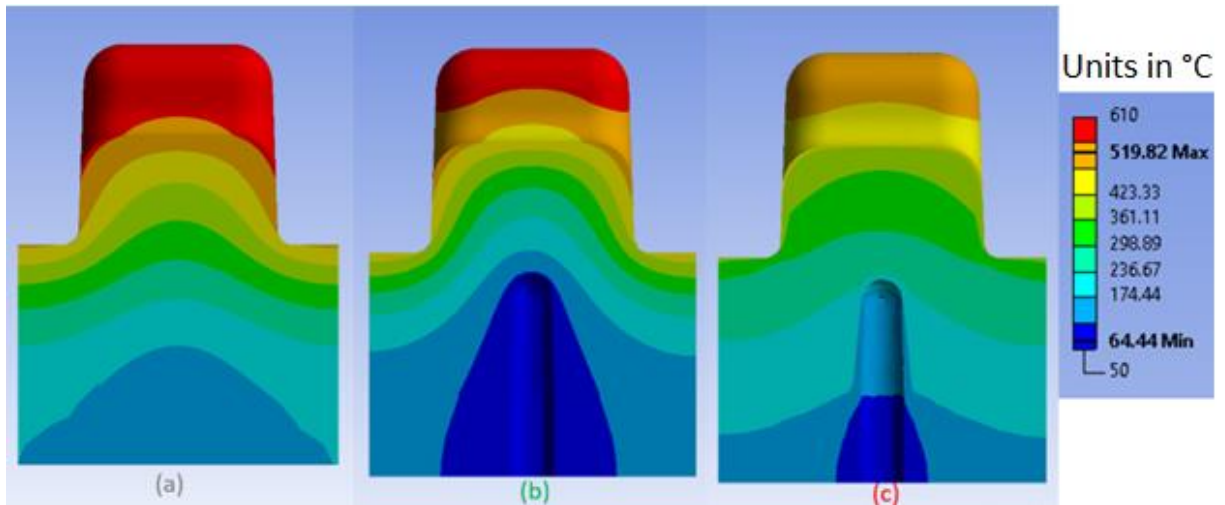


Figure 62: Temperature ($^{\circ}\text{C}$) contours at 25 sec at the cross-section for (a) Conventional, (b) Conformal and (c) Hybrid insert

Average temperature at the melt contact surface is with respect to time is presented in the graph shown in Figure 63 below.

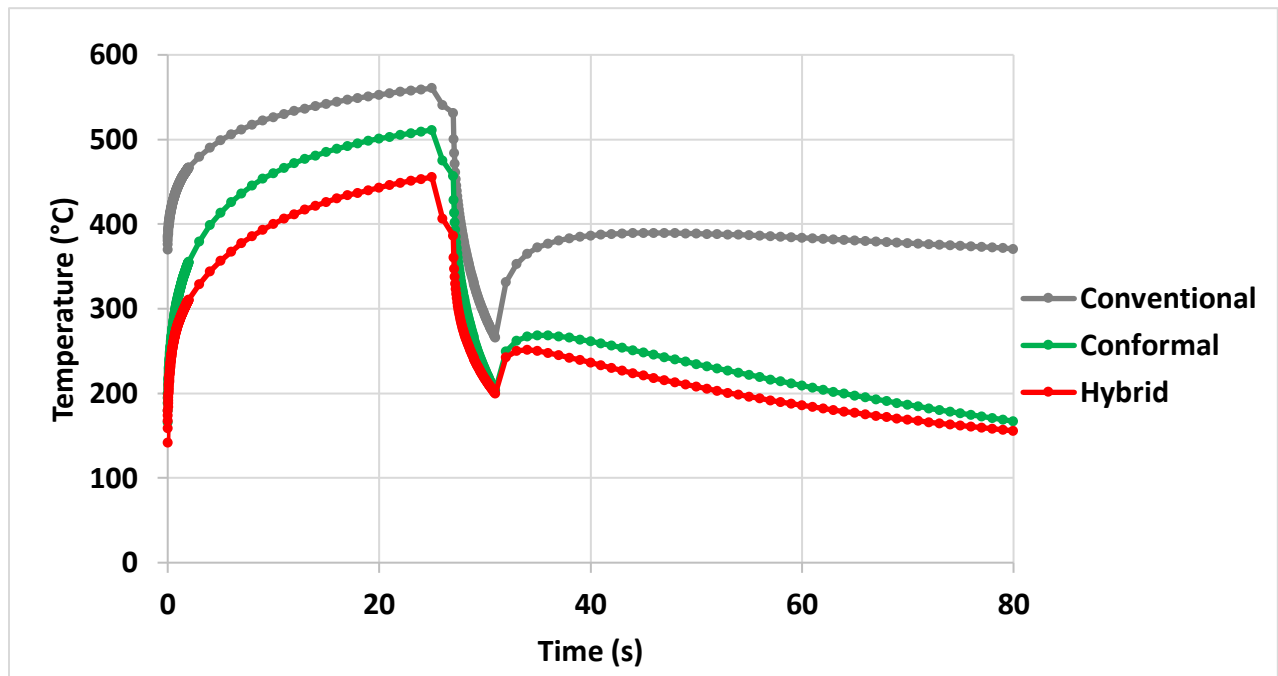


Figure 63: Average surface temperature at the melt contact surface

5.7 Mechanical Simulation Results

Maximum Von-Mises (Equivalent) Stress, probed at the saddle, is illustrated in Figure 64 for one casting cycle. The Figure 65 shows stress contours at the end of spray cooling (31 sec).

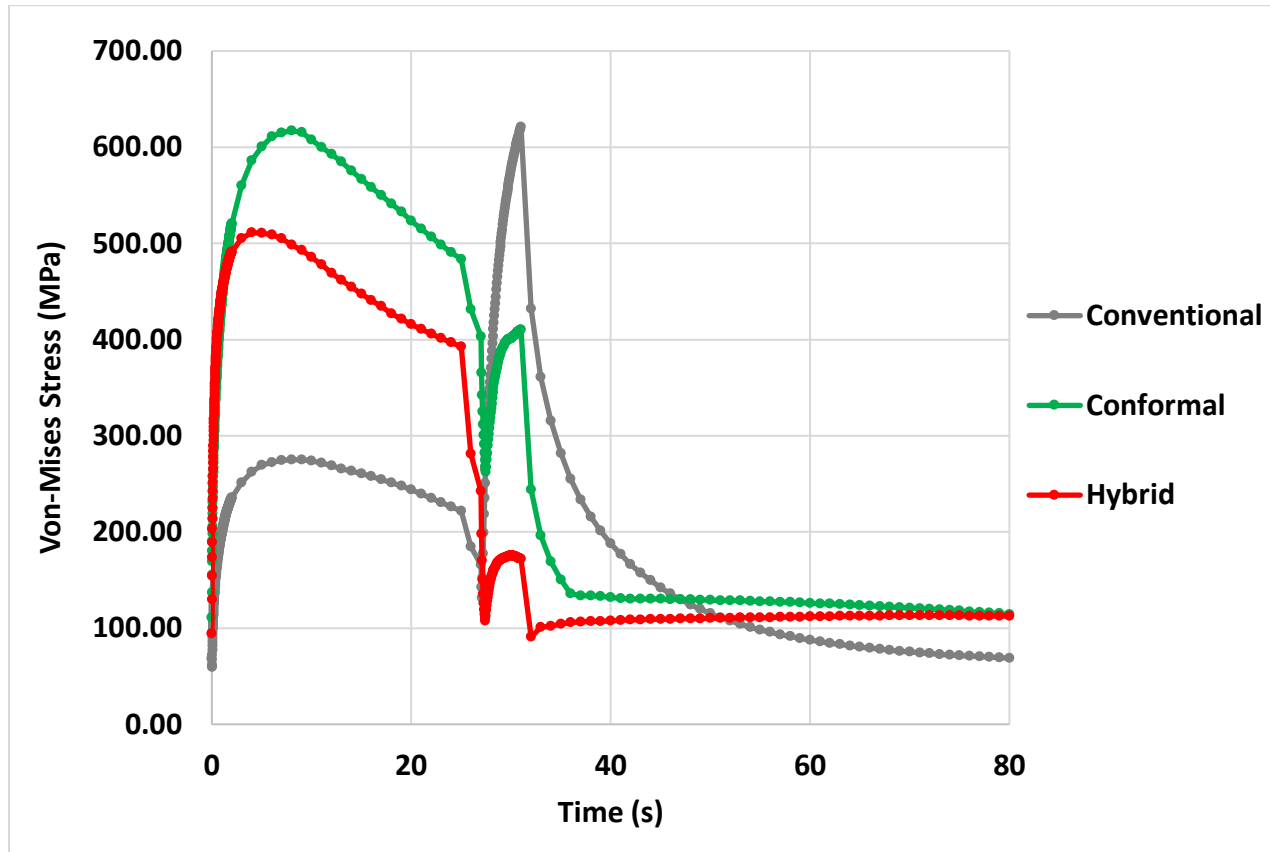


Figure 64: Von-Mises stress at the saddle

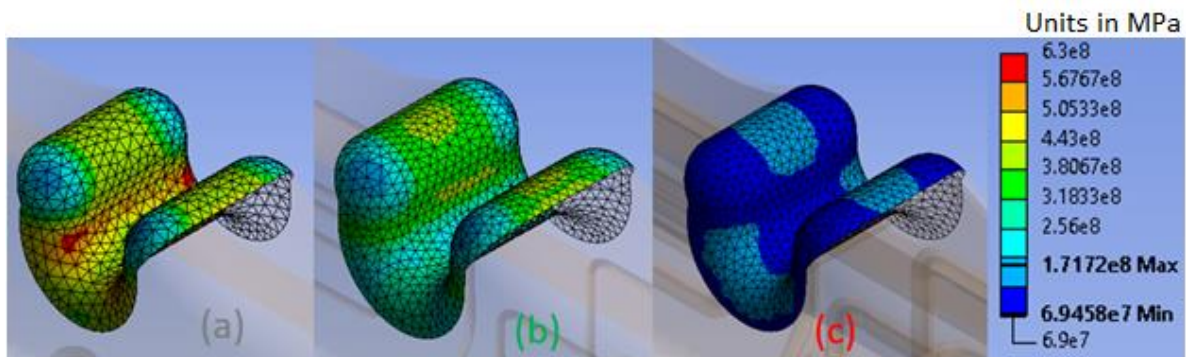


Figure 65: Von-mises stress(Pa) contours at 31 sec at the saddle of the insert for (a) Conventional, (b) Conformal and (c) Hybrid insert

Maximum Tensile stresses, significant factor for crack initiation and propagation, are plotted for saddle region as shown in Figure 66, as well as contours for the same at the end of spray cooling cycle are shown in Figure 67.

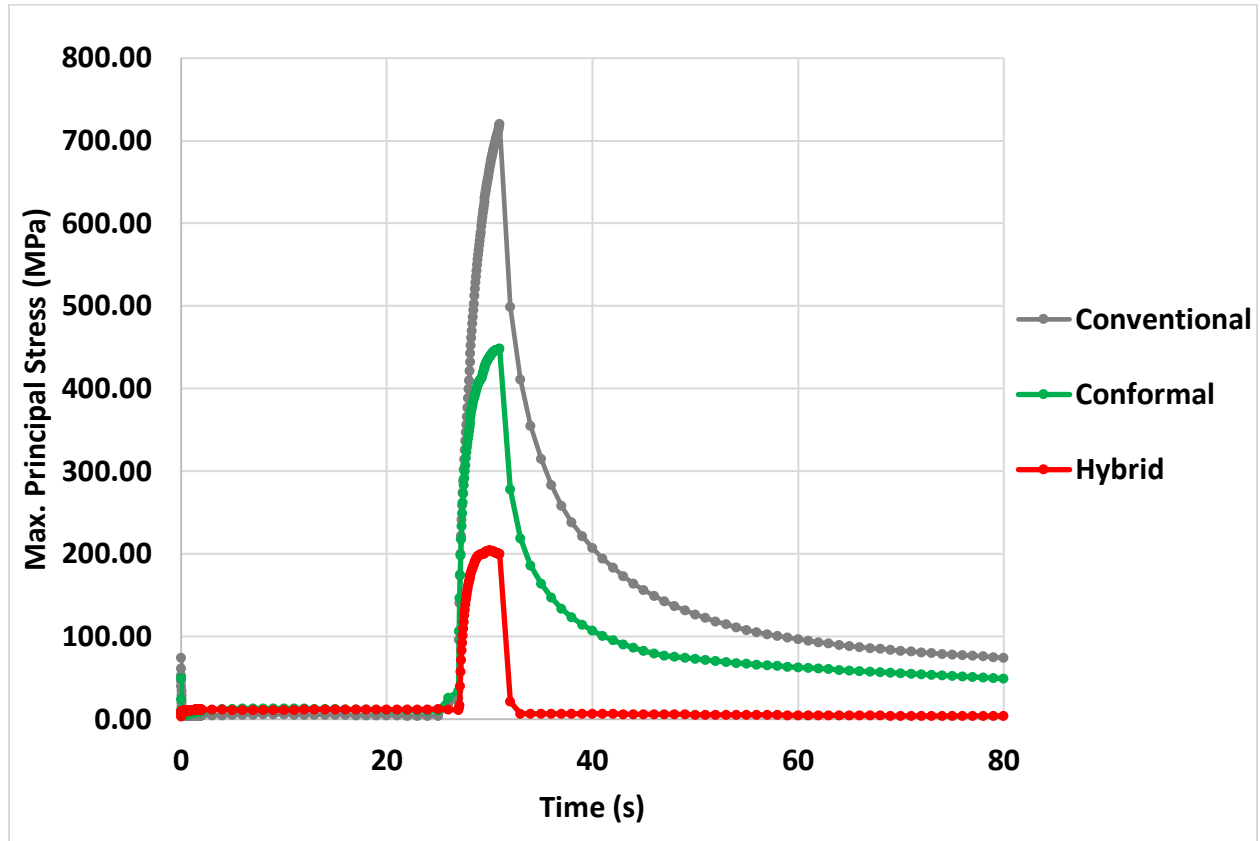


Figure 66: Maximum principal stress at the saddle

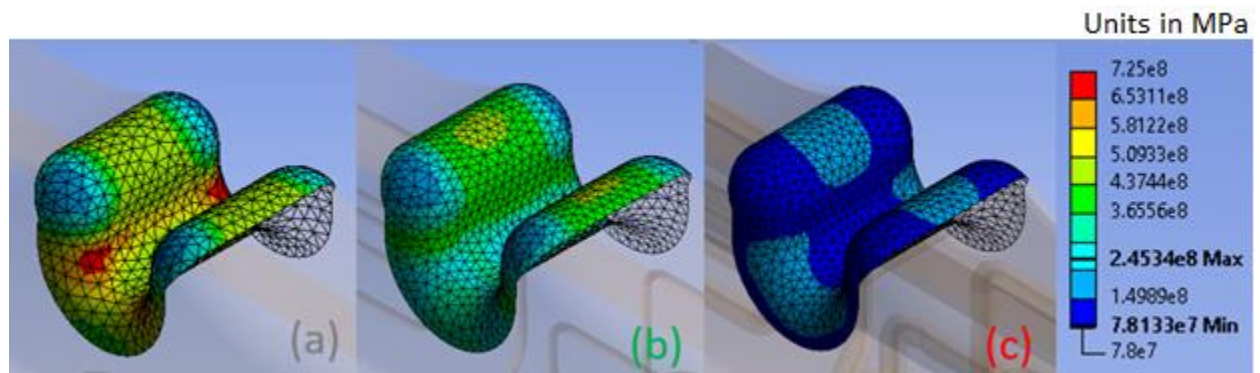


Figure 67: Principal stress (Pa) contours at 31 sec at the saddle of the insert for (a) Conventional, (b) Conformal and (c) Hybrid insert

Tresca Stress Factor at the node experiencing the maximum principal stress is determined by dividing the Tresca yield criterion (Maximum Principal Stress – Minimum Principal Stress) by the material's yield strength, represented as $SF = \bar{\sigma}_{Tresca} / \sigma_Y$. This Tresca Stress Factor throughout one casting cycle is plotted in Figure 68 below.

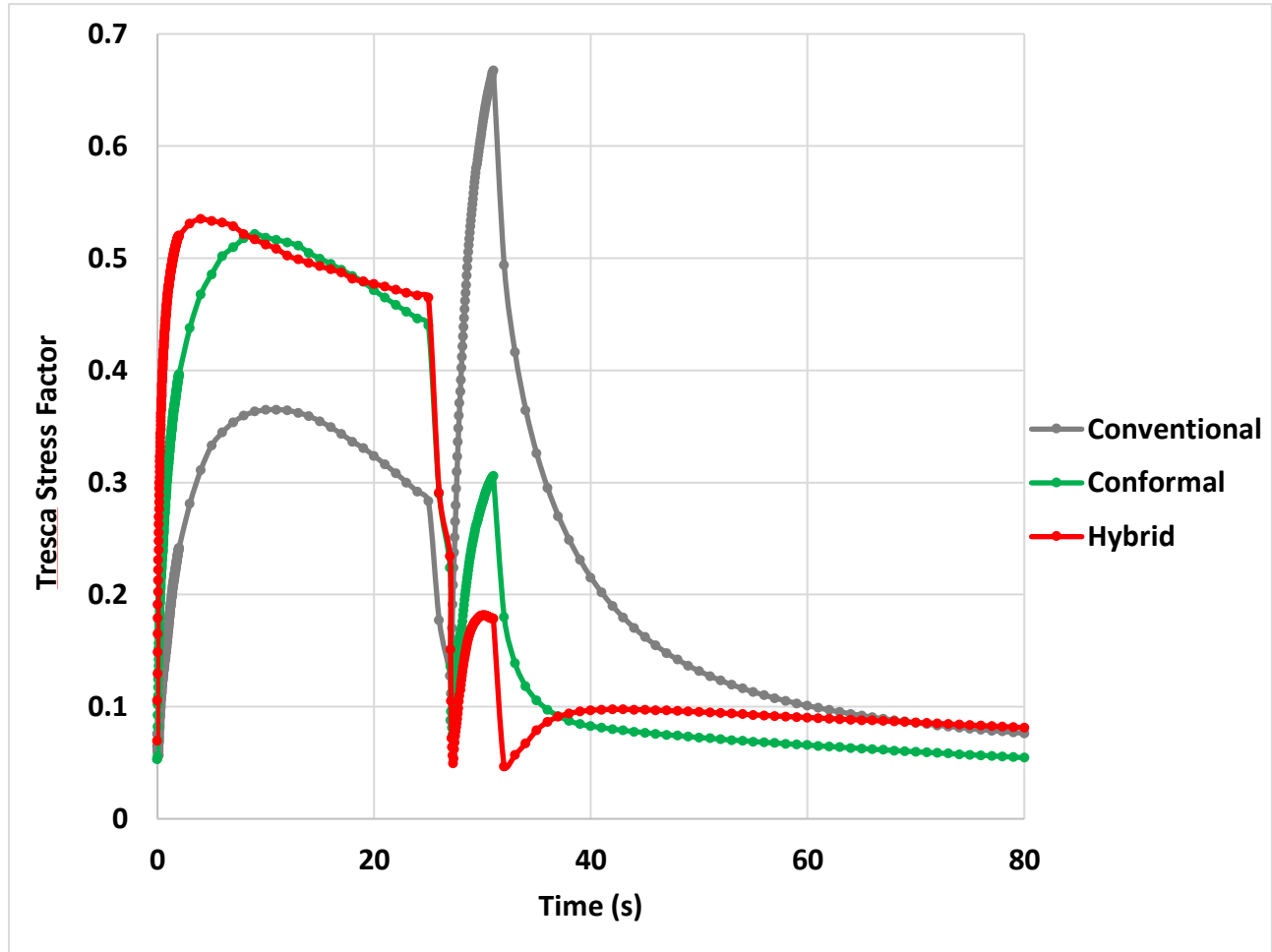


Figure 68: Tresca stress factor at maximum principal stress node

5.8 Discussion

The presented thermal and mechanical results above highlight key points, as discussed below, that affirm the efficacy of incorporating hybrid tooling.

- In Figure 59, the maximum temperature at the saddle region is depicted over the thermal cycle for three different scenarios. While additively manufactured conformal cooling channels contribute to a slight reduction in the maximum temperature, the incorporation of a hybrid core results in a significant drop of nearly 100°C in the temperature at the crack-prone saddle area.
- The temperature contours for the saddle area, the entire insert, and the cross-section at the saddle are illustrated in Figure 60, Figure 61 and Figure 62 respectively. These contour images clearly demonstrate that hybrid tooling not only lowers the maximum temperature but also enhances the temperature distribution throughout the geometry by improving the overall thermal response of the tooling.
- The decrease in average temperature at the melt contact surface, as illustrated in Figure 63, provides substantial evidence of the hybrid tool's effectiveness. This reduction showcases the hybrid tool's enhanced capability to dissipate heat more rapidly.
- As indicated in Figure 64, despite the observation of increased Von-Mises stresses in hybrid tooling during the heating cycle, a notable decrease in stress levels is achieved during the down-sock phase by spray cooling. This significant reduction in stress lessens the likelihood of die cracking by reducing tensile stresses, showcasing the effectiveness of hybrid tooling. The Von-Mises stress contours shown in Figure 65 indicate significant stress reduction at the end of spray cooling cycle.
- The plotting of tensile stress at the saddle is illustrated in Figure 66, while Figure 67 displays the contours of tensile stress at the conclusion of the spray cooling cycle. The

observations clearly demonstrate that hybrid tooling effectively lowered the tensile stresses by over 500 MPa.

- The Tresca Stress Factor (SF) is graphically illustrated in Figure 68. Utilizing conformal cooling through additive manufacturing, along with hybrid tooling that incorporates conformal cooling, appears to diminish the likelihood of crack formation in the tooling insert. This is achieved by maintaining the stresses consistently below the yield point throughout the entire die casting cycle.

Chapter 6: Conclusion

Early failure of dies in the die casting industry can often be attributed to the inadequate thermal conductivity of the tooling material, which results in thermal shock and cracking in the tooling inserts. In addition, lower thermal conductivity also contributes to higher surface temperature initiating soldering of aluminum onto the die surface. The use of additively manufactured hybrid tooling inserts, where a material with higher thermal conductivity is infused into the TPMS core of the insert, has demonstrated enhancements in the thermal performance of the casting tools.

In the two case studies examined, the implementation of hybrid tooling markedly reduced the peak temperatures within the specimens. Inserts maintained at cooler temperatures exhibit greater yield strength, thereby enhancing their resistance to cracking. Additionally, the integration of a hybrid core resulted in a more uniform temperature distribution. This uniformity minimizes the temperature gradients across the specimen, leading to the reduction of internal stresses.

Mechanical findings indicate a decrease in the stresses experienced by the insert throughout the casting cycle. The use of hybrid tooling can substantially lower the repetitive compressive and tensile stresses, which are a primary cause of die failure.

Chapter 7: Future work

1. Experimental investigations for both discussed cases in this study aim to compare and assess the accuracy of the computational findings.
2. Experimental assessment of the mechanical properties of the hybrid structure, consisting of two materials, aims to validate the calculations and gain insights into the interaction between the two materials during the casting process.
3. Integrating the hybrid tooling insert into the current production process to acquire more precise data regarding the performance of the hybrid insert and to provide comprehensive comparison with the data obtained from existing inserts.
4. Investigating how variations in process parameters, including melt injection temperature and pressure, along with heating and cooling durations, impact the performance of hybrid inserts with the aim of optimizing these parameters for improved efficiency of hybrid inserts.

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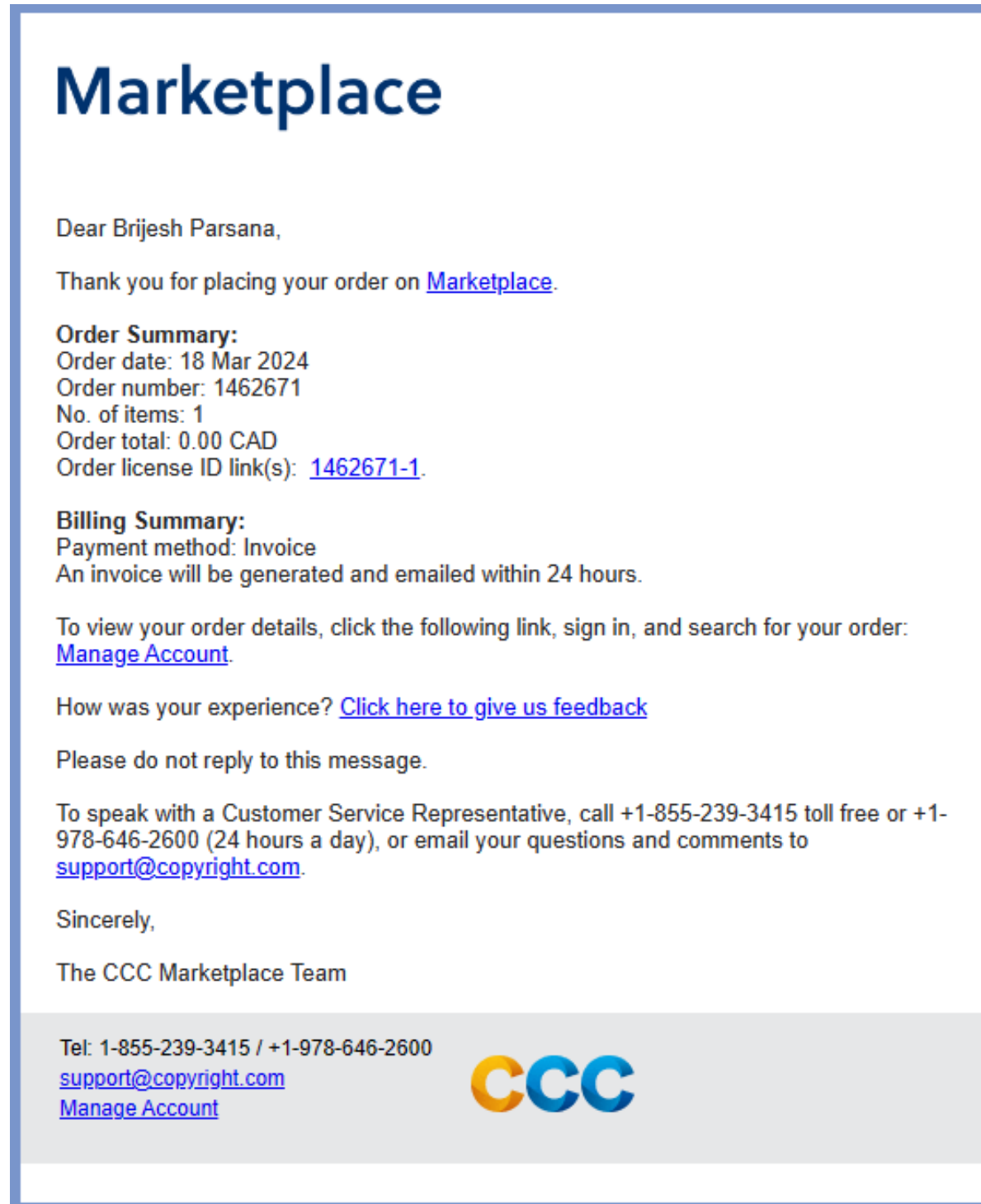


Figure 3

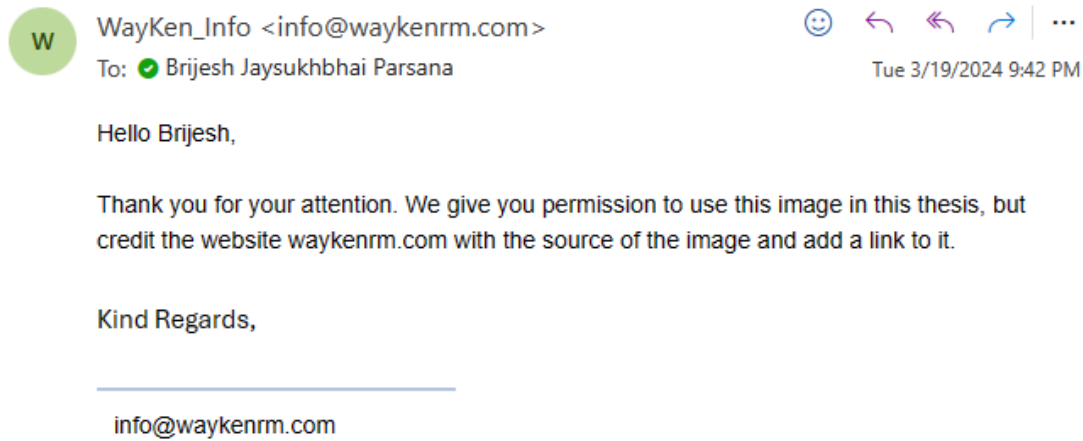


Figure 4

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Figure 5

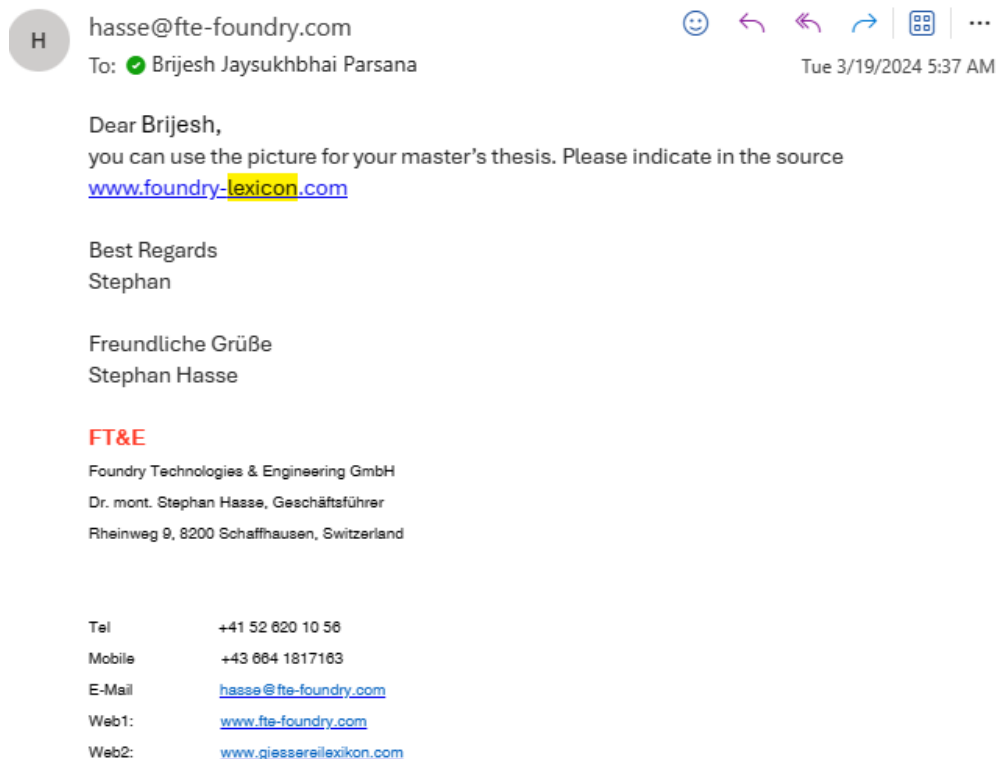


Figure 6



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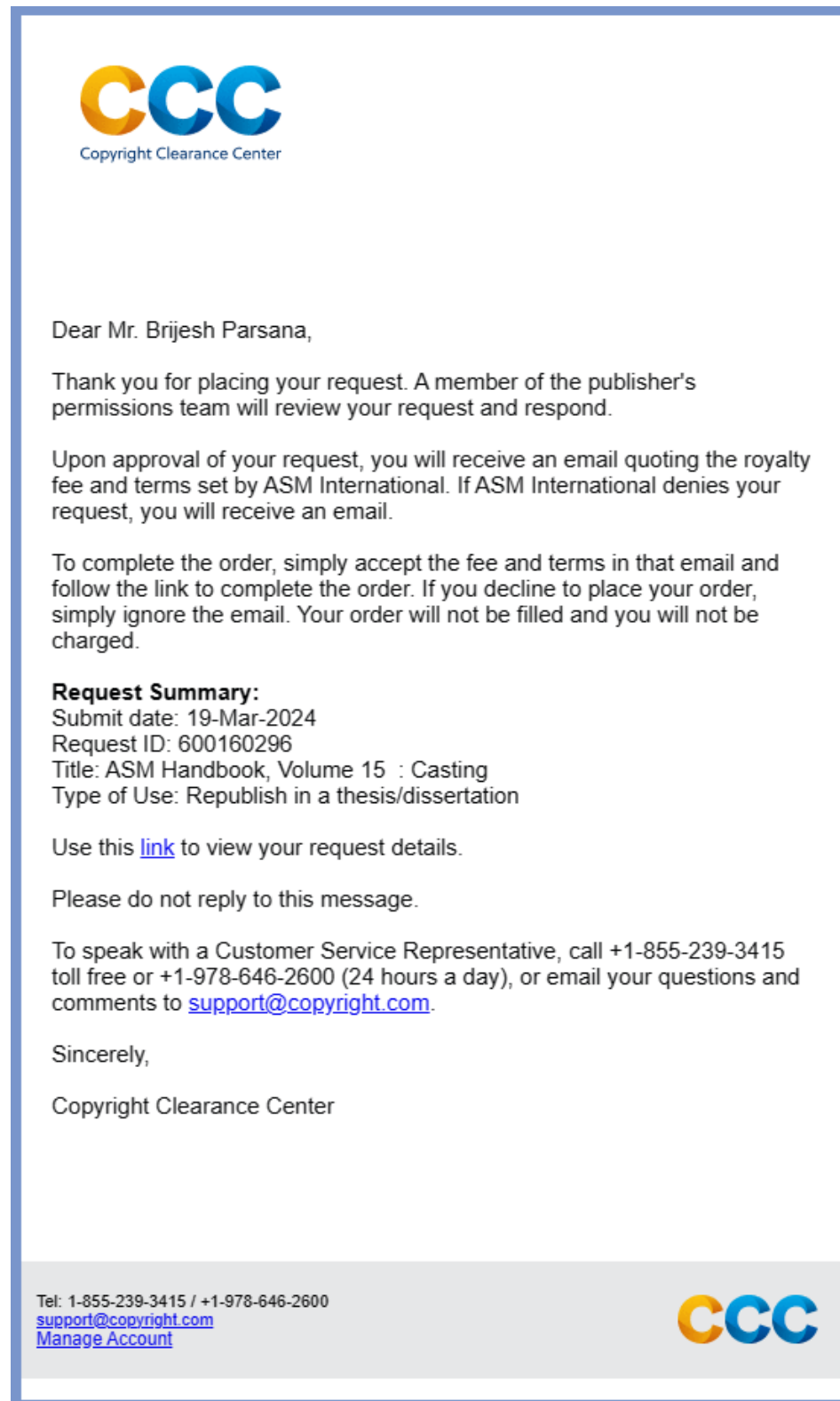


Figure 8

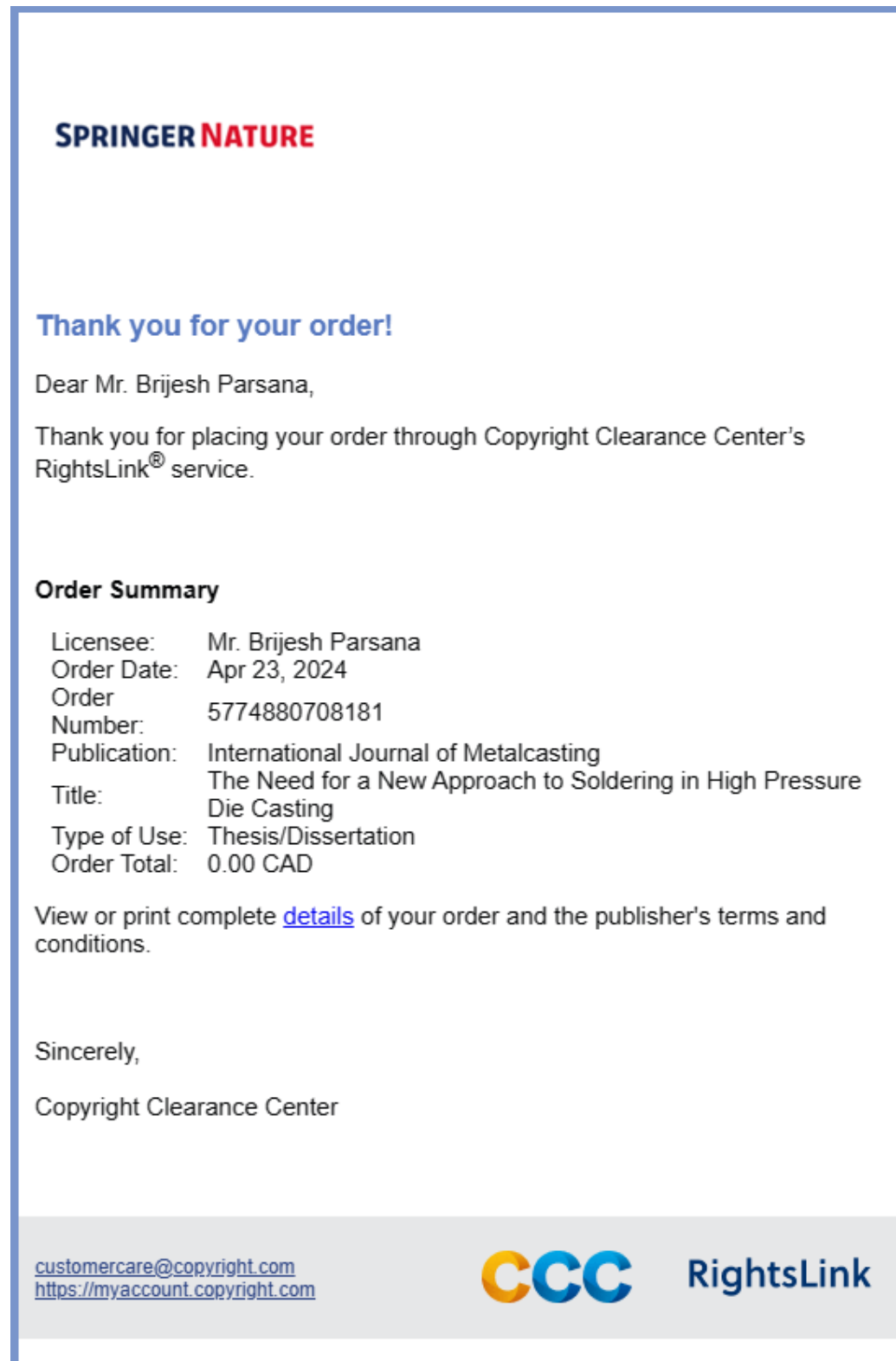
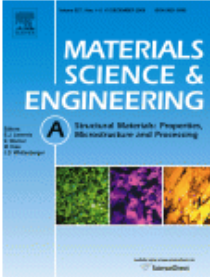


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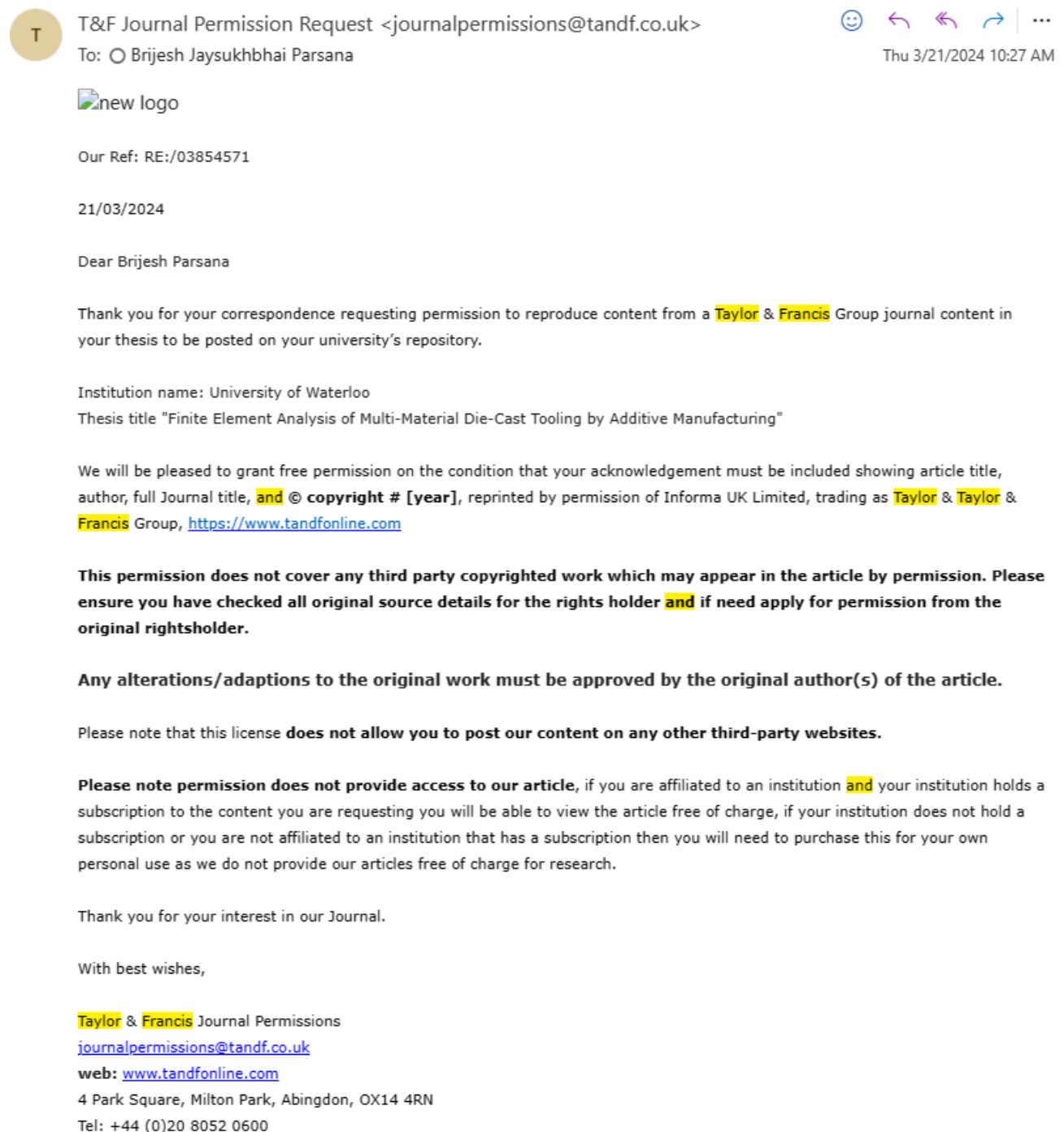


Figure 12 and Figure 13

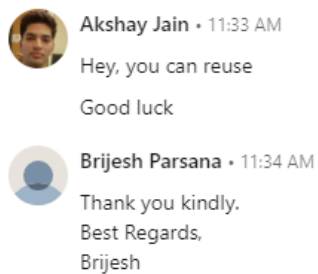


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Figure 16

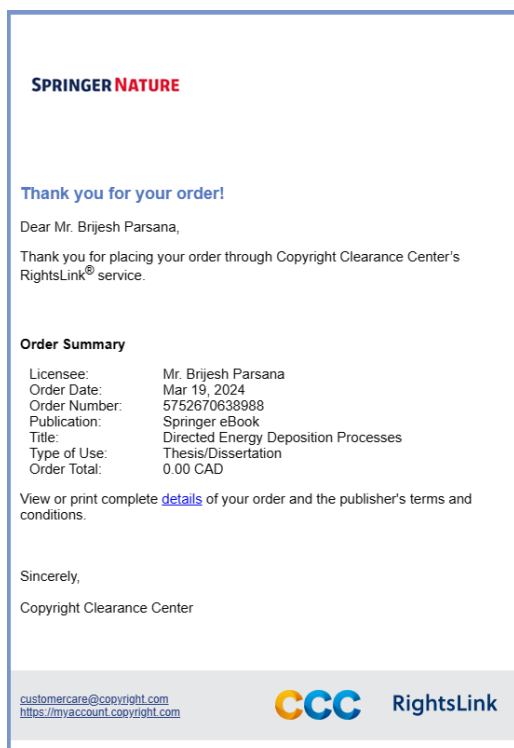
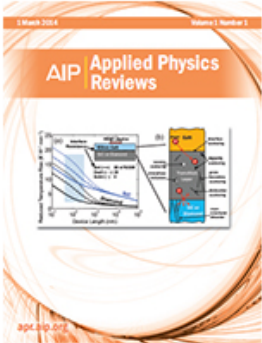


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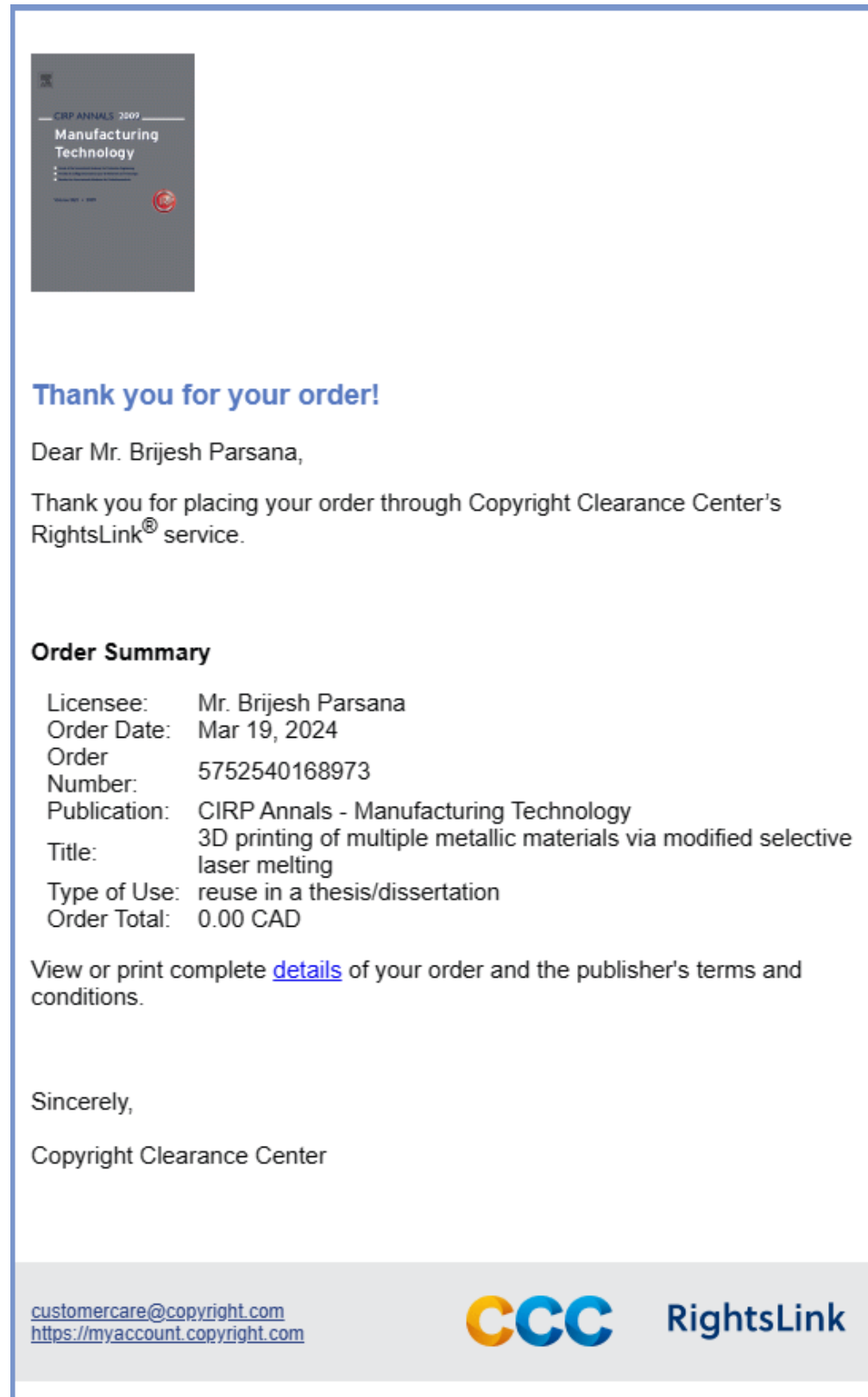


Figure 21

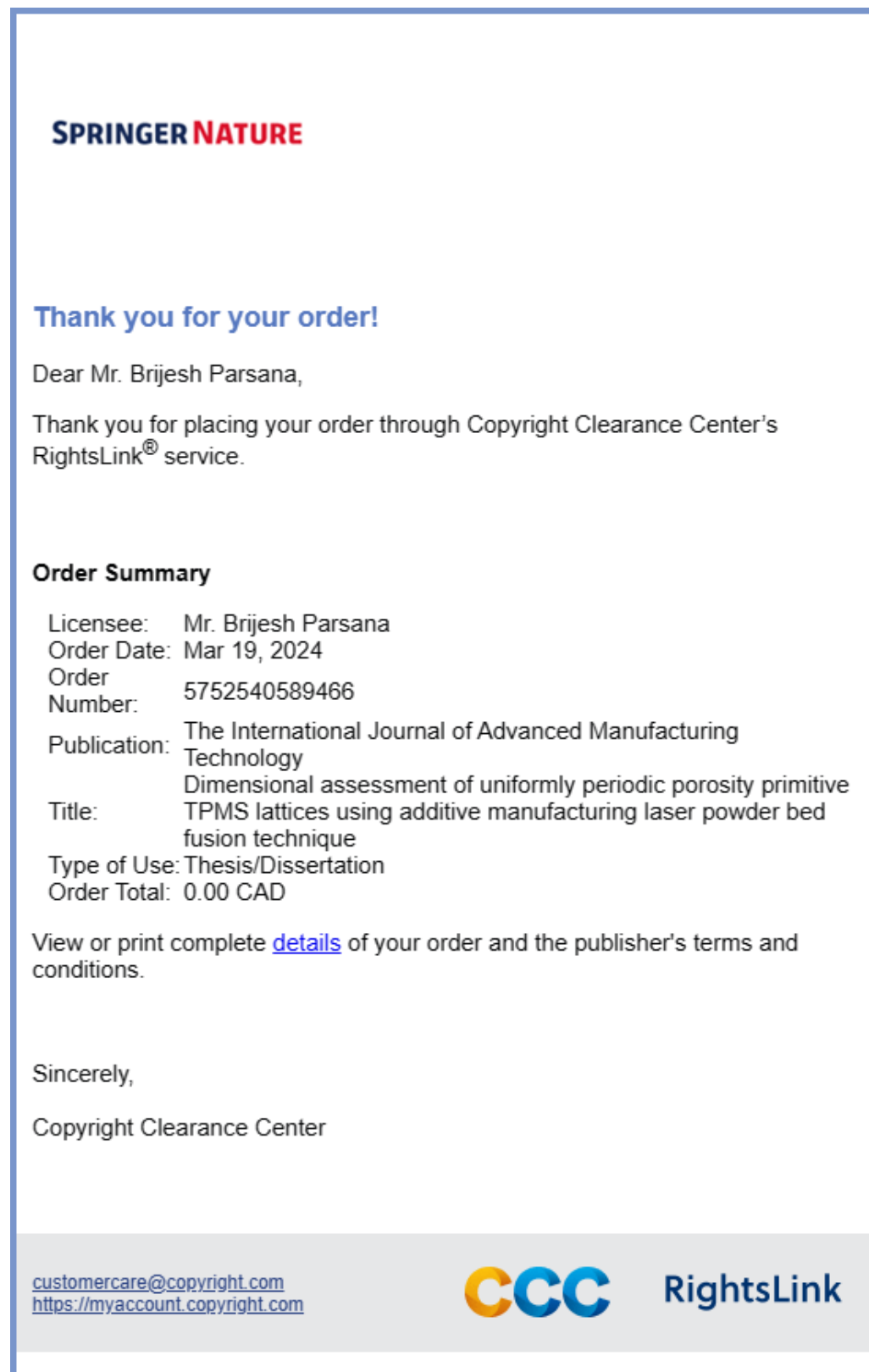
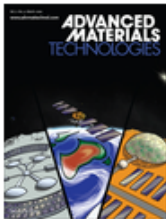


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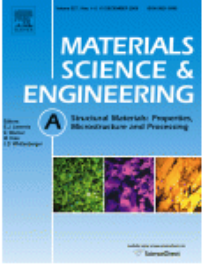
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