

**Three Dimensional Temperature and Residual Stress
Predictions in Machining**

by

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This is to certify that I have examined this copy of a master's thesis by

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ABSTRACT

The purpose of the first part of this study is to determine the three dimensional temperature fields on the chip, tool and workpiece during machining, which is one of the most important characteristic of machining processes; since the fields can affect other properties such as residual stresses and tool wear, and thus tool life and fatigue life of finished parts. The finite difference based model proposed in this paper offers very rapid solutions that are reasonably accurate with experiments. Finite difference based simulation results are validated with infrared thermal measurements which are determined from the machining of Al 7075, AISI 1050 and AISI H13 materials under various cutting conditions.

The second part of this study deals with the prediction of residual stresses in machining processes. To result in the residual stresses after machining, two-dimensional cutting temperatures are predicted on the chip, tool and workpiece, using the cutting forces as the cause for heat generation. An analytic elasto-plastic model is implemented on the superposition of thermal and mechanical stresses on the workpiece, and a relaxation procedure is applied, using kinematic and isotropic hardening together, resulting in the residual stresses on the workpiece. Results are verified with experimental data on residual stresses on Waspaloy, and accurate results are achieved, while the computational time to predict the residual stresses is immensely decreased, compared to FEM studies within literature.

ÖZET

Bu tez çalışmasının ilk bölümü, metal işleme sırasında talaş, kesme ucu ve parça üzerindeki sıcaklıkların tahminiyle ilgilidir. Bu işlemlerin sonucu olarak parçanın dayanıklılığında önemli rol oynayan kalıntı gerilimleri, ve kesme ucu dayanıklılığını etkileyen hızlı ve fazla aşınma oluşur. Bu tahmin de bu nedenle bu işlemlerin sonuçları açısından en önemli bulgulardan biridir. Bu tez çalışmasında önerilen sonlu farklar tabanlı model, bu tahminlerin yapılabilmesi için çok hızlı ve deneylerle paralel sonuçlar veren bir çözüm sunmaktadır. Bu modelin sonuçları, değişik kesme koşulları altında, Al 7075, AISI 1050 ve AISI H13 malzemelerinden yapılmış parçaların işlenmesi sırasında yapılmış olan kızılötesi termal ölçümlerle karşılaştırılmış ve iyi sonuçlar elde edilmiştir.

Çalışmanın ikinci bölümü, metal işleme sonrasında parçadaki kalıntı geriliminin tahmin edilmesine adanmıştır. Kesme sırasında oluşan kuvvetler, parçanın, kesme ucunun ve talaşın ısınmasına sebep olan ısı kaynakları oluştururlar. Bu kuvvetler kullanılarak ısı kaynakları bulunduktan sonra parçanın, kesme ucunun ve talaşın üzerindeki termal dağılım da bulunur ve bu termal dağılım, termal gerilimlerin bulunmasında kullanılır. Mekanik kuvvetlerden kaynaklanan mekanik gerilimler, termal gerilimler ile toplanarak parçanın üzerinde oluşan toplam gerilim bulunur, ve bu çalışmada geliştirilmiş analitik bir elasto-plastik model kullanılarak kesme ucu parçanın üzerinden geçerken gerilme-uzama eğrisinde bulunan noktaya göre malzemenin ne kadar kalıcı gerilmeye uğradığı hesaplanır. En son, bir relaksasyon prosedürü kullanılarak kesme ucu geçip gittikten sonra parça üzerinde ne kadar gerilme kaldığı hesaplanır, ki bu da kalıntı gerilimine eşittir. Sonuçlar, Waspaloy malzeme üzerinde yapılmış olan deneysel sonuçlarla karşılaştırılmış ve tutarlı sonuçlar elde edilmiştir. Model, FEM çalışmalarına göre, hesaplama süresini de ciddi miktarda azaltmıştır.

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Chapter 1

INTRODUCTION

Machining processes are very popular to study nowadays and many scholars have studied the mechanisms of them. Although there are many other aspects of machining processes that are of great importance, such as optimization of the processes and economics, this study is mainly based on the physical principles of machining. More specific focus of this study is the temperature field prediction and residual stress prediction during and after the machining process, mainly orthogonal machining.

The final product is generally produced using one of the machining processes these days, and with the help of brand new technologies using computers and sensors, and materials with better properties, it is getting even more widespread. Computational Numerical Control (CNC) machines are very helpful for machining processes such as turning, milling, drilling, boring, and grinding operations by subtracting material from a blank block of material.

CNC machines are used to control the motion of the machining tool and machined workpiece on a microscopic level of sensitivity, during the cutting motion between the cutting tool and workpiece material. The commands that are entered in the CNC machines are generated on Computer Aided Design/Manufacturing (CAD/CAM) systems, which determine the productivity and accuracy of the metal removal operation. Numerically controlled (NC) codes are generated according to many parameters of the operation such as the process parameters and cutting conditions, cutter geometry, tool and workpiece

materials, the rigidity of machine tool and the performance of the CNC unit, as well as the tolerance needs of the final product.

Turning and milling processes are very close to each other, except that milling is an interrupted machining process, where the machining tool has many cutter teeth on it and there is difference in contact within the time span. However, turning is an uninterrupted (continuous) machining process, where the contact between tool and workpiece does not change with time. This makes turning processes easier to investigate, which is why this study is based on turning instead of milling. However, the outcomes of this study might be used with slight revisions for other machining processes such as milling and drilling.

In turning operations, workpiece material is fixed axially on a shaft, but it turns along its longitudinal axis with a specified speed horizontally. The cutting tool, however, is fixed on its rotational axes and is only moving in linear axes, depending on the shape of the process. In orthogonal machining, where the calibrations are made to the tool-workpiece combination, workpiece material is a thin tube, and the cutting edge of the tool is bigger than the thickness of the tube, which means a full contact on the workpiece. The cutting tool is set in such a way that it is collinear with the longitudinal axis of the workpiece, distant from the center of the tube with an amount of the radius of the tube. It is then fed to the workpiece, which provides full support between the tool and the workpiece.

In this study, the force model of the previous studies has been taken as a starting point, and temperature distributions on the chip, tool and workpiece are calculated in three-dimensional space using a finite difference based model. Cutting forces that are calculated by the force model are used to calculate heat generation terms, which are considered as sources for temperature rise within the process. These sources are combined together to find the final state of the temperature distributions, which are of great importance due to their effect on the tool wear, which is the number one parameter in tool life calculation. Then, the temperature distribution on the workpiece is used to determine the residual

stresses on the material after the tool passes over, which is another important property, since it affects the fatigue life of machined parts substantially.

Chapter 2 of this study provides essential information within literature on turning operations, temperature prediction and experimentation systems, and residual stress prediction and experimentation techniques.

Chapter 3 explains the techniques used in predicting the three dimensional temperature distributions on chip, tool and workpiece, and the experimentation method used in measuring the temperature distribution during the process.

Chapter 4 describes how the residual stress predictions are made based on the two-dimensional temperature distribution predictions. Then, the experimentation technique used to measure surface and subsurface residual stresses after the machining processes is explained in detail.

Chapter 5 gives the results for both the thermal predictions and residual stress predictions, and the experiment results. Then, the simulations and experiments are compared and the simulations are verified, followed by the detailed discussion of the results.

The thesis is finalized with a short summary of the whole study, and the future work that is remained for next generation spirited scholars.

Chapter 2

LITERATURE REVIEW

2.1 Overview

This study consists of two main parts, where in the first part, the finite difference based three dimensional temperature prediction model is described, and experiments to validate the results of the model. Second part of the thesis is concerning the residual stress prediction after machining processes, and experiments corresponding to the simulations. There have been numerous amount of research on both of the fields of study, concerning both of the experiments and simulations, which are addressed in this part.

2.2 Temperature Prediction and Experiments

Over the past few decades, many academicians studied the characteristics of machining processes, using numerical, analytical and experimental techniques. Prediction and measurement of machining forces, temperatures, tool wear, residual stresses and many other characteristics are performed with substantial care, and many good agreements are found between numerical/analytical solutions and experimental data. Temperature field is one of the most important properties of a machining process; since the field can affect other characteristics such as residual stresses and tool wear.

Prediction of temperature distribution is generally engineered using finite element based models, which takes long time. Another way used widespread is to employ curve fitting: relating temperature distribution to cutting parameters and tool and workpiece properties by fitting a curve to temperature experiments. However, this method is not fully based on physical phenomena. Therefore, there is a need for rapid and accurate solution to the temperature prediction problem based fully on physical phenomena, which is found by using finite difference based model proposed in this paper.

It is known that the most widespread way to predict cutting temperatures on chip, tool and workpiece, in orthogonal machining, is FEM-based methods. Many studies on the issue [1,2,3,4,5,6] are present on simulation of several process outputs such as cutting forces, stresses, and temperature fields on chip, tool and workpiece, employing FEM based models. These studies show comparison of the results with experimental findings, reaching to acceptable conclusions. Grzesik [7] used analytical results as input to a FEM-based model to determine temperature distribution on uncoated and three-layer coated tools. Shatla et al. [8] predicted cutting temperature using FEM and analytical approaches, by determining the flow stress and investigating the effect of edge preparation on cutting forces, stresses and temperatures.

Many researchers also used algorithms like curve fitting to predict temperature fields and parameters leading to temperature fields in machining processes. Umbrello et al. [9] did such an analysis to find the heat transfer coefficient, and simulated the transient thermal conditions by a FEM based method and the artificially modified heat transfer coefficient [10]. Filice et al. [11] tried to predict wear in orthogonal machining using linear and polynomial models using a FE-based temperature prediction method. Yvonnet et al. [12] offered an inverse procedure to predict the temperatures. Kwon et al. [13] also used an inverse method to predict the average steady-state rake face temperature fields in cutting processes. They made this prediction by measuring the cooling state of the tool, and

inputting it to the spatial and temporal equations of heating of the tool as a boundary condition. Carvalho et al. [14] also proposed an inverse method to predict cutting temperatures, using a finite volume model to solve the three-dimensional heat diffusion equation.

There are also other analytical/numerical studies to predict the temperature field, average temperature or maximum temperature at the rake face [15-16], where an analytical approach was used while using multilayer coated tools in orthogonal turning. Chou and Song [17] developed an analytical model to predict cutting temperatures on the tool, and investigated the effect of temperature on the wear of the tool. Komanduri and Hou [18] modeled the heat flux due to shearing in the primary heat zone, and the temperature rise due to this heat flux. They resulted that the maximum temperature rise due to shearing happens at the shearing zone, far from tool-chip contact face.

Last, but most optimum way to predict temperature fields in cutting processes seems to be finite difference based models. These models offer considerably less computational time compared to FEM-based models with good accuracy, and more reliable results than curve fitting algorithms, since these methods offer analytical/numerical solutions. Lazoglu and Altintas [19] created such a two-dimensional finite difference based model to predict cutting temperatures in continuous (turning) and interrupted machining (milling) based on prediction of cutting forces, and primary and secondary zone heat generation terms. Ulutan et al. [20] used this method and improved it to predict residual stresses occurring on the workpiece after the tool finishes its way on it. Both studies compared their results with experimental findings and concluded that the numerical solution is in good agreement with measurements. Grzesik et al. [21] predicted the tool-chip interface temperatures in machining AISI 1050 steel, using an FDM-based model they created, and resulted that the simulations are in good agreement with measurements.

There are also studies to use measurement techniques to improve the results of simulations. Sreejith et al. [22] monitored the acoustic emission signals during machining, followed by a multiple regression analysis to model the temperature of machining. Experimentations alone can be observed within literature as well, to see the effect of some parameters mostly. Boud [23] conducted experiments while turning carbon steel bar to see the effect of bar diameter on temperature outcome.

Many researchers contributed in the issue by reviewing the studies. Recently, temperature measurement techniques and comparability of previous test results are reviewed, as well as ongoing research on heat generation and dissipation at the tool-chip contact face during orthogonal machining [24]. Filice et al. [25] investigated the capability of finite element models developed to predict the temperature field in machining, and compared two temperature measurement methods, namely thermocouple method and thermographic analysis. They also investigated the two dimensional thermo-mechanical and three-dimensional pure thermal types of numerical solutions. The analytical and experimental methods used to predict and measure temperature during machining are also reviewed [26].

In the first part of this thesis, prediction of three dimensional temperature fields in moving chip, stationary tool and moving workpiece during machining operations is presented; using a finite difference method based numerical model. The heat transfer from the primary and secondary heat sources (shear plane and friction face) is very crucial due to its brutal results. These results include, but are not limited to rapid wear of the tool, and tensile residual stresses on the surface of end product that cause low fatigue life. First, we study the chip and tool together to create a heat balance due to the friction on the rake face. Then, we investigate the effect of the temperature of chip and tool on the temperature field of the workpiece, taking into account the heat coming from the shear plane. A finite difference based model is employed to undergo this process, and it can be seen that the

results are in good agreement with experimental data presented in the literature. Additionally, the advantage of this method over FEM models is the computational time, which is decreased substantially; and the advantage over curve-fitting methods is that the method is fully based on physical explanation.

2.3 Residual Stress Prediction and Experiments

This part of the thesis has aimed to predict the residual stresses on workpieces after machining processes, and has had its start from previous studies within the literature [19,20,27] and other work within literature.

Residual stress is an important end product property in machining processes, since it is an effective parameter in fatigue life of machined parts. Parts that have tensile surface residual stresses need either extra tolerances, or post processing, which are time consuming, expensive and unwanted. It is important to predict the residual stresses occurring after machining processes, so that if tensile surface residual stresses are predicted, the process parameters can be changed and compressive residual stresses can be achieved, which provide longer fatigue life. The fatigue life of the parts can be even altered to find the optimum machining parameters [28].

There have been studies within literature to predict and measure the residual stresses in machining processes. However, predictions mostly include FEM-based models that take incredibly long time, and curve fitting methods, which are only valid for specific types of parameters, and are not fully reliable, since they are not entirely based on physical phenomena. Main studies of modeling efforts started with Merwin and Johson's work [29], and FEM based methods using rigid-plastic [30], elasto-plastic [31,32] and elastic-viscoplastic [33] models followed their footsteps.

There have been approaches to relate the residual stresses to many cutting, tool and workpiece parameters such as sequential cuts [33], tool rake angle [34], tool flank wear length [35], depth of cut [36], cutting temperature [37], feed rate [38] and cutting speed [36,38]. Also, although the main concern is in turning and milling operations, there have been studies to determine residual stresses in other machining operations such as grinding [39], hard turning [37,40,41], rolling [32], or electric discharge machining [42]. Curve-fitting methods [43,44] are also used to define a curve to the experimentally gathered data; but these methods are neither fully reliable, nor expandable, since they are valid for only the specific experiment, and close interpolations within it.

There is also a considerable amount of energy spent on the measurement of residual stresses during the last half-century, starting from the work of Henriksen [45]. Many techniques to measure the residual stresses were used, including X-ray diffraction [46,47], hole drilling [48,49] and deflection etching [43], where chronologically steel [46,48,50,51] was the most investigated material, followed by ceramics [52,53] and composites [54] with the advance of new materials. Many scholars also investigated the effect of cutting conditions, tool and workpiece properties on the residual stresses, including shear angle [55], length of the shear plane and shape of the cutting edge [50], tool nose radius [56,57], tool edge geometry [58], cutting speed [46,52,59], depth of cut [47,59], feed rate [46,51] and the use of coolants [59].

There are also new studies concerning the issue, to relate the cutting conditions on the residual stress patterns, especially on surface, like cutter orientation and workpiece angle [60], or machining conditions [61,62,63], tool wear [61], sequential cuts [62], tool geometry and presence of a cutting fluid [63]. Different types of machining are also investigated, with [64] modeling the evolution of welding residual stress after machining of the material, and [65] predicting residual stresses after quenching of aluminum blocks. Liang and Su [66] modeled the residual stresses analytically in orthogonal machining for

surface and subsurface predictions as well, using kinematic hardening condition. Umbrello *et al.* [67,68] made a study using the artificial neural network technique to predict the residual stresses after hard turning, based on the experimental and numerical studies prior to the work.

Last, there are also recent studies to measure the residual stresses. Carrera *et al.* [69] measured the residual stresses in cast aluminum engine blocks using a strain gauge technique, Outeiro *et al.* [62] measured the effect of many parameters on residual stresses using the X-ray diffraction technique, and Sharman *et al.* [70] measured the effect of tool material, geometry, wear and cutting conditions on the surface integrity and thus residual stress, after turning Inconel 718 using the blind hole drilling technique.

This paper suggests a finite difference based model, which predicts surface and subsurface residual stresses (until the stresses approach to zero) on both cutting and cross feed directions, within minutes of computation time.

Chapter 3

THREE-DIMENSIONAL TEMPERATURE PREDICTION AND EXPERIMENTS

3.1 Introduction

Determination of the three dimensional temperature fields on the chip, tool and workpiece during machining is one of the most important characteristic of machining processes; since the fields can affect other properties such as residual stresses and tool wear, and thus tool life and fatigue life of finished parts. There are studies within literature to predict the two dimensional temperature fields using fast methods, and three dimensional fields with generally FE-based methods; however, these methods are not the optimal solutions to the problem.

This chapter is dedicated to the three-dimensional temperature field prediction in machining processes. The finite difference based model proposed in this chapter offers very rapid solutions that are reasonably accurate with experiments. Finite difference based simulation results are validated with infrared thermal measurements, which are determined from the machining of Al 7075, AISI 1050, and AISI H13 materials under various cutting conditions.

3.2 Thermal Modeling of Chip, Tool and Workpiece in 3D

3.2.1 Heat Balance

Before starting to employ the finite difference method, analytical solution of the problem must be reviewed carefully. Everything concerning the heat balance comes from the first law of thermodynamics, which can be summarized as conservation of energy, or in a more explanatory way: Summation of the rate difference that thermal and mechanical energy enter and exit the control volume, and the rate of heat generation, is equal to the rate of energy stored within the control volume. In symbolic terms that explain the heat and mass transfer phenomena:

$$\dot{E}_{in} - \dot{E}_{out} + \dot{E}_{generated} = \dot{E}_{stored} \quad (3.1)$$

3.2.1.1 Heat Balance Equations of Chip

Considering this energy balance equation, if we define Q_x , Q_y and Q_z as the heat conduction entering the control volume from x, y and z directions respectively, and the control volume has dimensions of dx, dy, and dz, then the rates of heat conduction are defined as:

$$\begin{aligned} \dot{Q}_x &= -kA \frac{\partial T}{\partial x} = -kdydz \frac{\partial T}{\partial x} \\ \dot{Q}_y &= -kA \frac{\partial T}{\partial y} = -kdx dz \frac{\partial T}{\partial y} \\ \dot{Q}_z &= -kA \frac{\partial T}{\partial z} = -kdx dy \frac{\partial T}{\partial z} \end{aligned} \quad (3.2)$$

Here, k is the thermal conductivity of the workpiece. The heat conduction rates exiting the control volume from x, y, and z directions can be found from Taylor series expansion of the derivative of heat conduction rates and ignoring the higher order terms:

$$\begin{aligned}
\dot{Q}_{x+dx} &= \dot{Q}_x + \left(\frac{\partial \dot{Q}_x}{\partial x} \right) dx \\
\dot{Q}_{y+dy} &= \dot{Q}_y + \left(\frac{\partial \dot{Q}_y}{\partial y} \right) dy \\
\dot{Q}_{z+dz} &= \dot{Q}_z + \left(\frac{\partial \dot{Q}_z}{\partial z} \right) dz
\end{aligned} \tag{3.3}$$

These equations involve only the heat conduction terms. Since the study is three dimensional, and the system can be modeled as stationary with air flowing around, there is heat loss by convection from the system (chip, tool and workpiece) to surrounding air. This heat convection is included in the model as heat flow exiting from the control volume to the surroundings, if the control volume under consideration has contact with surroundings. Therefore, this heat convection rate is directed from the control volume to the surrounding air:

$$\dot{Q}_{z,conv} = hA(T - T_{\infty}) \tag{3.4}$$

Here, h is the convection coefficient between the chip and the ambient air flowing around. The temperature of ambient air is considered to be close enough to the room temperature; therefore room temperature boundary conditions are used. This term becomes zero if the control volume is not in contact with air, so special care should be taken while using the boundary conditions in finite difference form.

Since the heat generation in the control volume is equal to the volumetric heat generation rate times the volume of the control volume, and the amount of heat stored within the control volume is related to the density ρ , specific heat capacity C_p , and the rate of change in temperature,

$$\begin{aligned}\dot{E}_{generated} &= \dot{Q}dxdydz \\ \dot{E}_{stored} &= \rho C_p \frac{\partial T}{\partial t} dxdydz\end{aligned}\tag{3.5}$$

We can write the heat balance equation in chip as:

$$\dot{Q}_x + \dot{Q}_y + \dot{Q}_z - \dot{Q}_{x+dx} - \dot{Q}_{y+dy} - \dot{Q}_{z+dz} - \dot{Q}_{z,conv} + \dot{Q}dxdydz = \rho C_p \frac{\partial T}{\partial t} dxdydz\tag{3.6}$$

The differences of dimensional heat conduction rates become only a partial derivative of heat, which becomes the second derivative of temperature with the corresponding dimension. If we divide both sides by (k.dx.dy.dz), assuming that the heat conduction coefficient and heat convection coefficient are constant both with time and space, and define $\alpha = k / (\rho C_p)$, we can simplify the heat balance equation for chip as:

$$\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} + \frac{\dot{Q}}{k} - \frac{h(T - T_\infty)}{k\Delta z} = \frac{1}{\alpha} \frac{\partial T}{\partial t}\tag{3.7}$$

The equation is now in a form that finite difference method can be applied on, with second order approximations on spatial second derivatives, and first order approximation on temporal first derivative, which will be converted to a first order spatial derivative using the moving chip theory.

3.2.1.2 Heat Balance Equations of Tool

The same equation can be written for the tool employing the heat balance equation in cylindrical coordinates for a control volume of dimensions dr, rdψ, dz, and simplifying the corresponding elements and treating the heat conduction coefficient constant with time and space as:

$$\frac{\partial^2 T}{\partial r^2} + \frac{1}{r} \frac{\partial T}{\partial r} + \frac{1}{r^2} \frac{\partial^2 T}{\partial \psi^2} + \frac{\partial^2 T}{\partial z^2} + \frac{\dot{Q}}{k} - \frac{h(T - T_\infty)}{k\Delta z} = \frac{1}{\alpha} \frac{\partial T}{\partial t} \quad (3.8)$$

3.2.1.3 Heat Balance Equations of Workpiece

Workpiece is modeled as a rectangular prism, which is the same as chip model. This means that Cartesian coordinates are used to define the heat balance equations, same as 3D chip model:

$$\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} + \frac{\dot{Q}}{k} - \frac{h(T - T_\infty)}{k\Delta z} = \frac{1}{\alpha} \frac{\partial T}{\partial t} \quad (3.9)$$

3.2.2 Finite Difference Equations of Chip, Tool and Workpiece

Since it is hard to solve three dimensional heat balance equations analytically, and it is not even possible most of the times with conventionally used methods, it is important to apply numerical techniques such as finite element method or finite difference method. This part of the study deals with the finite difference equations of chip, tool and workpiece that are used to predict the temperature fields. The difference between the solutions of analytical and numerical solutions is that in numerical solution, the field is subdivided into smaller segments (grids). These elements generally have the same size and dimensions for simplicity. In Figure 3.1, a two dimensional grid is employed on the field and the temperature of the node under consideration is written with respect to the temperatures of the neighboring nodes. These neighboring nodes are shown in Figure 3.2, with a closer look to the specific node that we consider. Likewise, the first and second spatial derivatives of the temperature of the node under consideration can be written by using the temperatures of the same node and its neighboring nodes as:

$$\begin{aligned}
\frac{\partial T}{\partial x} &\cong \frac{T_{x+\Delta x, y, z} - T_{x-\Delta x, y, z}}{2\Delta x} \\
\frac{\partial^2 T}{\partial x^2} &\cong \frac{T_{x+\Delta x, y, z} + T_{x-\Delta x, y, z} - 2T_{x, y, z}}{(\Delta x)^2} \\
\frac{\partial^2 T}{\partial y^2} &\cong \frac{T_{x, y+\Delta y, z} + T_{x, y-\Delta y, z} - 2T_{x, y, z}}{(\Delta y)^2} \\
\frac{\partial^2 T}{\partial z^2} &\cong \frac{T_{x, y, z+\Delta z} + T_{x, y, z-\Delta z} - 2T_{x, y, z}}{(\Delta z)^2} \\
\frac{\partial T}{\partial r} &\cong \frac{T_{r+\Delta r, \psi, z} - T_{r-\Delta r, \psi, z}}{2\Delta r} \\
\frac{\partial^2 T}{\partial r^2} &\cong \frac{T_{r+\Delta r, \psi, z} + T_{r-\Delta r, \psi, z} - 2T_{r, \psi, z}}{(\Delta r)^2} \\
\frac{\partial^2 T}{\partial \psi^2} &\cong \frac{T_{r+\Delta \psi, \psi, z} + T_{r, \psi-\Delta \psi, z} - 2T_{r, \psi, z}}{(\Delta \psi)^2}
\end{aligned} \tag{3.10}$$

The Cartesian approximations were used for chip and workpiece, while the polar form approximations were used for the tool.

For the chip, Eq. (3.11) can be found using these approximations. Writing this equation in matrix form with nodal temperatures being the unknown, coefficients of the temperatures constituting the coefficient matrix, and the T^0 coefficients being the RHS of the equation, one can solve the matrix problem using Gaussian elimination (Eq. 3.12).

$$\begin{aligned}
&\frac{T_c(x+\Delta x, y, z) + T_c(x-\Delta x, y, z) - 2T_c(x, y, z)}{(\Delta x)^2} + \frac{T_c(x, y+\Delta y, z) + T_c(x, y-\Delta y, z) - 2T_c(x, y, z)}{(\Delta y)^2} \\
&+ \frac{T_c(x, y, z+\Delta z) + T_c(x, y, z-\Delta z) - 2T_c(x, y, z)}{(\Delta z)^2} - \frac{V_c}{\alpha} \frac{T_c(x+\Delta x, y, z) - T_c(x-\Delta x, y, z)}{2\Delta x} \\
&- \frac{hT_c(x, y, z)}{k\Delta z} = \frac{-\dot{Q}}{k} - \frac{hT_\infty}{k\Delta z}
\end{aligned} \tag{3.11}$$

$$[A]\{T_c\} = \{C\} \Rightarrow \{T_c\} = [A]^{-1}\{C\} \tag{3.12}$$

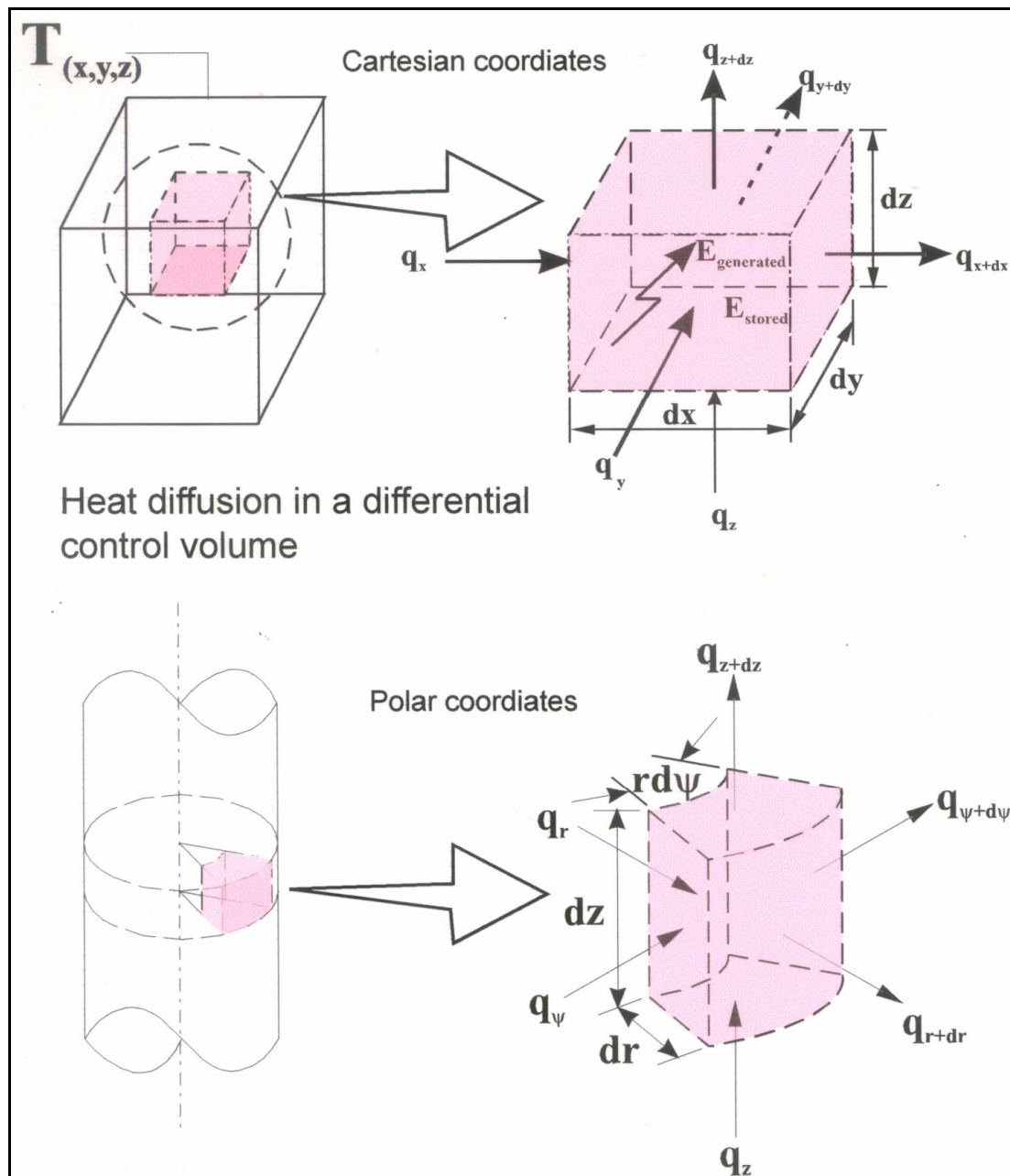


Fig. 3.1. Coordinate axes and differential amounts for Cartesian and polar coordinates

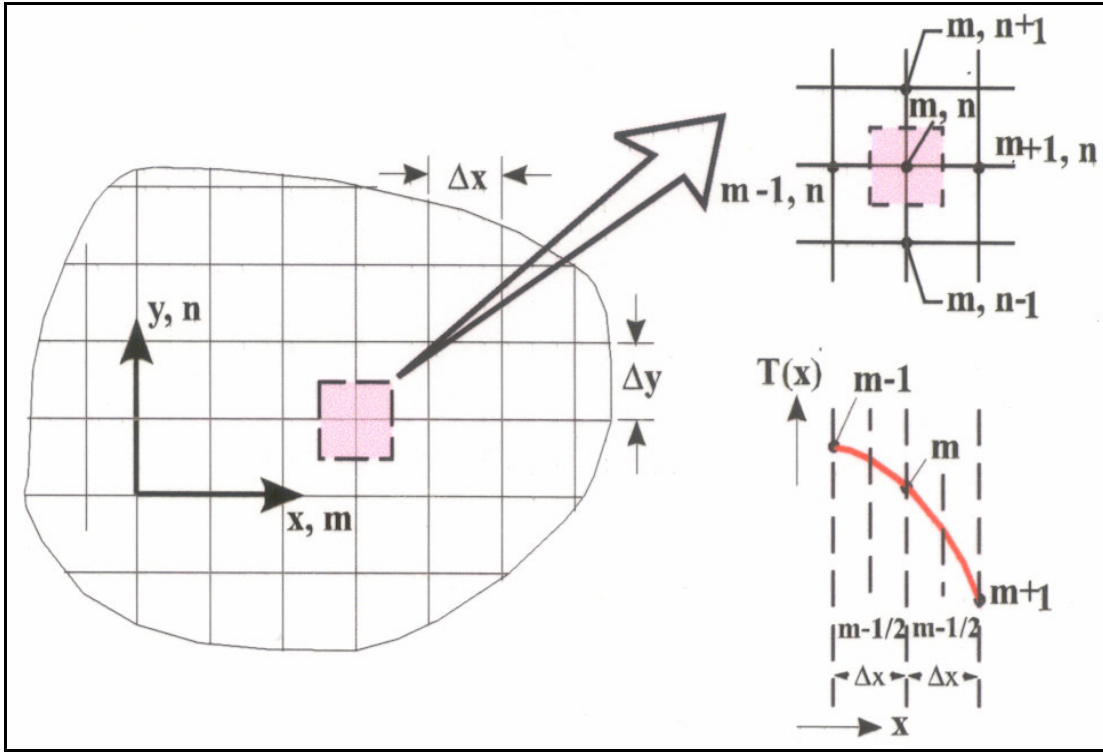


Fig. 3.2. Closer look to the specific node under consideration

For the tool, radial, angular and longitudinal (cylindrical) approximations are used, and Eq. (3.13) is gathered combining these. Again, grouping the nodal temperatures and coefficients at one side, with T^0 coefficients at the other, the matrix problem can be solved (Eq. 3.14).

$$\begin{aligned}
 & \frac{T_i(r+\Delta r, \psi, z) + T_i(r-\Delta r, \psi, z) - 2T_i(r, \psi, z)}{(\Delta r)^2} + \frac{T_i(r, \psi+\Delta \psi, z) + T_i(r, \psi-\Delta \psi, z) - 2T_i(r, \psi, z)}{r^2(\Delta \psi)^2} \\
 & + \frac{T_i(r, \psi, z+\Delta z) + T_i(r, \psi, z-\Delta z) - 2T_i(r, \psi, z)}{(\Delta z)^2} + \frac{T_i(r+\Delta r, \psi, z) - T_i(r-\Delta r, \psi, z)}{2r\Delta r} \\
 & - \frac{hT_i(r, \psi, z)}{k\Delta z} = \frac{-\dot{Q}}{k} - \frac{hT_\infty}{k\Delta z}
 \end{aligned} \tag{3.13}$$

$$[D]\{T_i\} = \{E\} \Rightarrow \{T_i\} = [D]^{-1}\{E\} \quad (3.14)$$

For the workpiece, heat balance equations in finite difference form are analogous to the equations of chip, with the only difference being T_w instead of T_c . Therefore, Eq. (3.15) can be achieved by grouping the corresponding terms together.

$$[R]\{T_w\} = \{S\} \Rightarrow \{T_w\} = [R]^{-1}\{S\} \quad (3.15)$$

Once these equations are employed to solve the analytical problem and the corresponding temperature values are written, the inlet and exit heat conduction terms are handled within the equation. The only thing that remains becomes the heat generation.

3.2.3 Heat Generation

There are three main heat generation zones during the machining processes, which are named primary, secondary and tertiary zones. Since we are dealing with orthogonal machining, tertiary zone heat generation is theoretically not observed. Primary zone heat generation due to shearing, and secondary zone heat generation due to friction at the rake face are modeled per unit depth of cut. These heat generation values are found from the force and velocity components along shear and friction dimensions, such as:

$$\begin{aligned} Q_s &= F_s V_s = \frac{\tau h V \cos(\alpha_n)}{\sin(\alpha_n) \cos(\phi_n - \alpha_n)} \\ Q_f &= F_f V_c = \frac{\tau h V \sin(\beta_n)}{\cos(\phi_n + \beta_n - \alpha_n) \sin(\phi_n - \alpha_n)} \end{aligned} \quad (3.16)$$

In these equations, h is the uncut chip thickness, which corresponds to feedrate in turning, τ is the shear flow stress, V is the cutting velocity and α_n , ϕ_n and β_n are the normal

rake angle, normal shear angle and normal friction angle respectively. These values of heat generation are found per unit depth of cut; but multiplying them by depth of cut would give the actual values. Then, by dividing to the amount of nodes along the depth of cut, one can find the amount of heat generated at any node at the heat generation surface.

3.2.3.1 Distribution of Heat Generation in the Primary Zone

Heat generated at the shear plane is distributed between chip and workpiece. Since we look at the tool-chip contact first, we modeled this heat generation as a heat source that raises the temperature of the nodes at the shear plane. However, since the specific values of the temperature rise at the nodes is hard to find, the average temperature rise is found according to Oxley's energy partitioning function, and is used as a boundary condition at the nodes on chip that has just started to shear.

$$\Delta T = \frac{Q_s (1 - B2)}{\rho C_p h V} \quad (3.17)$$

where ρ is the density of the workpiece, C_p is the specific heat capacity of the workpiece, h is the uncut chip thickness, V is the cutting velocity, Q_s is the shear plane heat generation per unit depth of cut and $B2$ is the proportion of shearing heat flux that has entered the workpiece. This value can be found by the following approximations [19]:

$$\begin{aligned} B2 &= 0.5 - 0.35 \log(R_t \tan(\phi_n)), \text{ if } 0.004 < R_t \tan(\phi_n) < 10 \\ B2 &= 0.5 - 0.15 \log(R_t \tan(\phi_n)), \text{ if } R_t \tan(\phi_n) > 10 \end{aligned} \quad (3.18)$$

Here, ϕ_n is the normal shear angle and R_t is called the thermal number found by:

$$R_t = \frac{\rho C_p h V}{k} \quad (3.19)$$

3.2.3.2 Distribution of Heat Generation in the Secondary Zone

Heat generation in the secondary zone is more complexly handled than the heat generation in the primary zone. Here, we first assume a portion of heat generated due to friction flows to the tool, and after solving chip and tool temperature fields, we compare the rake face temperatures. Since these nodes on chip and tool are actually the same points on space, the temperature difference between the two must be theoretically zero. However, reaching this zero difference is very hard and time consuming in numerical solutions, if not impossible. This is due to the characteristic of numerical solution methods, the convergence of the element size. Hence, a difference below an acceptable tolerance limit is expected. If the difference is not under the tolerance limits, the portion of heat generation flowing to the tool is updated in such a fashion that this difference might be smaller. Iteratively, this difference is pulled below the tolerance limits and the temperatures of tool and chip rake face nodes are brought close together.

3.3 Experimental Procedure

Temperature field observed on the tool is measured with an infrared camera, and compared to simulations for three-dimensional tool temperature field, concerning the maximum rake face temperature at the tool from an orthogonal point of view. The infrared (IR) camera technique has many advantages to measure temperature in machining processes. These advantages include zero contact between the camera and the heat sources, therefore no effect on temperature distribution, and fast response of the IR camera system. While using other methods such as thermocouples or fiber-optic pyrometer, the measurement device has to be embedded within the tool, which perturbs the heat flow in

the tool and changes the results. In addition, fast response of the IR camera lets high cutting speeds to be used in machining experiments, since it becomes possible to capture the transient changes.

The technique has some disadvantages as well, such as the need of knowledge about the exact surface emissivity and chip obstruction. To make the measurements accurate with the IR camera, one needs to know the exact surface emissivity, which changes with surface temperature, roughness and phase of the material, all of them changing during the process. Furthermore, during the process, flowing chip may get between the tool and the camera, which will obstruct the measurements. This disadvantage is handled by repeatedly doing the measurements until getting the best chip-free view.

The IR camera used in the experiments can be seen in Figure 3.3. The main part of this system is the IR camera, whose model is Electrophysics PV-320, having an uncooled focal plane array (FPA) detector - which takes advantage of a ferroelectric phase transition in certain dielectric materials - and utilizing an array of pyroelectric elements in that plane, which is a ceramic material of barium, strontium and titanium salts. The nominal desired composition of the ceramic is $\text{Ba}_{0.66}\text{Sr}_{0.34}\text{TiO}_3$. The IR camera has 320x240 pixels of array size, a target image temperature range between -18°C to 523°C , and a minimum resolvable temperature difference of approximately 0.2°C at 25°C . The spectral responsivity of the camera is 7-14 μm , and the camera has 2:1 interlace scanning property. The diaphragm adjusts the IR light intensity that enters into the camera. The nitrogen input is needed in order to overcome the condensation problem due to the humid environment, since this condensation can cause noises in the final captured IR image. The reflective microscope objective images the area of interest on to the FPA detector. The protective window prevents chips to harm the objective.

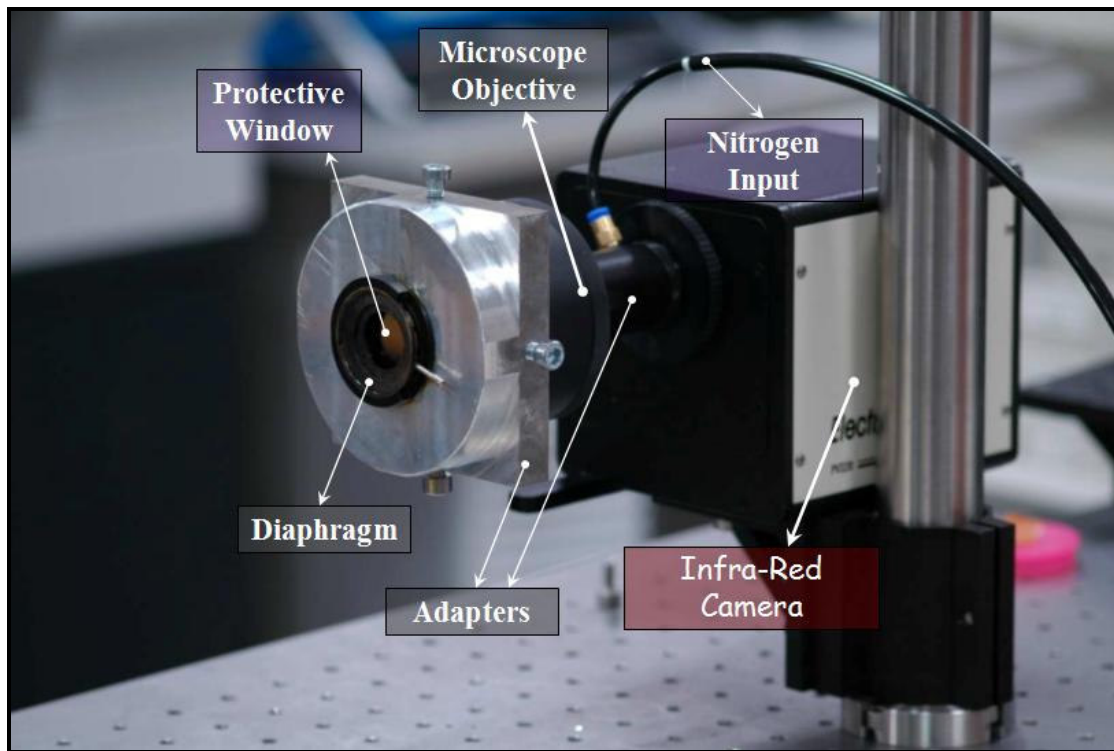


Fig. 3.3. The IR camera system used for temperature measurements

Figure 3.4 shows the same IR camera system adapted into the Vertical CNC Milling Center, in which the measurements are done. As seen in the figure, during the temperature measurements in the CNC machine, the IR setup is protected with an additional covering in order to isolate the camera and the objective from the chips and any flying particles that are present in the environment of CNC machine. The tool is placed in the workpiece location, since it has to be steady to measure correctly, and the workpiece is placed in the tool location, rotating with the spindle, forming a vertical turning machine. The experiments were done with three different workpiece materials, namely Al 7075, AISI 1050 carbon steel and AISI H13 steel. Cutting inserts were Sandvik, type H13A, which are tungsten carbide inserts without any coating or chip breakers. Half of the tests were done with fresh

tools having a rake angle of 6° , with edge radius of approximately 15 microns, and the other half with fresh tools having a rake angle of 18° , with edge radius of approximately 20 microns. The force measurement system included a 3-component Kistler dynamometer, a Kistler charge amplifier, and a National Instruments I/O box, with data acquisition carried out by MALDAQ, a module of CutPro 6.

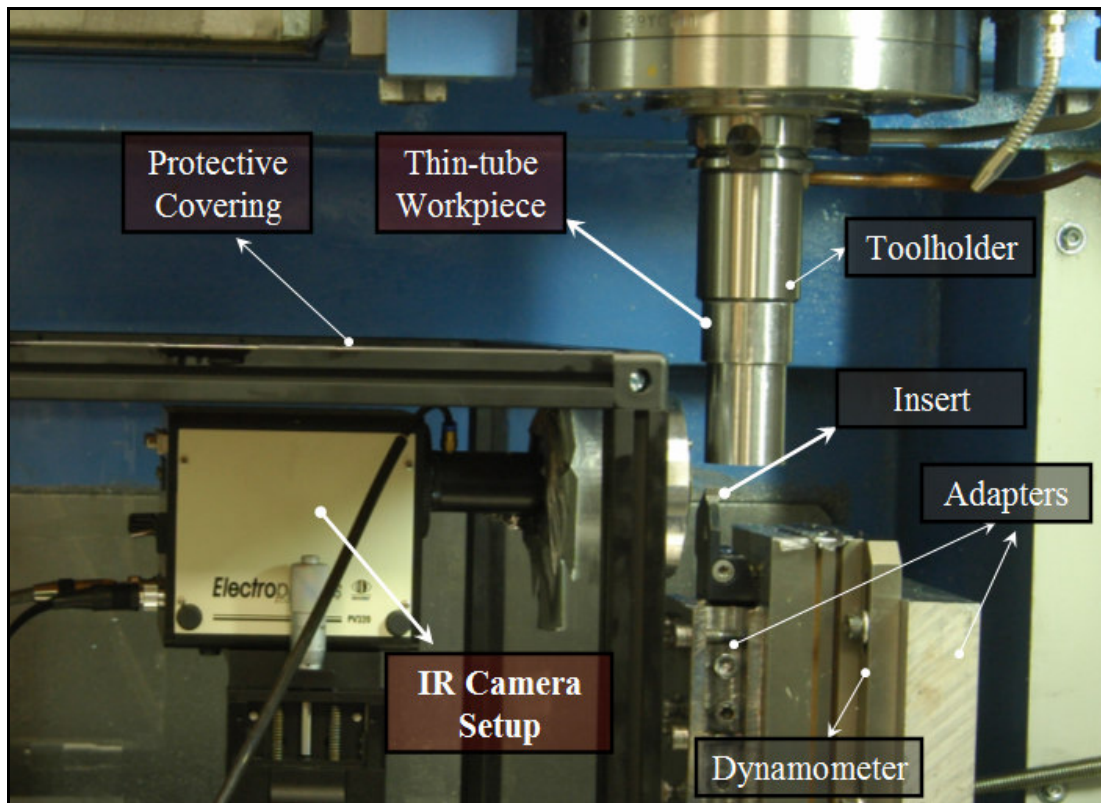


Fig. 3.4. The IR camera system as it is in the vertical CNC machine

Figure 3.5 shows a close-up view of the experimental setup, where the distance between the aluminum adapter in front of the microscope and the tool rake face is approximately 20 mm. It can be resulted from this fact that making these experiments very

close to the cutting edge of the tool in the CNC machine is very difficult, and this distance also explains the need for the additional protective covering.

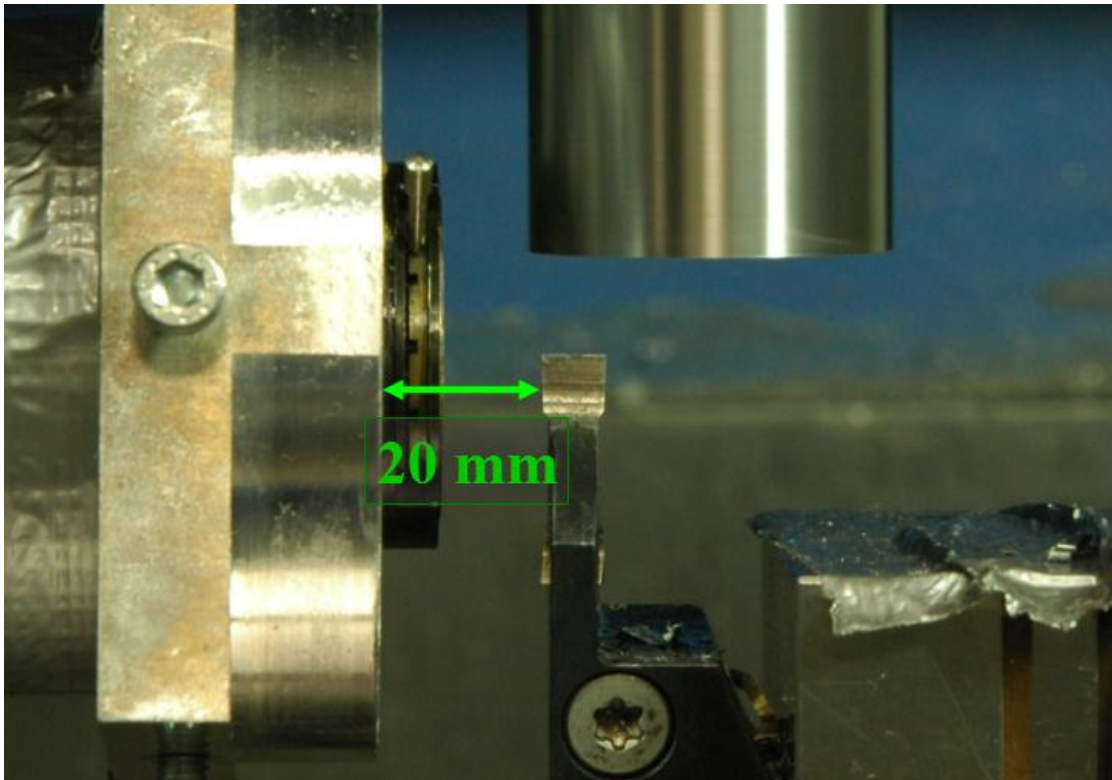


Fig. 3.5. The close-up view of the IR system near the tool insert.

Finally, the video output from the IR camera is processed by differentiating, noise filtering, and coloring, to get the final results

Chapter 4

RESIDUAL STRESS PREDICTION AND EXPERIMENTS

4.1 Introduction

Prediction of residual stresses is a very critical aspect for characterization of the machining processes. The analytical model developed here within this thesis predicts the residual stresses in machining processes, by an elasto-plastic loading using the superposition of thermal and mechanical stresses, and an anisotropic hardening model including a relaxation procedure. To result in the residual stresses after machining, cutting temperatures are predicted on the chip, tool and workpiece, using the cutting forces as the reason for heat generation. A two-dimensional temperature distribution prediction is used for this, which is a simpler version of the three-dimensional temperature field prediction explained in Chapter 3.

Results are verified with experimental data on residual stresses on Waspaloy, and accurate results are achieved, while the computational time to predict the residual stresses is immensely decreased.

4.2 Thermo-Mechanical Model

The system of interest and most of the formulation can be found in a previous study of the authors [20]. Still, the schematic that shows the system is shown in Figure 4.1. There are forces in feed and tangential (cutting) directions on the workpiece, which are modeled

as triangularly, distributed forces. In the beginning, the distribution of the mechanical loads (both tangential and normal components) was assumed to be rectangular. This rectangular distribution method was easy to apply, but not very realistic, since the load is assumed to be the same where the tool just touches the workpiece and where it is fully immersed within the workpiece. Hertzian contact is another frequently used type within literature; however it is very theoretical and not close enough to the real case. The results shown in this thesis use the triangular distribution of mechanical forces, which made available the optimization between rectangular and Hertzian contact. The magnitude of the distributions are found from either measurements with the help of a dynamometer, or cutting force predictions, explained in detail in [27]. As seen from Figure 4.1, the system is considered to be two dimensional, since all of the cross-sections in the third dimension are assumed to be the same. This situation is the property of the orthogonal cutting process. The workpiece is modeled as a semi-infinite material when the x direction is infinite and z direction is half-plane infinite. The material is considered to be homogeneous over its volume, and it exhibits both isotropic and kinematic hardening. Von Mises stresses are effective on the workpiece. The temperature distribution predicted as explained before in Chapter 3 is present on the workpiece, and it works along with mechanical loading on the workpiece to create the von Mises stresses and therefore, the residual stresses due to plastic loading.

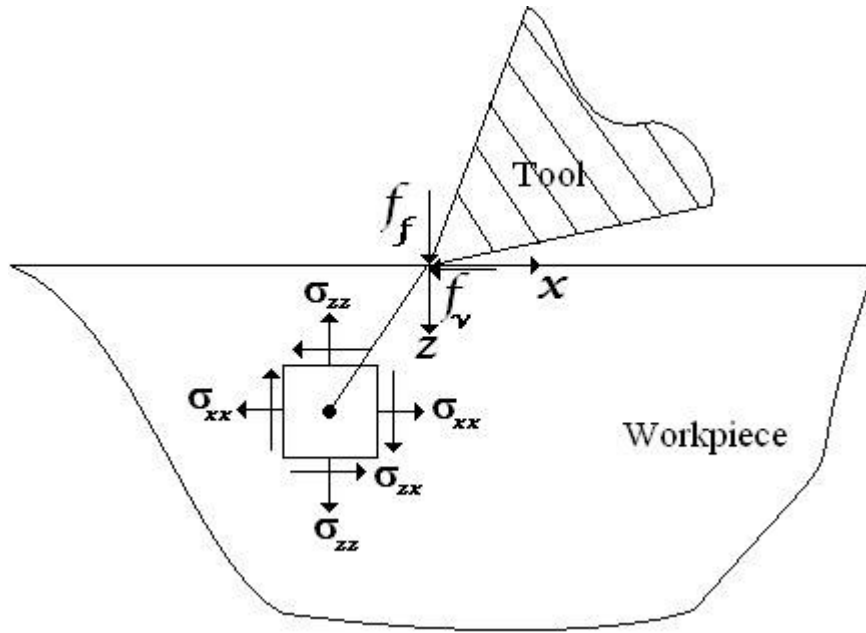


Fig. 4.1. The geometry of the system. The problem is assumed to be plain strain, therefore no strain in y-direction. The stresses shown here are the resultant stresses from the thermal and mechanical load coming from the process on the point of interest.

4.2.1 Elastic loading

The material has an elastic modulus (or Young's Modulus) of E , a Poisson's ratio of ν , coefficient of thermal expansion of α , isotropic hardening coefficient of h , and kinematic hardening coefficient of c . Plain strain condition is assumed on the workpiece, with the strain component in y-direction considered to be zero all times. Therefore, the mechanical loading due to triangularly distributed forces in feed and tangential directions can be found by [72]:

$$\begin{aligned}
\sigma_{xx}^{mechanical} &= -\frac{2z}{\pi} \int_{-a}^a \left(\frac{p(s)(x-s)^2 + q(s)(x-s)^3}{((x-s)^2 + z^2)^2} \right) ds \\
\sigma_{zz}^{mechanical} &= -\frac{2z^3}{\pi} \int_{-a}^a \left(\frac{p(s) + q(s)(x-s)}{((x-s)^2 + z^2)^2} \right) ds \\
\sigma_{xz}^{mechanical} &= -\frac{2z^2}{\pi} \int_{-a}^a \left(\frac{p(s)(x-s) + q(s)(x-s)^2}{((x-s)^2 + z^2)^2} \right) ds
\end{aligned} \tag{4.1}$$

where p is the normal force distribution (feed direction) and q is the tangential force distribution (cutting velocity direction) that are distributed as follows:

$$\begin{aligned}
p(s) &= f_f \left(1 - \frac{|s|}{a} \right) \\
q(s) &= f_v \left(1 - \frac{|s|}{a} \right)
\end{aligned} \tag{4.2}$$

and x and z are the distances in x - and z -directions from the point of contact to the point of interest, s is the integral variable, and a is the contact radius, meaning that tool and workpiece contact each other within the line of $[-a, a]$. This contact length is a function of several parameters, and can be calculated from [72]:

$$a = \sqrt{\frac{4PR}{\pi E}} \tag{4.3}$$

Here, P is the total normal force, R is the resultant radius and E is the resultant Young's Modulus. These resultant values can be found by:

$$\frac{1}{R} = \frac{1}{R_t} + \frac{1}{R_w} \tag{4.4}$$

$$\frac{1}{E} = \frac{1}{E_t} + \frac{1}{E_w} \quad (4.5)$$

Since the radius of the workpiece is very big compared to the nose radius of the tool, nose radius can be taken as R.

The thermal loading due to the temperature field on the workpiece can be calculated from addition of three values:

- a) Thermal stresses due to body forces
- b) Thermal stresses due to surface traction
- c) Thermal stresses due to hydrostatic pressure [73]

Summation of these three effects can be found from [20] and is used to calculate the thermal effects [72,74].

When the thermal and mechanical effects are summed up, the stresses along each normal and shear directions can be found:

$$\begin{aligned} \sigma_{xx} &= \sigma_{xx}^{mechanical} + \sigma_{xx}^{thermal} \\ \sigma_{zz} &= \sigma_{zz}^{mechanical} + \sigma_{zz}^{thermal} \\ \sigma_{xz} &= \sigma_{xz}^{mechanical} + \sigma_{xz}^{thermal} \\ \sigma_{yy} &= \nu(\sigma_{xx} + \sigma_{zz}) - \alpha E \Delta T \end{aligned} \quad (4.6)$$

These are the elastic stresses occurring on the workpiece. When the plasticity criterion is reached, the calculations are different. Here, ν is the Poisson's Ratio for the workpiece, α is the coefficient of thermal expansion for the workpiece, and ΔT is calculated by:

$$\Delta T = T - T_{room} \quad (4.7)$$

Then, the von Mises stresses corresponding to these orthogonal stresses can be found by:

$$\begin{aligned}
S_{xx} &= \sigma_{xx} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
S_{yy} &= \sigma_{yy} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
S_{zz} &= \sigma_{zz} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
S_{xz} &= S_{zx} = \sigma_{xz}
\end{aligned}
\tag{4.8}$$

$$f = \frac{1}{2} \left((S_{xx} - \alpha_{xx})^2 + (S_{yy} - \alpha_{yy})^2 + (S_{zz} - \alpha_{zz})^2 + (S_{xz} - \alpha_{xz})^2 + (S_{zx} - \alpha_{zx})^2 \right) - K^2
\tag{4.9}$$

τ_0 : Yield in pure shear

σ_0 : Yield in uniaxial tension

$$K = \tau_0 = \frac{\sigma_0}{\sqrt{3}}, \text{ according to Von Mises}
\tag{4.10}$$

If $f < 0$, the tool is still in the elastic zone and we need to apply elastic procedure for yet another node. If $f = 0$, the tool is in plastic region and we need to apply the following plastic procedure. If $f > 0$, this is physically not possible; but due to numerical approximations it can happen in computational environment. Best way to handle this situation is to go back one step, reduce the step size and try the same procedure for the same node all over again until f becomes really zero. Another way to solve it more quickly with close accuracy is equate f to zero, and start with values from the previous step. This way, no iteration is required and if the initial (elastic) step size is small enough, there is not too much error.

If elastic,

$$\begin{aligned}
\alpha_{xx} &= \alpha_{yy} = \alpha_{zz} = \alpha_{xz} = \alpha_{xy} = \alpha_{yz} = 0 \\
\varepsilon_{xx} &= \frac{\sigma_{xx}}{E} (1 - \nu^2) - \nu \frac{\sigma_{zz}}{E} (1 + \nu) + \alpha \Delta T (1 + \nu) \\
\varepsilon_{yy} &= 0 \\
\varepsilon_{zz} &= \frac{\sigma_{zz}}{E} (1 - \nu^2) - \nu \frac{\sigma_{xx}}{E} (1 + \nu) + \alpha \Delta T (1 + \nu) \\
\varepsilon_{xz} &= \varepsilon_{zx} = \frac{\sigma_{xz}}{2G} \\
\varepsilon_{xx}^{plastic} &= \varepsilon_{zz}^{plastic} = \varepsilon_{xz}^{plastic} = 0
\end{aligned} \tag{4.11}$$

Here, the α terms are the backstresses and ε terms are the strains occurring in corresponding directions. If the stresses become plastic (f becomes zero), we start a step by step approach for more accuracy.

4.2.2 Plastic loading

Figure 2 shows the plastic schematic of the system. When the process becomes plastic, it is important to decrease the step size, since the stresses are path dependant within the plastic zone. We define Point 2 as the current step, and Point 1 as previous step. If the stresses just became plastic, previous values of α 's and ε 's are all zero, since they were elastic in the previous step.

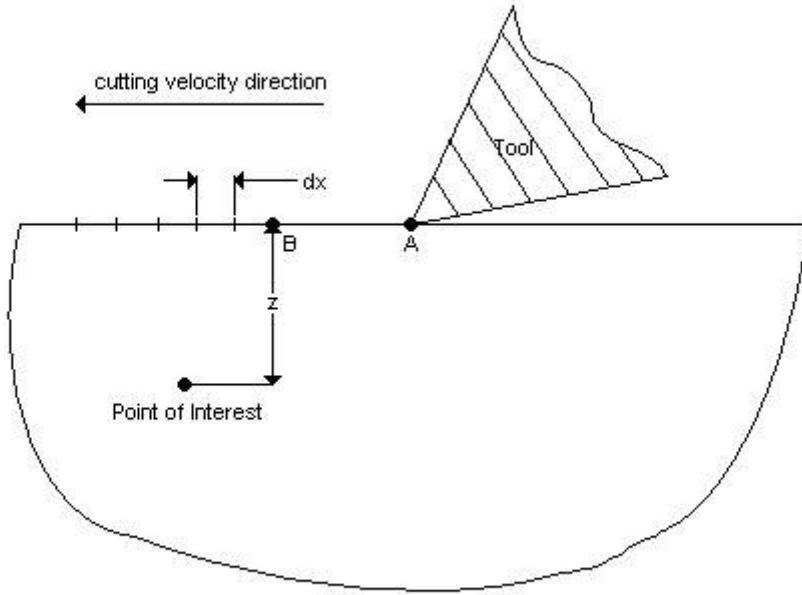


Fig. 4.2. The discretization of the system. In the elastic region (until B), the element size is greater; however, after the process becomes plastic (at point B), the discretization amount becomes smaller (dx , after point B).

After finding the stresses at the current step as described in the elastic loading part, and knowing every value from the previous step, the marginal stresses from the previous to current step can be found by:

$$\begin{aligned} d\sigma_{xx} &= \sigma_{xx}^2 - \sigma_{xx}^1 \\ d\sigma_{xz} &= \sigma_{xz}^2 - \sigma_{xz}^1 \\ d\sigma_{zz} &= \sigma_{zz}^2 - \sigma_{zz}^1 \end{aligned} \quad (4.12)$$

From the plain strain conditions, it is known that the sum of plastic and elastic strain in y-direction must be zero. The plastic strain in the y-direction is:

$$d\epsilon_{yy}^{plastic} = \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{yy} - \alpha_{yy}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + \right. \\ \left. (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \quad (4.13)$$

The mechanical and thermal strains in y-directions are:

$$d(\epsilon_{yy}^{mechanical} + \epsilon_{yy}^{thermal}) = \frac{d\sigma_{yy}}{E} - \frac{\nu}{E}(d\sigma_{xx} + d\sigma_{zz}) + \alpha\Delta T \quad (4.14)$$

The summation of these stresses equal to zero:

$$d\epsilon_{yy}^{plastic} + d\epsilon_{yy}^{mechanical} + d\epsilon_{yy}^{thermal} = 0 \quad (4.15)$$

Here, h is the isotropic hardening coefficient and c is the kinematic hardening coefficient for the workpiece. These coefficients are found from the stress-strain curves and fatigue curves of the workpiece material [75]. Isotropic hardening coefficient is the slope of the stress-strain curve in the plastic region, and kinematic hardening coefficient is equal to the stress value at the material's endurance limit. There are three unknowns in these equations; marginal plastic strain, total marginal mechanical and thermal strains and marginal stresses in y direction. The three equations relating these unknowns to each other will give the results, if they are solved together. First, find $d\sigma_{yy}$, and then use it to find the plastic strain in y-direction, as well as x-, z- and xz-directions, using the equations below, and at last, the backstress values at each direction.

$$d\epsilon_{xx}^{plastic} = \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{xx} - \alpha_{xx}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \quad (4.16)$$

$$d\epsilon_{zz}^{plastic} = \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{zz} - \alpha_{zz}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \quad (4.17)$$

$$d\epsilon_{xz}^{plastic} = d\epsilon_{zx}^{plastic} = \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{xz} - \alpha_{xz}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \quad (4.18)$$

$$\begin{aligned}
d\alpha_{xx} &= cd\epsilon_{xx}^{plastic} \\
d\alpha_{yy} &= cd\epsilon_{yy}^{plastic} \\
d\alpha_{zz} &= cd\epsilon_{zz}^{plastic} \\
d\alpha_{xz} &= d\alpha_{zx} = cd\epsilon_{xz}^{plastic}
\end{aligned} \tag{4.19}$$

Then, we need to update everything to use for the next step. We already calculated next step stress values; we need to find next step strain and backstress values.

$$\begin{aligned}
\epsilon_{xx}^{plastic,2} &= \epsilon_{xx}^{plastic,1} + d\epsilon_{xx}^{plastic} \\
\epsilon_{zz}^{plastic,2} &= \epsilon_{zz}^{plastic,1} + d\epsilon_{zz}^{plastic} \\
\epsilon_{xz}^{plastic,2} &= \epsilon_{xz}^{plastic,1} + d\epsilon_{xz}^{plastic} \\
\epsilon_{xx}^2 &= \epsilon_{xx}^{plastic,2} + \epsilon_{xx}^{elastic,2} \\
\epsilon_{zz}^2 &= \epsilon_{zz}^{plastic,2} + \epsilon_{zz}^{elastic,2} \\
\epsilon_{xz}^2 &= \epsilon_{zx}^2 = \epsilon_{xz}^{plastic,2} + \epsilon_{xz}^{elastic,2} \\
\alpha_{xx}^2 &= \alpha_{xx}^1 + d\alpha_{xx} \\
\alpha_{yy}^2 &= \alpha_{yy}^1 + d\alpha_{yy} \\
\alpha_{zz}^2 &= \alpha_{zz}^1 + d\alpha_{zz} \\
\alpha_{xz}^2 &= \alpha_{zx}^2 = \alpha_{xz}^1 + d\alpha_{xz}
\end{aligned} \tag{4.20}$$

We update the value of the yield in uniaxial tension, due to kinematic and isotropic hardening effects, and therefore the K value as well.

$$\begin{aligned}
d\bar{\sigma}_0 &= \frac{1}{\left(1 + \frac{c}{h}\right)} \frac{3}{2\bar{\sigma}_0} \sum_{i,j} (S_{ij}^1 - \alpha_{ij}^1) d\sigma_{ij} \\
\bar{\sigma}_0^2 &= \bar{\sigma}_0^1 + d\bar{\sigma}_0 \\
f &= \frac{1}{2} \left[\sum_{i,j} (S_{ij}^2 - \alpha_{ij}^2) (S_{ij}^2 - \alpha_{ij}^2) \right] - \frac{1}{3} (\bar{\sigma}_0^2) (\bar{\sigma}_0^2)
\end{aligned} \tag{4.22}$$

If $f < 0$, it means the process went back to become elastic, and the unloading subroutine must be called. If $f = 0$, it means that the process is still plastic, and we need to continue for yet another step, and then check for plasticity again. If $f > 0$, it means that something is wrong, since it is physically impossible. However, numerical approximations may cause this kind of error, and if the difference is small compared to elastic values, we take f to be zero, correcting the yield stress coefficient K .

After the loading is done and stresses become elastic again, we don't need to wait until the tool passes over the workpiece entirely. Since both thermal and mechanical effects are non-increasing after tool passes the point of interest, instant unloading of the forces is appropriate. However, after some amount of unloading, the unloading process will become plastic as well. This behavior is handled with a similar approach presented below, and then a relaxation procedure is applied to get residual stress results.

4.2.3 Relaxation

We know that since plain strain conditions are considered; ϵ_{yy} , strain in y-direction must be zero. Moreover, after the unloading, we should not be observing any strain in x-direction (cutting velocity direction) or stress in z- and xz-directions (feed and shear directions). We will be left with only stresses in x- and y-directions (residual stresses in cutting and depth of cut directions), and strains in z-and xz-directions (feed and shear directions). To achieve this, we need to decrease the values of $\epsilon_{xx}, \sigma_{zz}, \sigma_{xz}$ step by step until they become zero simultaneously [32]. We will again start with $\sigma_{ij}^{initial} = 0$, and $\bar{\sigma}_0$ as the initial yield strength. We choose a large enough number, M , to divide the step size, which we use for step-by-step approach. We divide each of the stresses/strains that need to become zero with this number, and the amounts become:

$$d\epsilon_{xx} = -\frac{\epsilon_{xx}}{M}, d\sigma_{zz} = -\frac{\sigma_{zz}}{M}, \text{ and } d\sigma_{xz} = -\frac{\sigma_{xz}}{M} \quad (4.23)$$

Then, for every step, we check the plasticity and continue with plastic calculations if necessary.

$$\begin{aligned}
 S_{xx} &= \sigma_{xx} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
 S_{yy} &= \sigma_{yy} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
 S_{zz} &= \sigma_{zz} - \frac{1}{3}(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \\
 S_{xz} &= S_{zx} = \sigma_{xz}
 \end{aligned}
 \tag{4.24}$$

$$f = \frac{1}{2} \left((S_{xx} - \alpha_{xx})^2 + (S_{yy} - \alpha_{yy})^2 + (S_{zz} - \alpha_{zz})^2 + (S_{xz} - \alpha_{xz})^2 + (S_{zx} - \alpha_{zx})^2 \right) - K^2
 \tag{4.25}$$

Again, if $f < 0$, unloading is elastic, and if $f = 0$, that means plastic unloading occurs. If elastic unloading is of interest,

$$\begin{aligned}
 d\sigma_{xx} &= \frac{\nu E}{(1+\nu)(1-2\nu)} (d\epsilon_{xx} + d\epsilon_{yy} + d\epsilon_{zz}) + \frac{E}{1+\nu} d\epsilon_{xx} \\
 d\sigma_{yy} &= \frac{\nu E}{(1+\nu)(1-2\nu)} (d\epsilon_{xx} + d\epsilon_{yy} + d\epsilon_{zz}) + \frac{E}{1+\nu} d\epsilon_{yy} \\
 d\sigma_{zz} &= \frac{\nu E}{(1+\nu)(1-2\nu)} (d\epsilon_{xx} + d\epsilon_{yy} + d\epsilon_{zz}) + \frac{E}{1+\nu} d\epsilon_{zz} \\
 d\epsilon_{xz} &= d\epsilon_{zx} = \frac{1}{2} \frac{d\sigma_{xz}}{G}
 \end{aligned}
 \tag{4.26}$$

Here, $d\epsilon_{yy}$ is zero due to plastic strain condition. We know $d\epsilon_{xx}$ and $d\sigma_{zz}$ and $d\sigma_{xz}$ from the dividend, thus we have four unknowns: $d\epsilon_{zz}$, $d\sigma_{xx}$, $d\sigma_{yy}$, and $d\epsilon_{xz}$. We can solve these unknowns by first finding $d\epsilon_{zz}$ from the third equation and putting it into the first two of the equations to find the values of $d\sigma_{xx}$ and $d\sigma_{yy}$, and solving the fourth equation itself. Then, we need to find the stresses and strains at every step by:

$$\begin{aligned}
\epsilon_{xx}^2 &= \epsilon_{xx}^1 + d\epsilon_{xx} \\
\epsilon_{xz}^2 &= \epsilon_{xz}^1 + d\epsilon_{xz} \\
\epsilon_{zz}^2 &= \epsilon_{zz}^1 + d\epsilon_{zz} \\
\sigma_{xx}^2 &= \sigma_{xx}^1 + d\sigma_{xx} \\
\sigma_{yy}^2 &= \sigma_{yy}^1 + d\sigma_{yy} \\
\sigma_{zz}^2 &= \sigma_{zz}^1 + d\sigma_{zz} \\
\sigma_{xz}^2 &= \sigma_{xz}^1 + d\sigma_{xz}
\end{aligned} \tag{4.27}$$

where 1 is the previous point and 2 is the current point of interest.

When the unloading process becomes plastic, we find the needed stress and strain values in a different way. First we find the marginal stresses in x- and y-directions.

$$\begin{aligned}
&\left\{ \frac{3(S_{xx} - \alpha_{xx})(S_{yy} - \alpha_{yy})}{2\bar{\sigma}_0^2(h+c)} - \frac{\nu}{E} \right\} d\sigma_{xx} + \left\{ \frac{3(S_{yy} - \alpha_{yy})(S_{yy} - \alpha_{yy})}{2\bar{\sigma}_0^2(h+c)} + \frac{1}{E} \right\} d\sigma_{yy} + \\
&\left\{ \frac{3(S_{zz} - \alpha_{zz})(S_{yy} - \alpha_{yy})}{2\bar{\sigma}_0^2(h+c)} - \frac{\nu}{E} \right\} d\sigma_{zz} + \left\{ \frac{3(S_{xz} - \alpha_{xz})(S_{yy} - \alpha_{yy})}{\bar{\sigma}_0^2(h+c)} \right\} d\sigma_{xz} = 0
\end{aligned} \tag{4.28}$$

$$\begin{aligned}
&\left\{ \frac{3(S_{xx} - \alpha_{xx})(S_{xx} - \alpha_{xx})}{2\bar{\sigma}_0^2(h+c)} + \frac{1}{E} \right\} d\sigma_{xx} + \left\{ \frac{3(S_{yy} - \alpha_{yy})(S_{xx} - \alpha_{xx})}{2\bar{\sigma}_0^2(h+c)} - \frac{\nu}{E} \right\} d\sigma_{yy} + \\
&\left\{ \frac{3(S_{zz} - \alpha_{zz})(S_{xx} - \alpha_{xx})}{2\bar{\sigma}_0^2(h+c)} - \frac{\nu}{E} \right\} d\sigma_{zz} + \left\{ \frac{3(S_{xz} - \alpha_{xz})(S_{xx} - \alpha_{xx})}{\bar{\sigma}_0^2(h+c)} \right\} d\sigma_{xz} = d\epsilon_{xx}
\end{aligned} \tag{4.29}$$

In these equations, only $d\sigma_{xx}$ and $d\sigma_{xz}$ are unknown, but they are solvable. After finding these values, we need to update the stress values at every direction. The plastic strains, on the other hand are also needed to be found, since they contribute in the calculation of kinematic hardening:

$$\begin{aligned}
d\epsilon_{xx}^{plastic} &= \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{xx} - \alpha_{xx}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + \right. \\
&\quad \left. (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \\
d\epsilon_{yy}^{plastic} &= \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{yy} - \alpha_{yy}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + \right. \\
&\quad \left. (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \\
d\epsilon_{zz}^{plastic} &= \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{zz} - \alpha_{zz}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + \right. \\
&\quad \left. (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right) \\
d\epsilon_{xz}^{plastic} &= d\epsilon_{zx}^{plastic} = \frac{1}{h+c} \frac{3}{2\bar{\sigma}_0^2} (S_{xz} - \alpha_{xz}) \left((S_{xx} - \alpha_{xx}) d\sigma_{xx} + (S_{yy} - \alpha_{yy}) d\sigma_{yy} + \right. \\
&\quad \left. (S_{zz} - \alpha_{zz}) d\sigma_{zz} + (S_{xz} - \alpha_{xz}) d\sigma_{xz} + (S_{zx} - \alpha_{zx}) d\sigma_{zx} \right)
\end{aligned} \tag{4.30}$$

$$\begin{aligned}
d\epsilon_{xx} &= \frac{d\sigma_{xx}}{E} - \frac{\nu(d\sigma_{yy} + d\sigma_{zz})}{E} + d\epsilon_{xx}^{plastic} \\
d\epsilon_{yy} &= \frac{d\sigma_{yy}}{E} - \frac{\nu(d\sigma_{xx} + d\sigma_{zz})}{E} + d\epsilon_{yy}^{plastic} \\
d\epsilon_{zz} &= \frac{d\sigma_{zz}}{E} - \frac{\nu(d\sigma_{xx} + d\sigma_{yy})}{E} + d\epsilon_{zz}^{plastic} \\
d\epsilon_{xz} &= \frac{d\sigma_{xz}}{E} + d\epsilon_{xz}^{plastic}
\end{aligned} \tag{4.31}$$

Now that we have found the marginal plastic strains at every direction:

$$\begin{aligned}
d\alpha_{xx} &= c * d\epsilon_{xx}^{plastic} \\
d\alpha_{yy} &= c * d\epsilon_{yy}^{plastic} \\
d\alpha_{zz} &= c * d\epsilon_{zz}^{plastic} \\
d\alpha_{xz} &= c * d\epsilon_{xz}^{plastic} \\
\alpha_{ij}^2 &= \alpha_{ij}^1 + d\alpha_{ij}
\end{aligned} \tag{4.32}$$

We will use this updated hardening at the next step, unless it becomes elastic again. We check the yielding criterion, and if it is still plastic, we will do the same procedure again at the next step, and if f drops below zero and the unloading process becomes elastic again,

we apply the elastic procedure over again. The typical behavior of this f value can be observed at Figure 4.3 below, as the value becomes closer to zero as the tool approaches the point of interest, and after reaching zero, it stays at zero for some time, while the yield stress is increased due to hysteresis. Then, unloading becomes so effective that the f value drops below zero and the residual stresses at the level of interest are obtained.

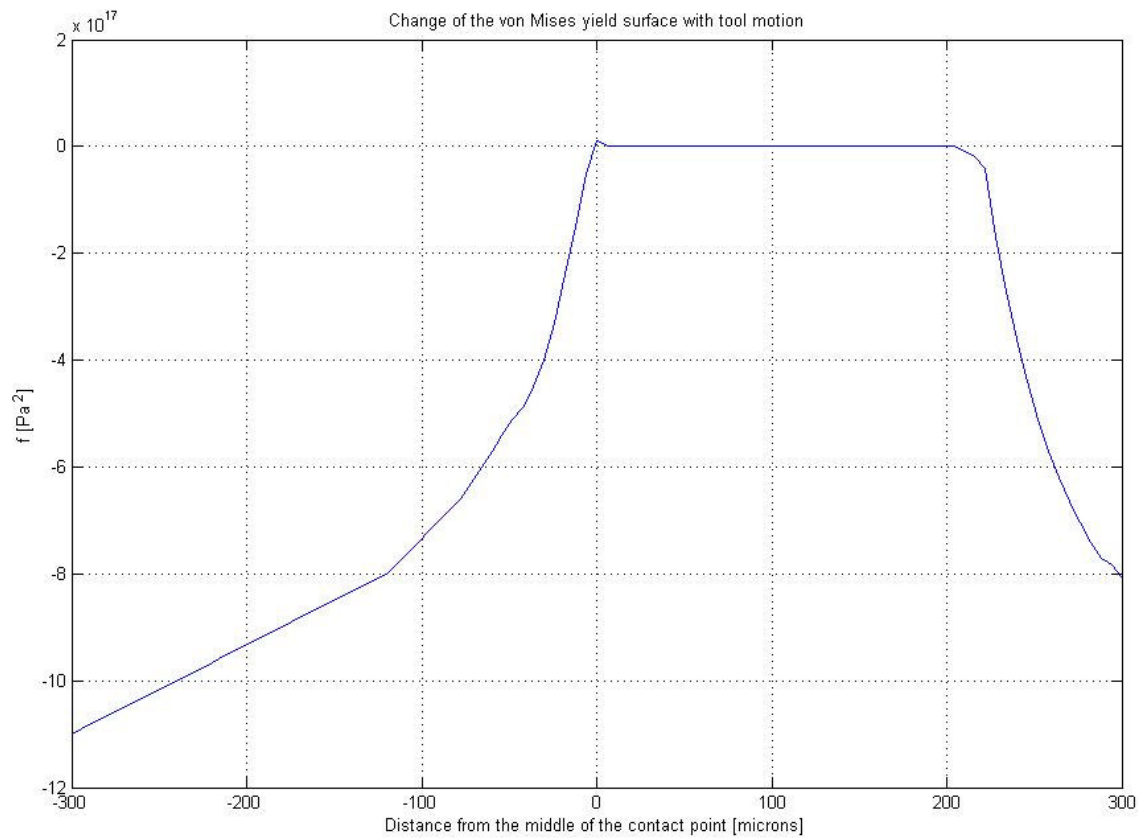


Fig. 4.3. The behavior of the f value with the motion of the tool. The depth stays constant, while the tool is moving on top of the point of interest, loading and then unloading the point.

Chapter 5

SIMULATIONS, VALIDATIONS AND RESULTS

5.1 Temperature Prediction Validations

The aforementioned finite difference based model is applied for different machining conditions and compared with experimental results. Since heat convection is included, chip, tool and workpiece temperatures were observed to be at maximum at the middle part of the system. The effect of the heat convection is at minimum due to the fact that it is the furthest piece from both sides that are subjected to heat convection. However, temperatures close to the edge at both sides drop closer to room temperature, as expected. The cutting conditions for figures related to temperature predictions can be found from Table 5.1, along with orthogonal cutting parameters for every workpiece-tool combination.

Heat convection coefficient can be determined using air at room temperature flowing at cutting speed as surroundings, stationary system, and approximation of the system to a flat plane. However, cutting velocity component at chip flow direction is used for the heat convection coefficient of chip/air interface. Eq. 5.1 is used to calculate the heat convection coefficient.

Table 5.1: Cutting parameters and orthogonal cutting results for all workpiece-tool combinations

Material	Tool Rake Angle [°]	Cutting Speed [m/min]	Feedrate [mm]	Shear Angle [°]	Friction Angle [°]	Shear Flow Stress [MPa]
Al 7075	6	160, 200	0.05, 0.10, 0.15, 0.20	31.5	33.0	287.1
	18			32.0	40.5	266.8
AISI 1050	6	40, 60, 80	0.025, 0.05, 0.075, 0.10	32.2	41.9	644.9
	18			33.7	42.4	825.0
AISI H13	6	20, 40, 60	0.025, 0.05, 0.075, 0.10	37.5	36.1	772.2
	18			33.9	43.2	1076.3

$$Nu_L = 0.664 Re^{1/2} Pr^{1/3} = \frac{hL}{k} \quad (5.1)$$

Here, Re is the Reynolds number and Pr is the Prandtl number concerning tool, workpiece and machining properties. L is the length of contact between air and the system, and k is the thermal conductivity of the tool/workpiece. Nu_L is the Nusselt number of the process.

Table 5.2 shows the maximum temperatures simulated and experimentally measured on the tool and the workpiece for the whole 64 tool-workpiece combination.

Orthogonal cutting and temperature measurement tests are done for conditions given in Table 5.1. Simulations are also made for the cutting conditions given in Table 5.1. Orthogonal cutting results for shear angle, friction angle and shear flow stress for every tool-workpiece combination is given in Table 5.1, as well. Figures 5.1-5.6 show the maximum rake face temperature comparisons between simulations and measurements for all the tool-workpiece combinations and cutting parameters. It can be resulted that the simulations are in acceptable agreement with measurements, although underpredicting 40-80°C for low feedrate values. This is mostly due to the fact that for low temperatures (temperatures closer to room temperature), thermal conductivity and density of materials change, which will effect Reynolds number of the process and thus heat convection coefficient. Thermal conductivity change will also effect the calculations of chip-tool interface heat generation terms, and perturb the simulation results. Since the calculations for thermal conductivity and density of the materials are done for maximum temperatures that are present, approximately 400°C, the simulations are most valid for temperature results close to this value.

The simulations and experiments for every tool-workpiece combination given in Table 5.1 can be found in Appendix.

Table 5.2: Cutting conditions and maximum simulated and experimental tool rake face temperatures

Test #	Material	α_r [degrees]	V_c [m/min]	h [mm]	$T_{\max,ex}$ [°C]	$T_{\max,sim}$ [°C]
1	Al 7075	6	160	0,050	229	141
2	Al 7075	6	160	0,100	281	204
3	Al 7075	6	160	0,150	329	254
4	Al 7075	6	160	0,200	363	302
5	Al 7075	6	200	0,050	235	157
6	Al 7075	6	200	0,100	288	224
7	Al 7075	6	200	0,150	310	289
8	Al 7075	6	200	0,200	344	345
9	Al 7075	18	160	0,050	225	123
10	Al 7075	18	160	0,100	275	180
11	Al 7075	18	160	0,150	295	238
12	Al 7075	18	160	0,200	363	299
13	Al 7075	18	200	0,050	262	142
14	Al 7075	18	200	0,100	281	212
15	Al 7075	18	200	0,150	308	228
16	Al 7075	18	200	0,200	317	329
17	AISI 1050	6	40	0,025	203	127
18	AISI 1050	6	40	0,050	265	184
19	AISI 1050	6	40	0,075	327	238
20	AISI 1050	6	40	0,100	371	289
21	AISI 1050	6	60	0,025	212	163
22	AISI 1050	6	60	0,050	314	247
23	AISI 1050	6	60	0,075	346	308
24	AISI 1050	6	60	0,100	416	379
25	AISI 1050	6	80	0,025	259	191
26	AISI 1050	6	80	0,050	304	283
27	AISI 1050	6	80	0,075	372	369
28	AISI 1050	6	80	0,100	461	461
29	AISI 1050	18	40	0,025	178	131
30	AISI 1050	18	40	0,050	230	201
31	AISI 1050	18	40	0,075	265	254

32	AISI 1050	18	40	0,100	292	293
33	AISI 1050	18	60	0,025	265	168
34	AISI 1050	18	60	0,050	268	257
35	AISI 1050	18	60	0,075	361	336
36	AISI 1050	18	60	0,100	412	412
37	AISI 1050	18	80	0,025	253	202
38	AISI 1050	18	80	0,050	329	309
39	AISI 1050	18	80	0,075	380	389
40	AISI 1050	18	80	0,100	412	446
41	AISI H13	6	40	0,025	177	106
42	AISI H13	6	40	0,050	249	162
43	AISI H13	6	40	0,075	298	203
44	AISI H13	6	40	0,100	336	251
45	AISI H13	6	60	0,025	340	163
46	AISI H13	6	60	0,050	312	252
47	AISI H13	6	60	0,075	342	317
48	AISI H13	6	60	0,100	397	388
49	AISI H13	6	80	0,025	302	207
50	AISI H13	6	80	0,050	371	318
51	AISI H13	6	80	0,075	430	422
52	AISI H13	6	80	0,100	506	493
53	AISI H13	18	40	0,025	217	121
54	AISI H13	18	40	0,050	242	183
55	AISI H13	18	40	0,075	281	237
56	AISI H13	18	40	0,100	287	281
57	AISI H13	18	60	0,025	246	184
58	AISI H13	18	60	0,050	295	278
59	AISI H13	18	60	0,075	385	378
60	AISI H13	18	60	0,100	421	433
61	AISI H13	18	80	0,025	302	237
62	AISI H13	18	80	0,050	414	357
63	AISI H13	18	80	0,075	410	418
64	AISI H13	18	80	0,100	424	483

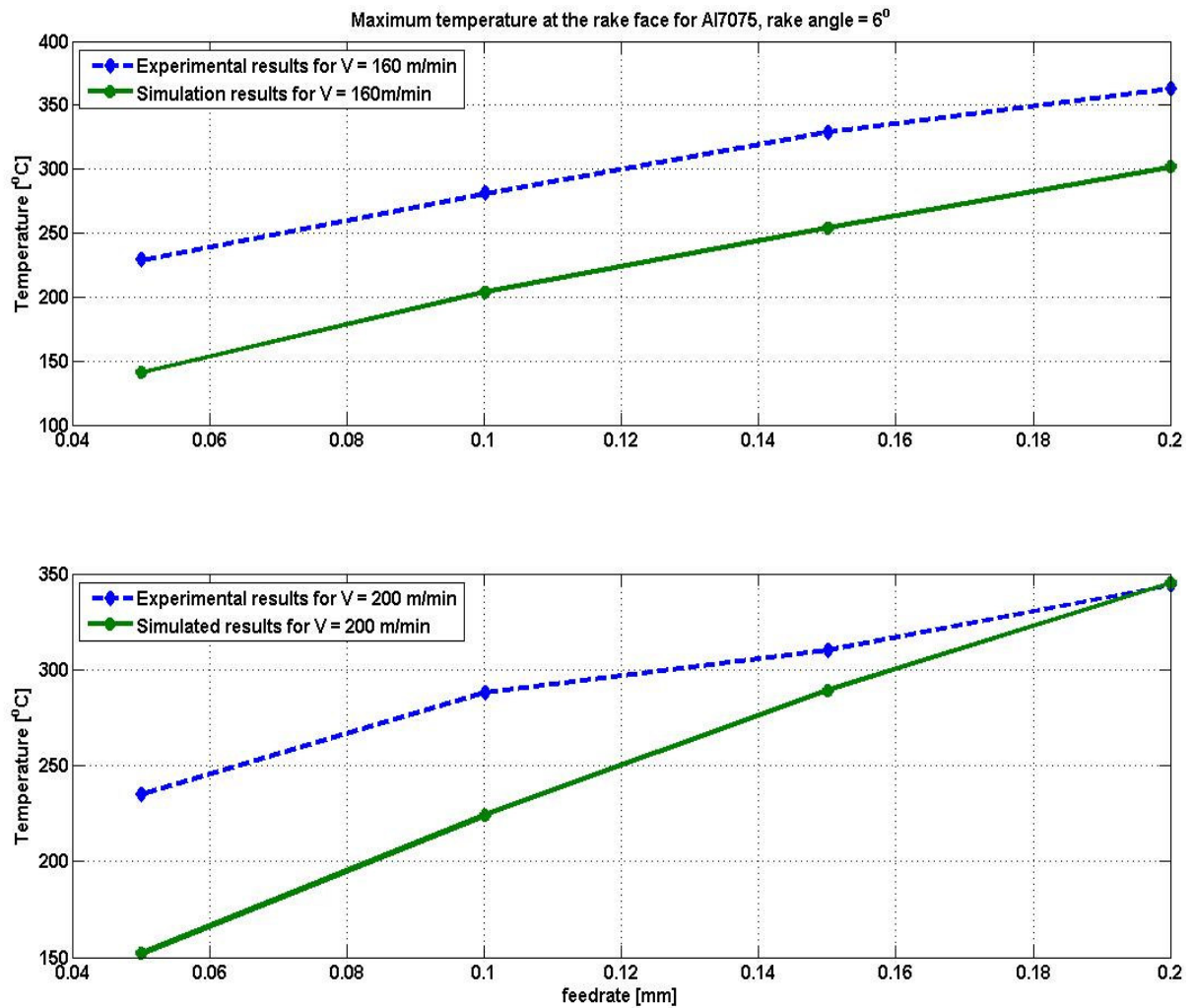


Fig. 5.1: Maximum temperatures at the rake face – comparison of tool and workpiece for Al 7075 material and tool with 6 degrees of rake angle

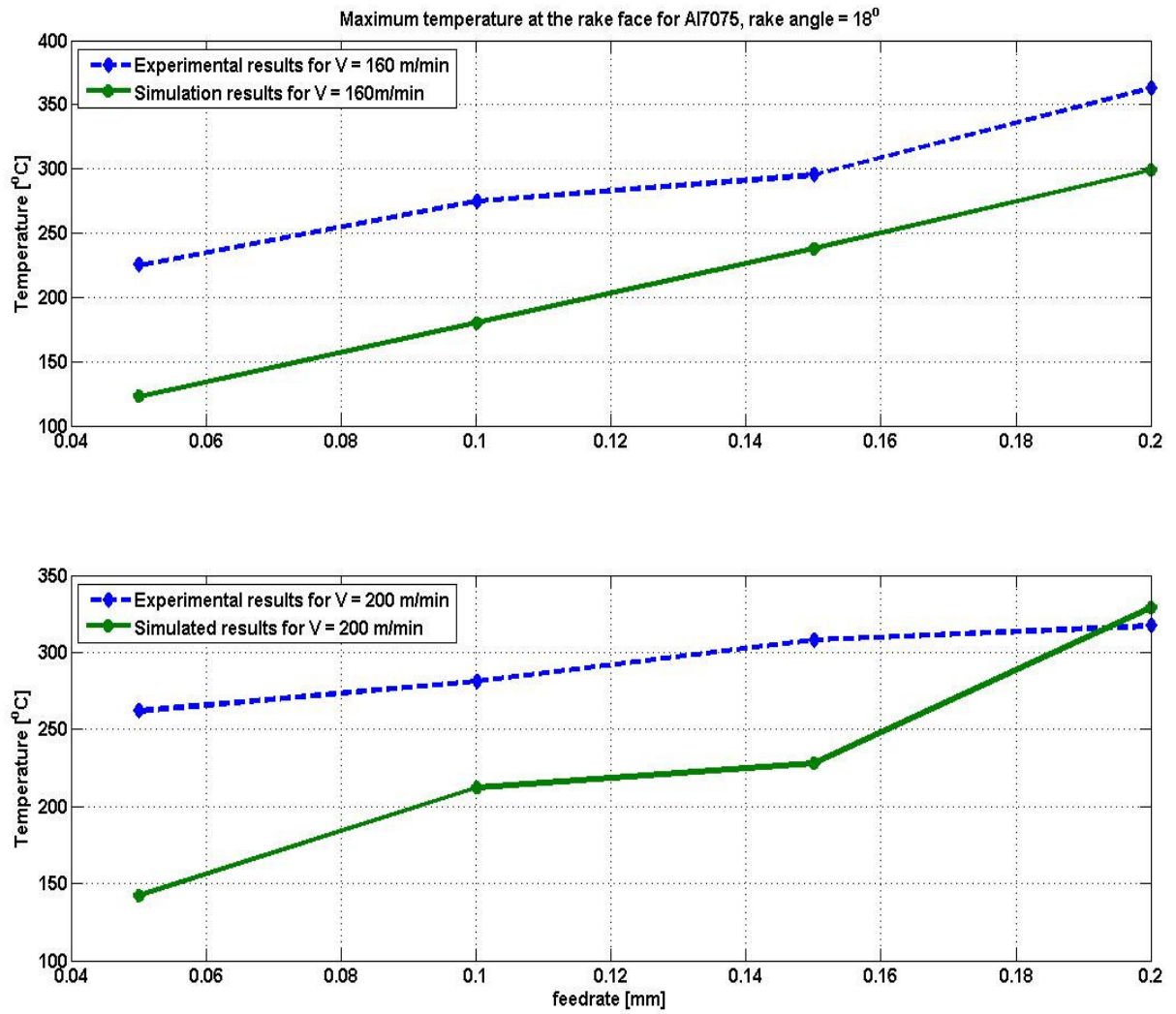


Fig. 5.2: Maximum temperatures at the rake face – comparison of tool and workpiece for Al 7075 material and tool with 18 degrees of rake angle

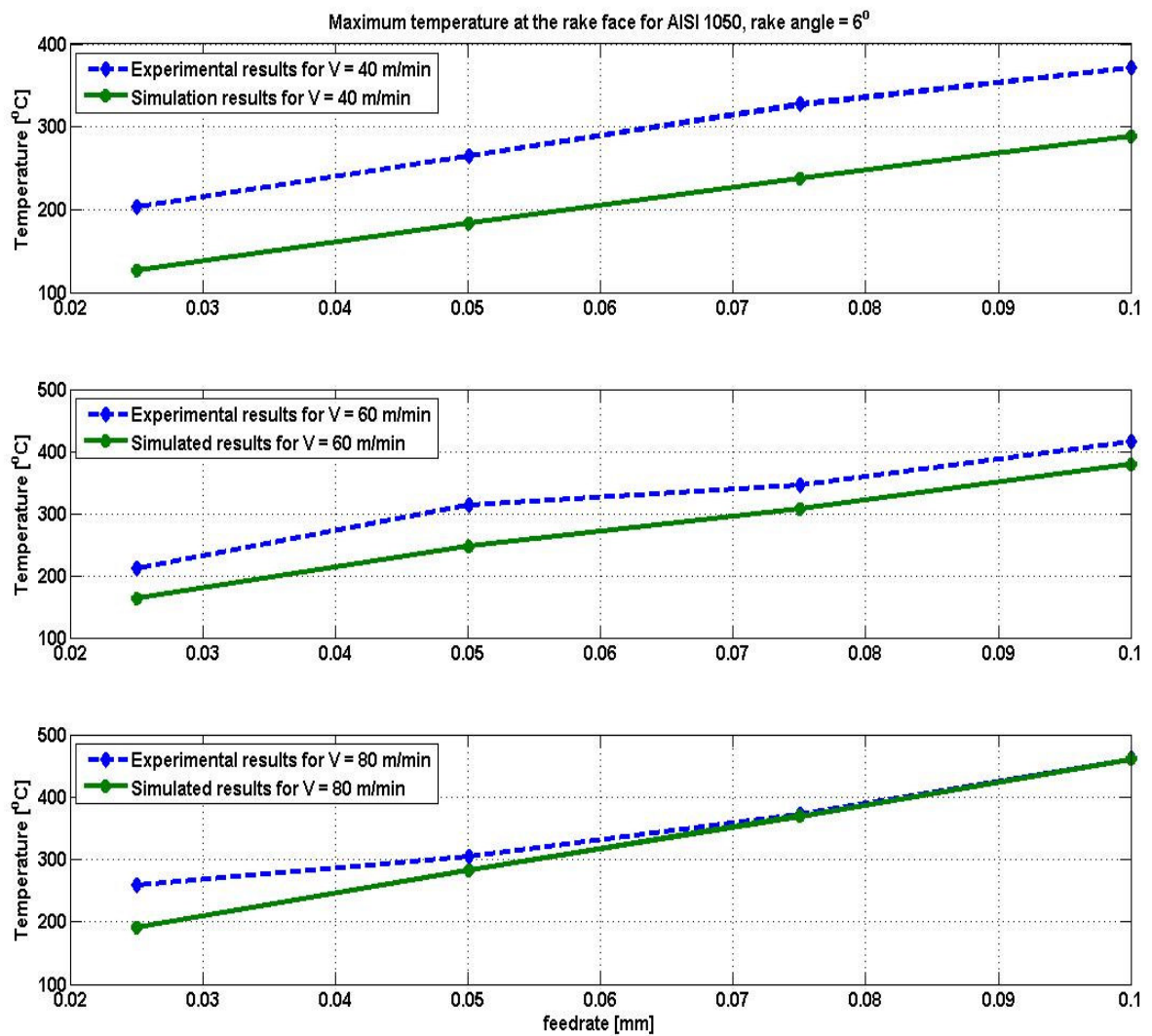


Fig. 5.3: Maximum temperatures at the rake face – comparison of tool and workpiece for AISI 1050 material and tool with 6 degrees of rake angle

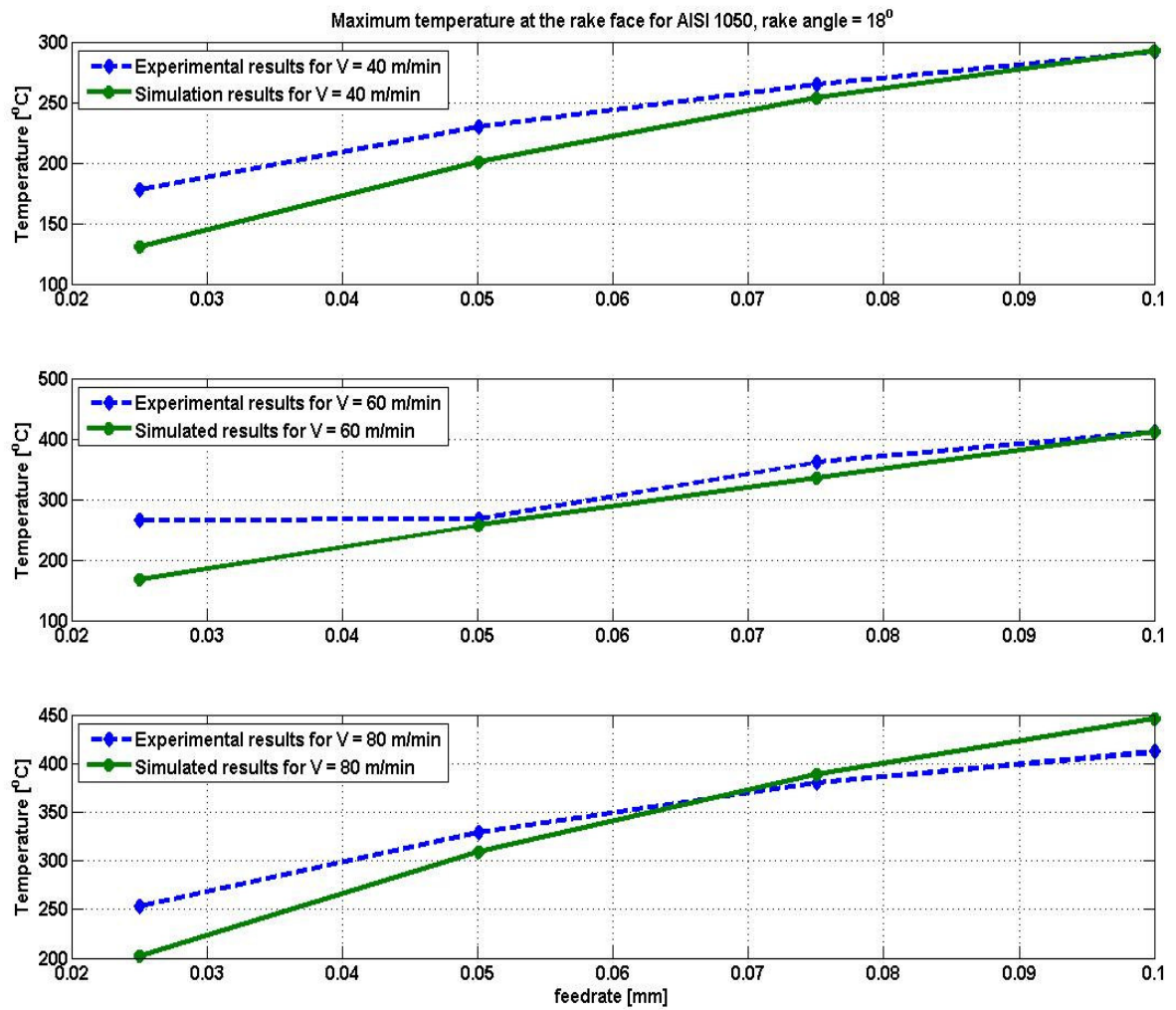


Fig. 5.4: Maximum temperatures at the rake face – comparison of tool and workpiece for AISI 1050 material and tool with 18 degrees of rake angle

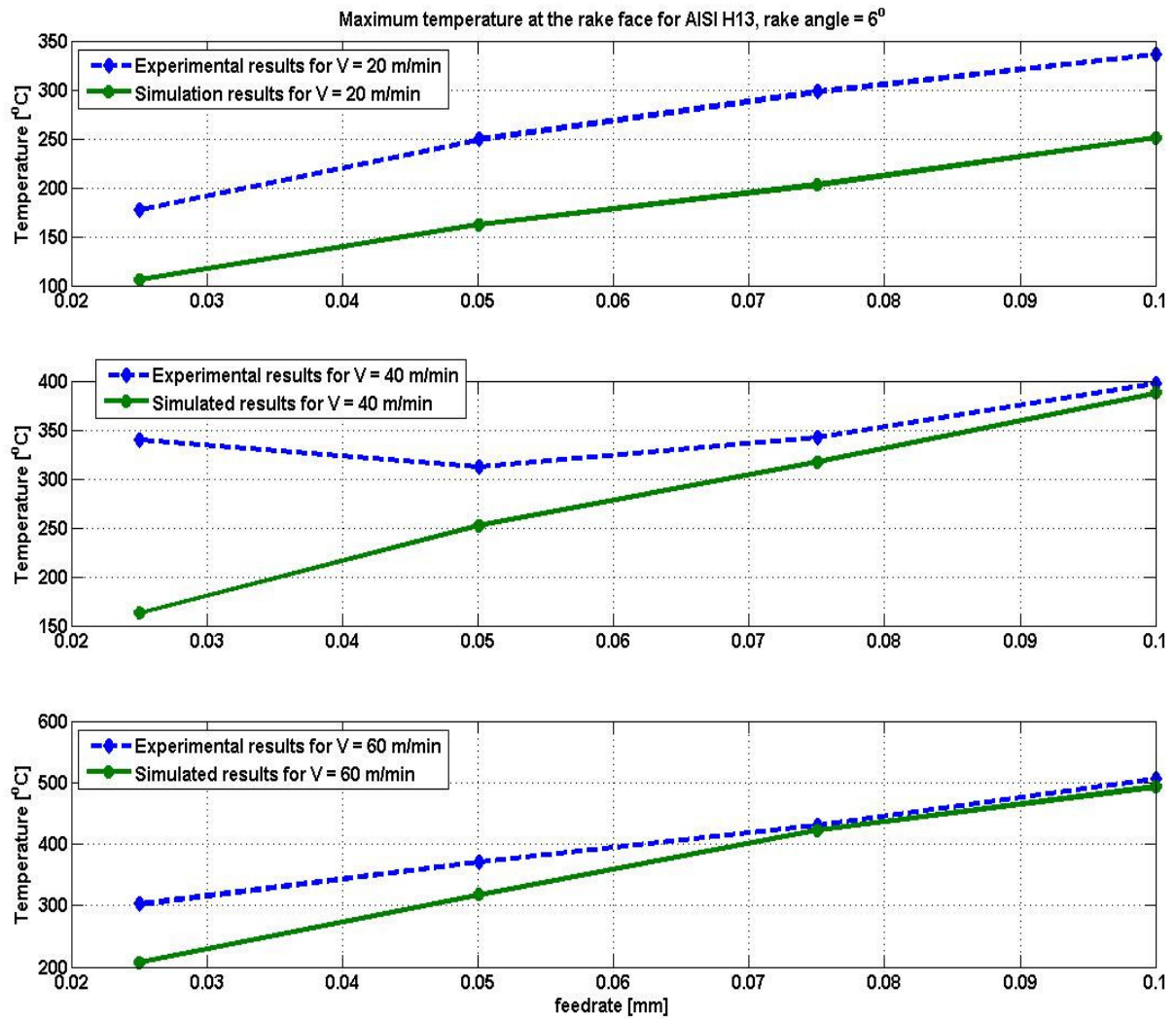


Fig. 5.5: Maximum temperatures at the rake face – comparison of tool and workpiece for AISI H13 material and tool with 6 degrees of rake angle

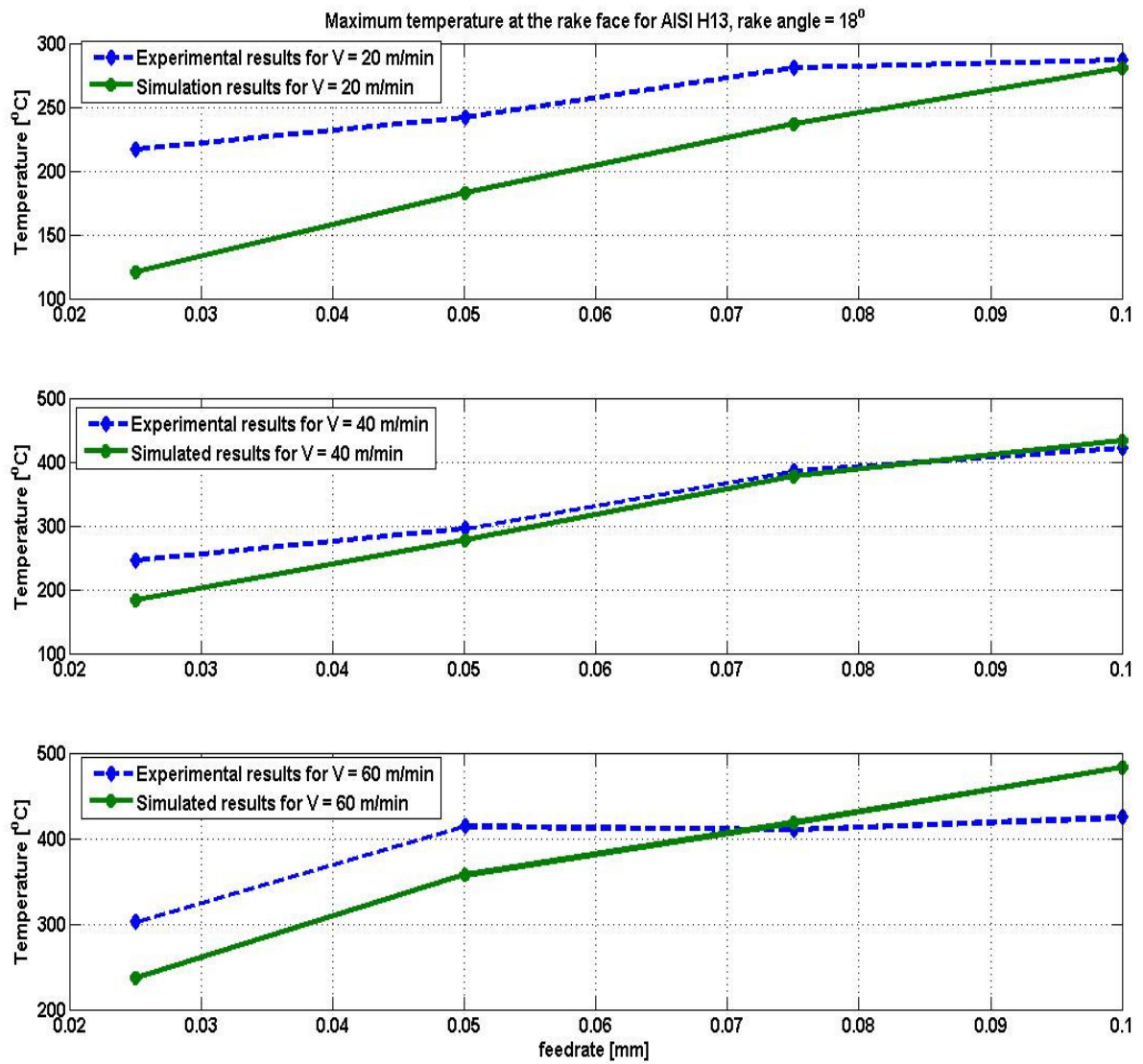


Fig. 5.6: Maximum temperatures at the rake face – comparison of tool and workpiece for AISI H13 material and tool with 18 degrees of rake angle

5.2 Residual Stress Prediction Validations

To validate the analytical model developed to predict the residual stresses in machining processes, experimentations were done at Pratt & Whitney Co. and simulations were performed at the corresponding experimental conditions. The cutting conditions and tool and workpiece properties are given in Table 5.3. To do the measurements, X-ray diffraction technique was used, and the measurements were done in the depth of cut and cutting velocity directions, to see the changes in the feed direction.

Table 5.3. Cutting conditions and tool and workpiece properties

	P2	P3	P4
Cutting velocity (m/min)	54.9	25	54.9
Feed rate (mm/rev)	0.075	0.15	0.15
Depth of cut (mm)	0.25	0.25	0.25
Tool rake angle (°)	0	5	0
Workpiece	Waspaloy	Waspaloy	Waspaloy
Tool	Carbide VC29	Carbide VC29	Carbide VC29
Contact radius (μm)	34.1	51.6	39.2
Tool nose radius (mm)	2.39	2.39	2.39

For the conditions given in Table 5.3, tool and chip thermal distributions were found, as explained in Chapter 3, but for two-dimensional plane. These distributions were coupled and solved together, using the finite difference method explained before. Then, the workpiece thermal field is also computed. These thermal distributions for chip, tool and workpiece at P3 conditions are given in Figures 5.7-5.9, to show an example. More examples and more detailed explanation for the thermal distributions can be found in [19,20], and similar three dimensional temperature predictions can be found in Appendix. Three-dimensional solution to the problem results in a temperature distribution at the rake face on the tool as in Figure 5.10. Residual stress predictions are done using the analytical

method explained in this study, and are compared for three different conditions (Table 5.3). The comparison of these results can be found in Figure 5.11-5.13. Figure 5.14 shows the optimization for the plastic step size. Since for a unit step size ($dx=1$), the results are not good with the measurements and it develops with the decrease of the step size, we lower it until it becomes small enough to simulate the measurements. Yet, we do not decrease the value even further due to time constraints.

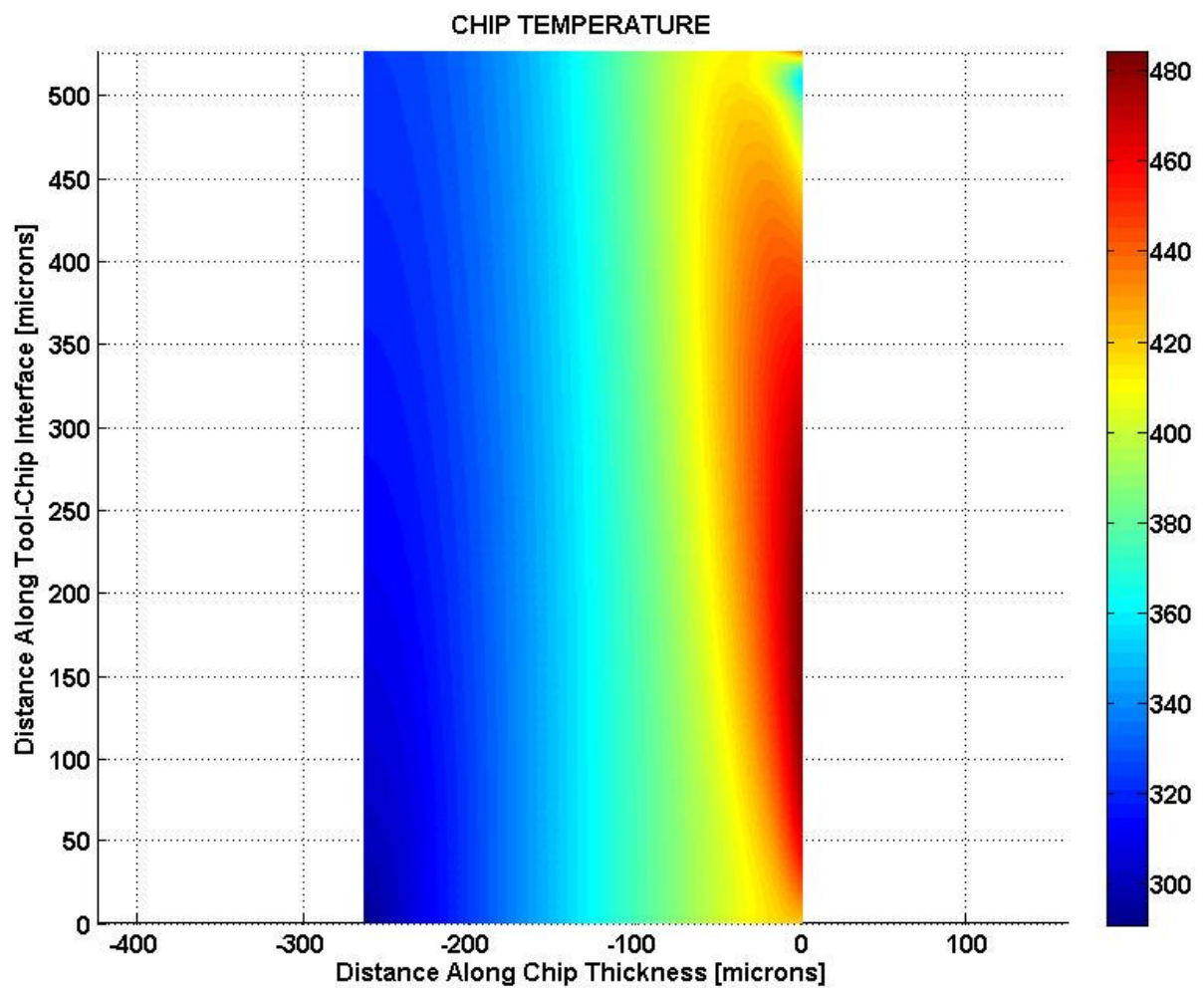


Fig. 5.7. Thermal field for the chip

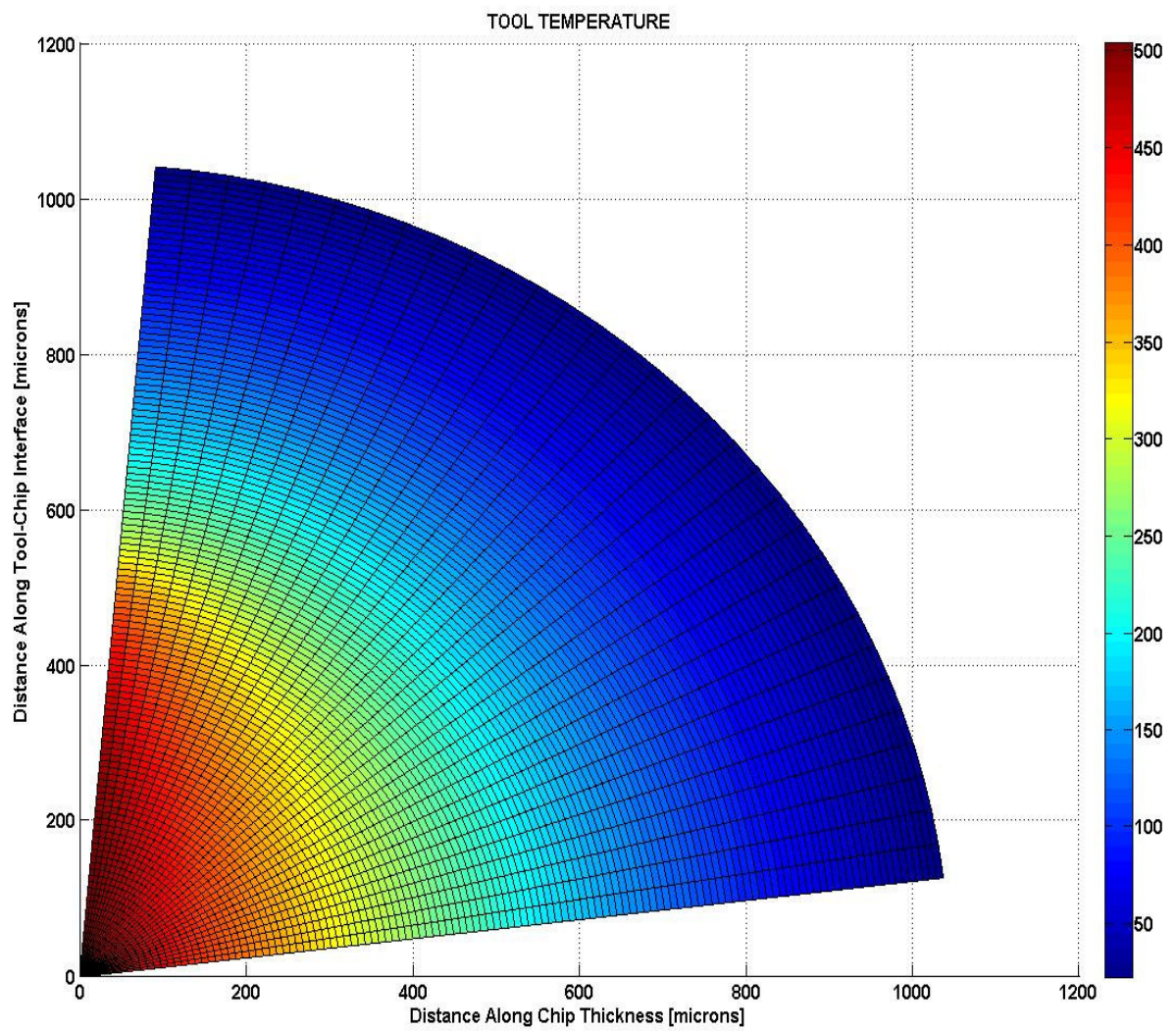


Fig. 5.8 Thermal field for the tool

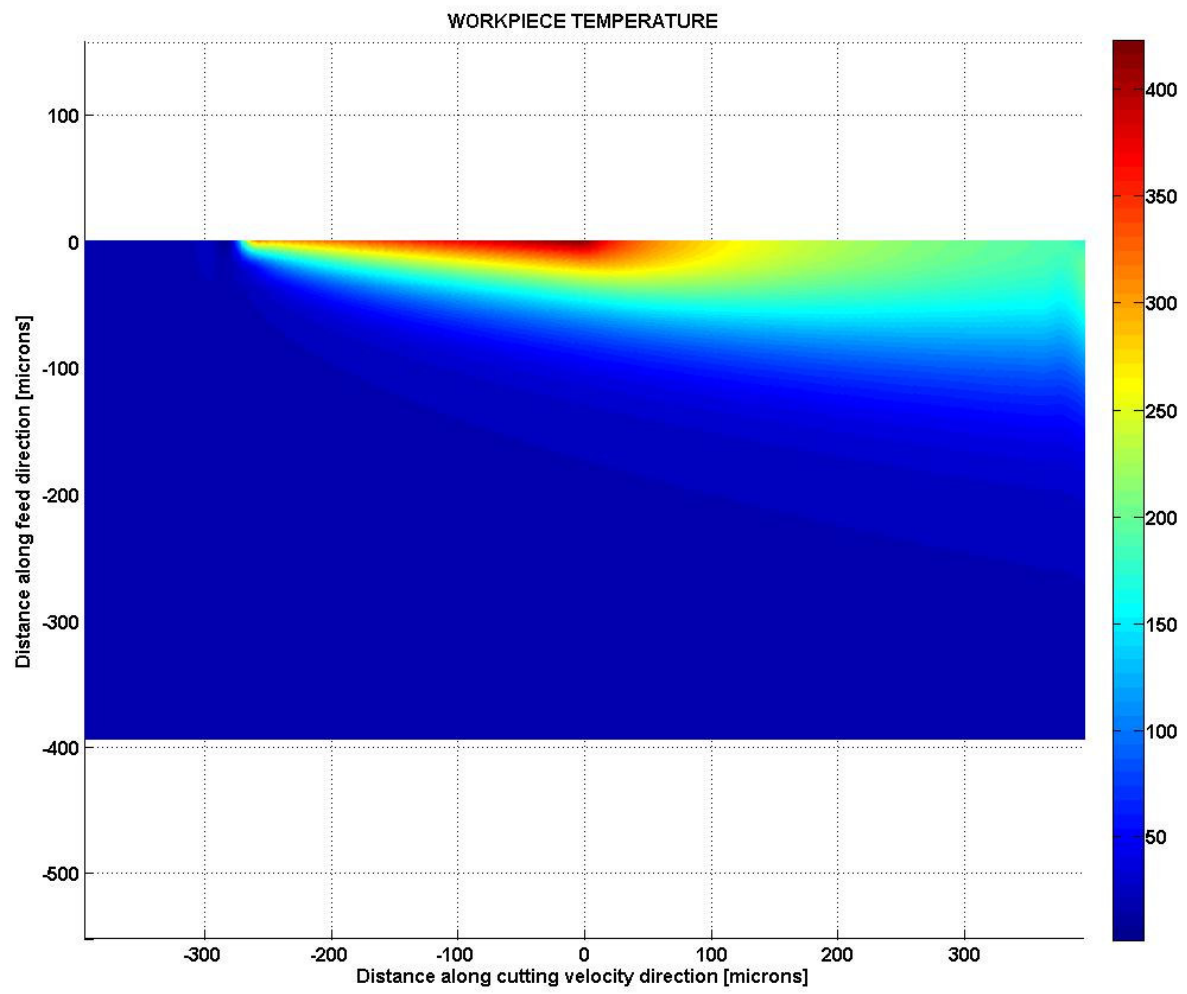


Fig. 5.9 Thermal field for the workpiece

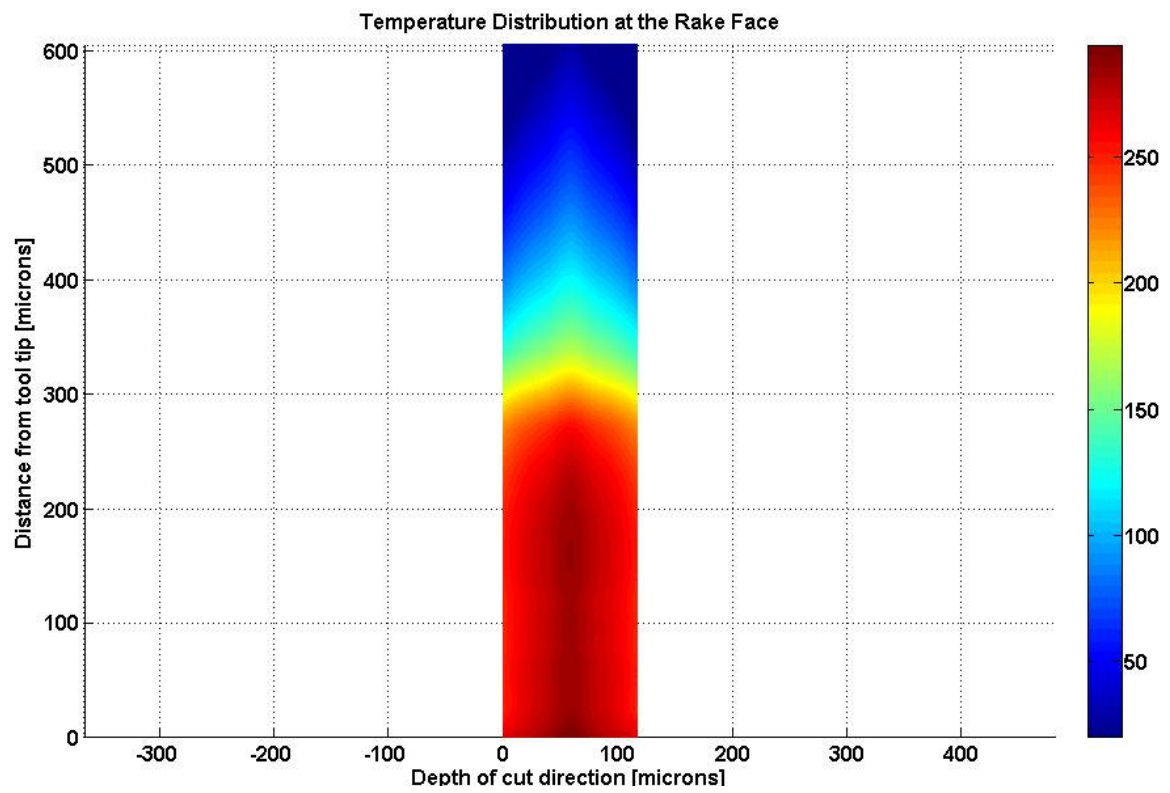


Fig. 5.10 Temperature distribution at the rake face

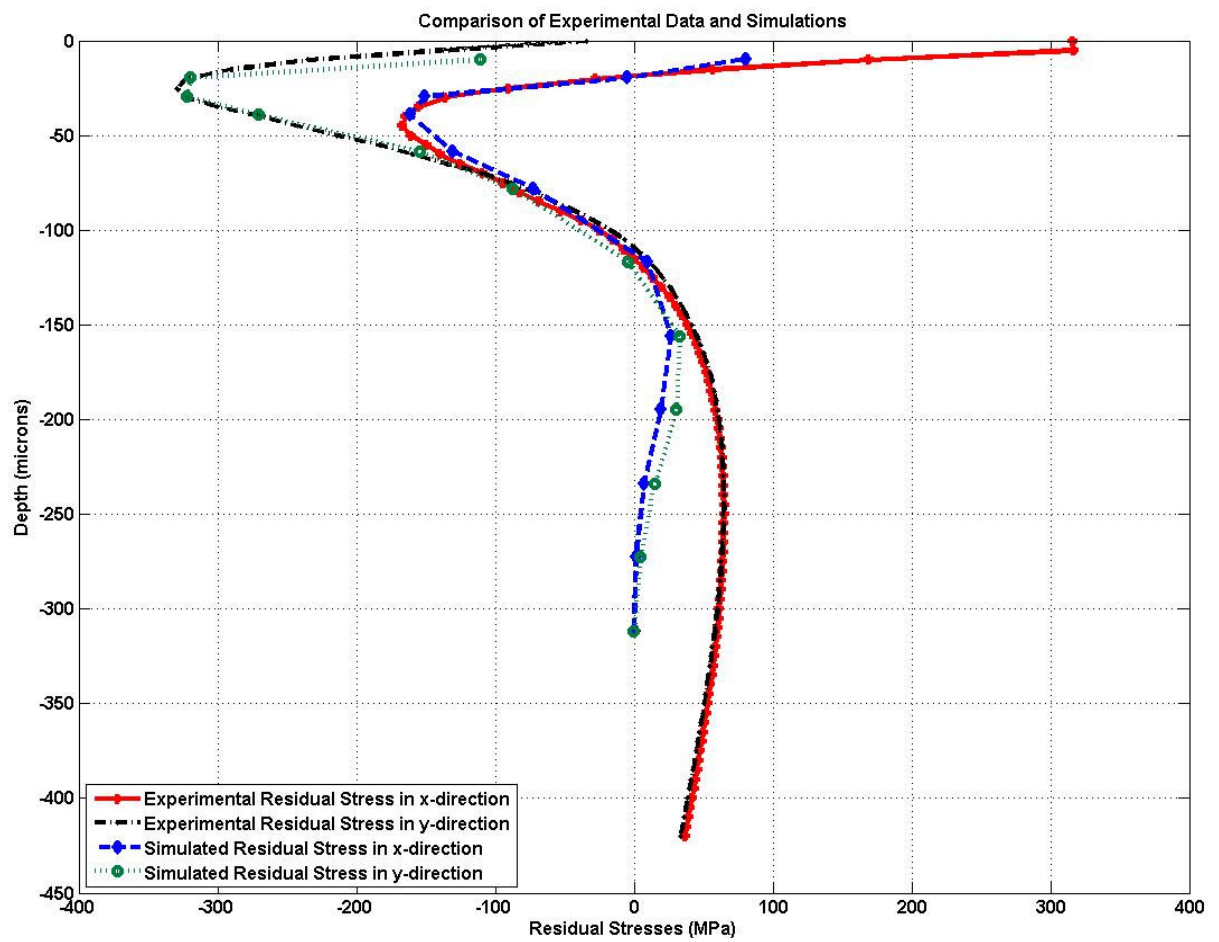


Fig. 5.11: Comparison of Residual Stress Predictions and Experimental Results for P2 conditions (Table 5.3)

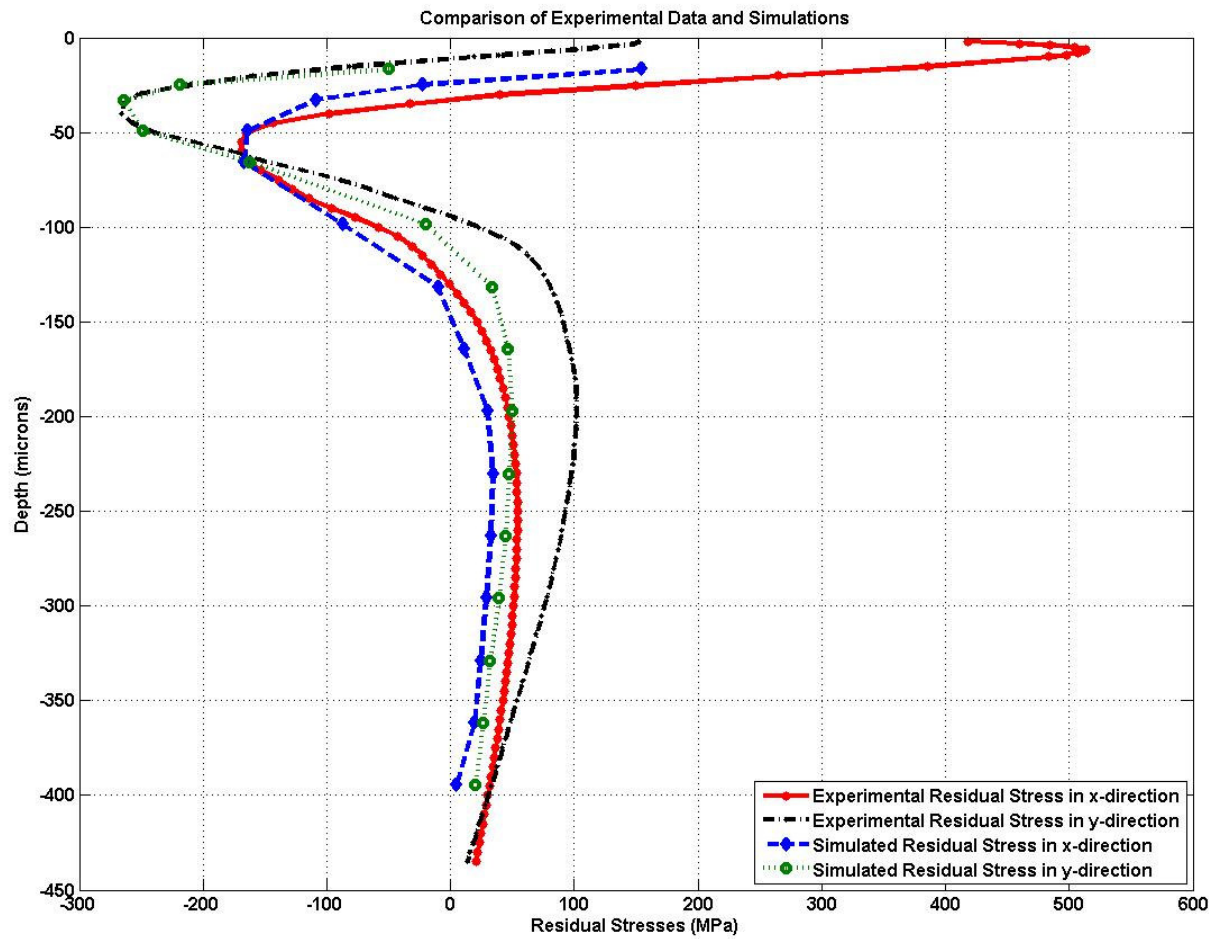


Fig. 5.12: Comparison of Residual Stress Predictions and Experimental Results for P3 conditions (Table 5.3)

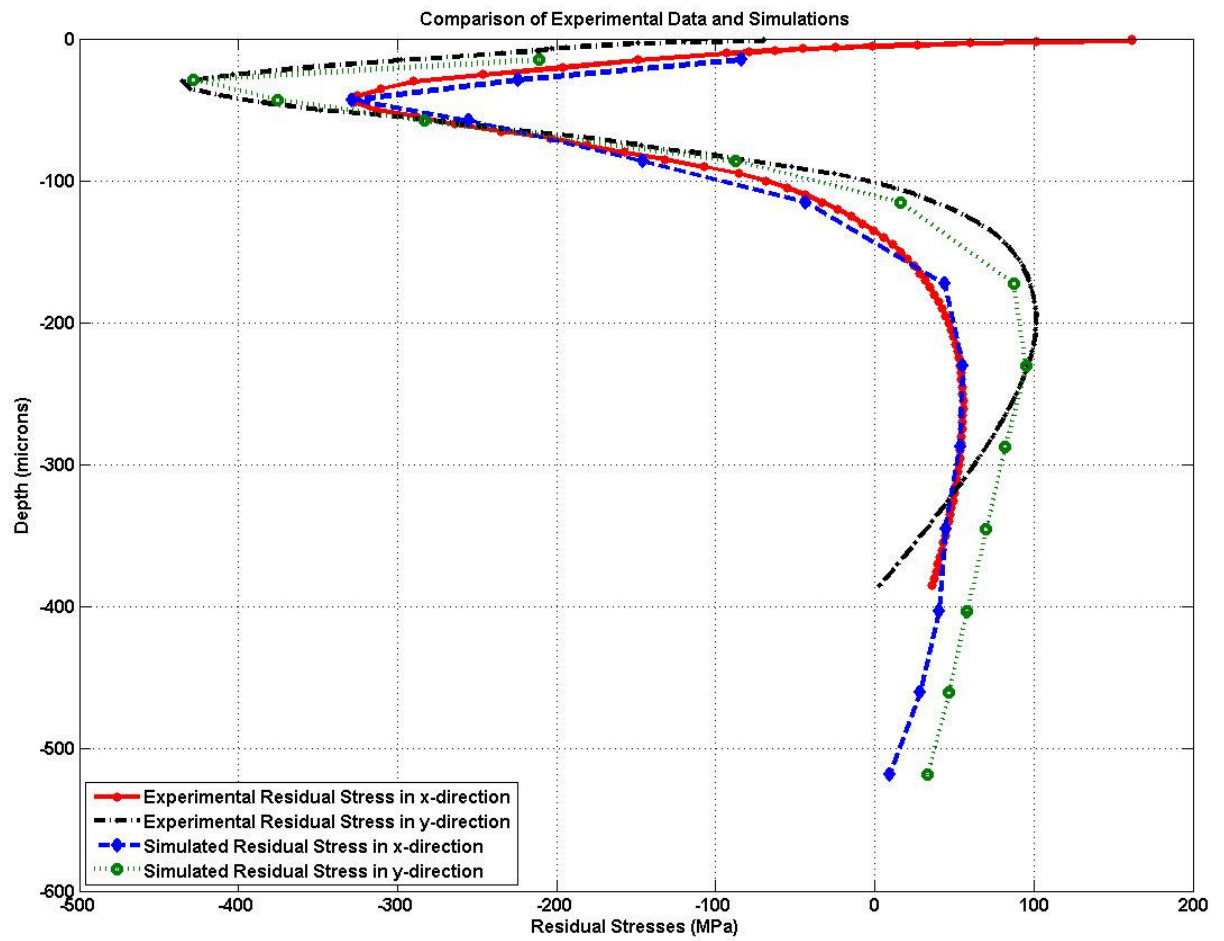


Fig. 5.13: Comparison of Residual Stress Predictions and Experimental Results for P4 conditions (Table 5.3)

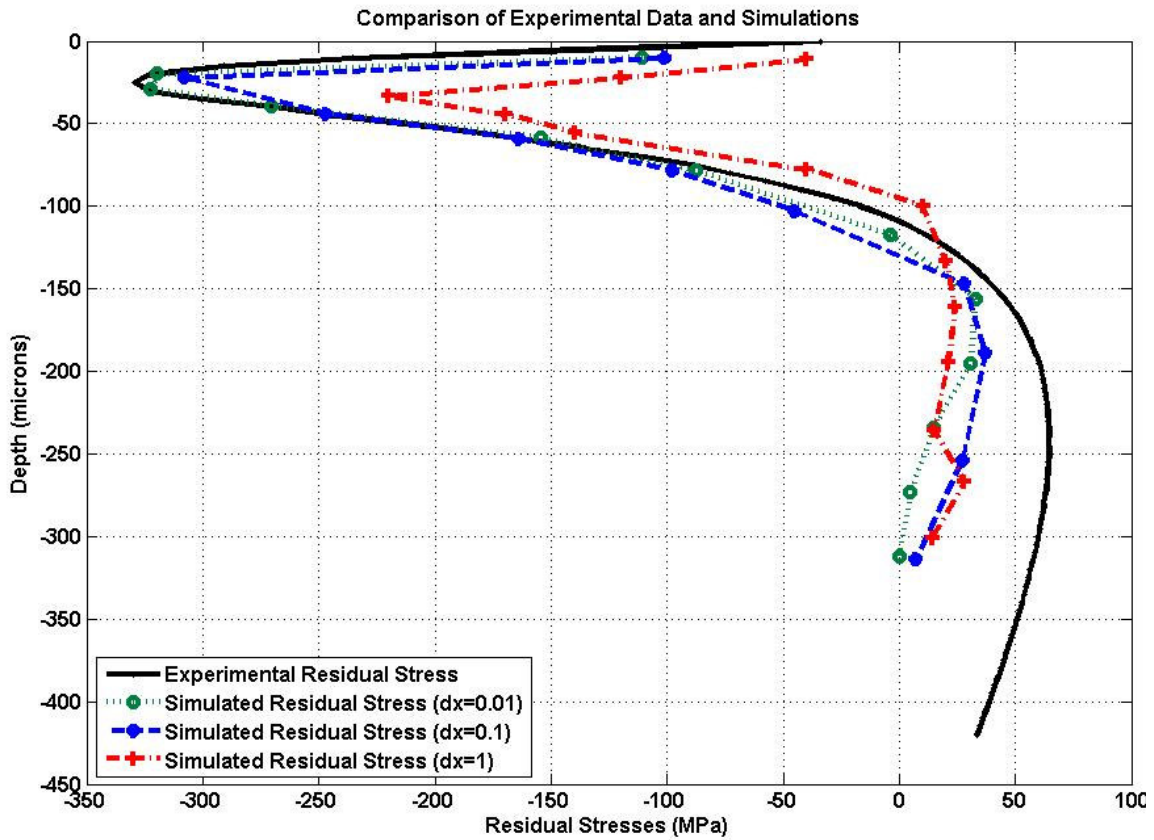


Fig. 5.14: Comparison of measurements and simulations with different plastic step sizes ($dx=1, 0.1, 0.01$).

It can be seen from the figures that the simulations are in very good agreement with the experiments; and the following results can be drawn from the findings:

- (1) The peak compressive residual stresses that occur close to the surface (but not at the surface) can be predicted with very well accuracy for all of the simulations.
- (2) Deep surface residual stresses are harder to predict, since they become close to zero. However, for P4 conditions, the simulations are in good agreement with the experiments. For P2 and P3 conditions, there is still agreement with the experiments, although there are more deviations.

- (3) The surface residual stresses are not easy to predict due to numerical constraints. However, residual stresses very close to the surface are predicted with good agreement. These stresses can be used and extrapolated to make an educated guess about the exact surface residual stresses, and the results will be well enough.
- (4) As plastic step size decreases, the simulations converge to the measured values, and 100 plastic steps per one elastic step seems accurate enough with computational time not so much compromised.

In this study, anisotropic hardening (isotropic and kinematic hardening) is assumed, which is the real case [76]. In addition, triangular distribution of mechanical forces was used instead of rectangular distribution and Hertzian distribution, which are frequently used within literature. This is due to the fact that triangular distribution allows the use of shear forces unlike Hertzian distribution, and it is closer to the real case than rectangular distribution, since its effect decays with point of interest moves closer to contact length.

These assumptions made the model closer to the real case, not compromising from computational time comparably. The accuracy of the results are very good compared to the experiments, which are very reliable in this study, due to the improvements in measurement techniques and collaboration between experimentation and prediction teams. Predicting the residual stresses correctly allows the choice of cutting conditions, and since compressive surface residual stresses are often considered to be performance enhancing in terms of fatigue life [77], state of the final product might be altered accordingly.

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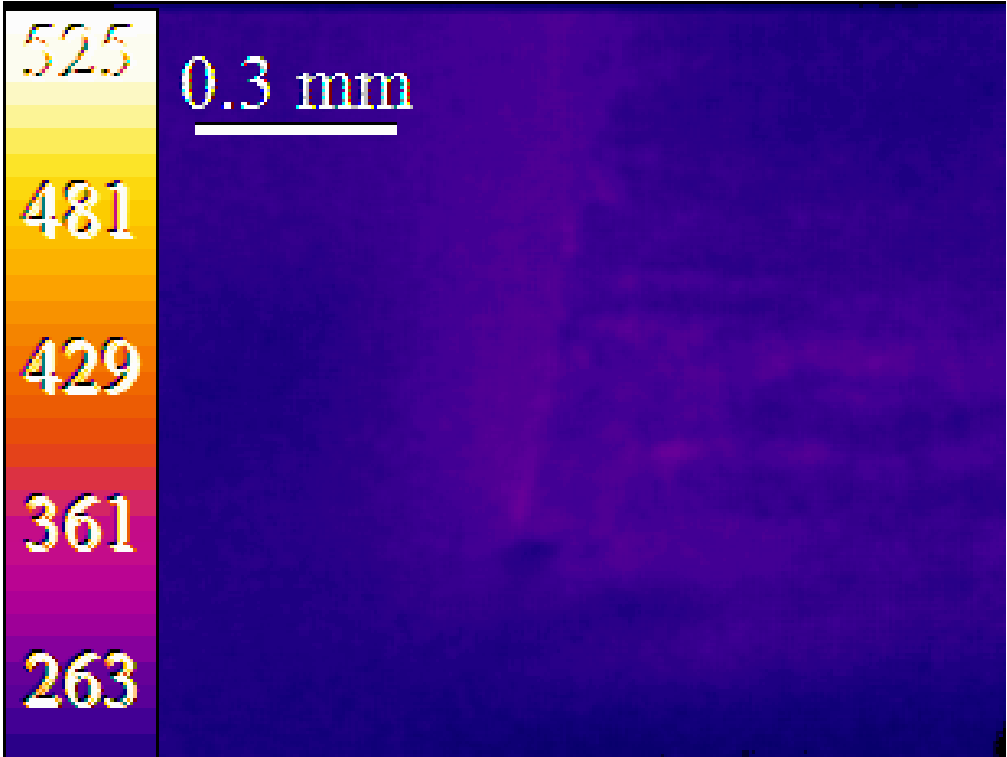
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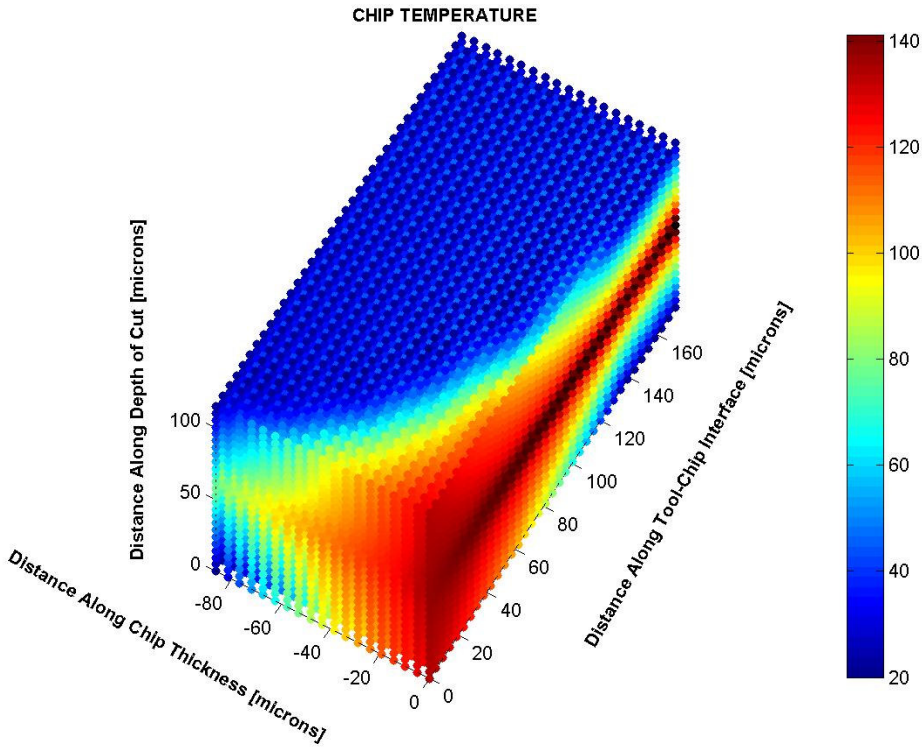
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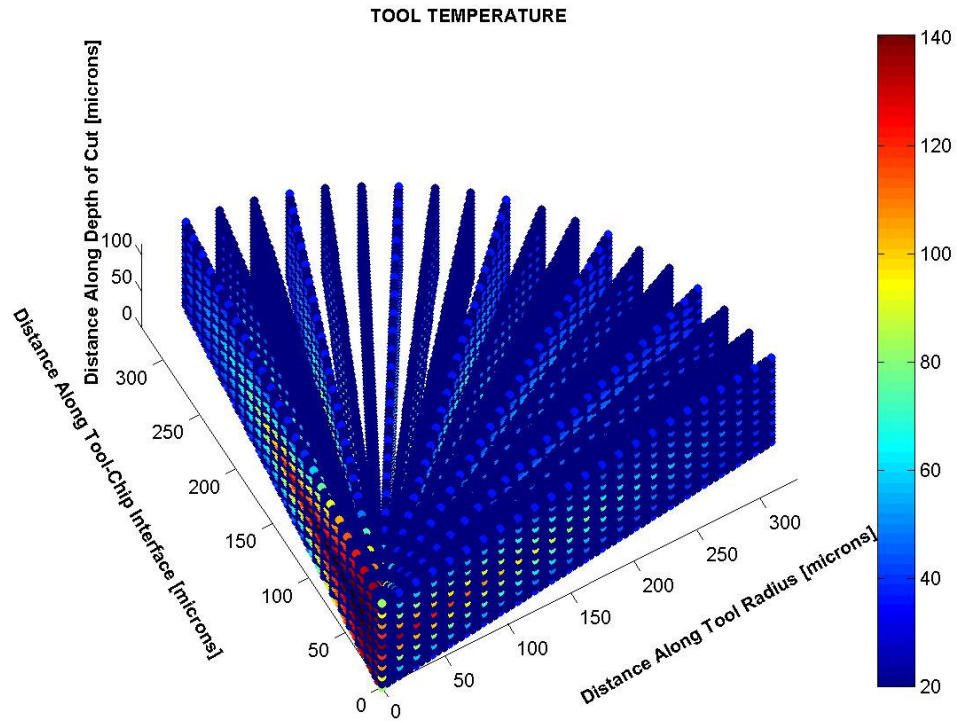
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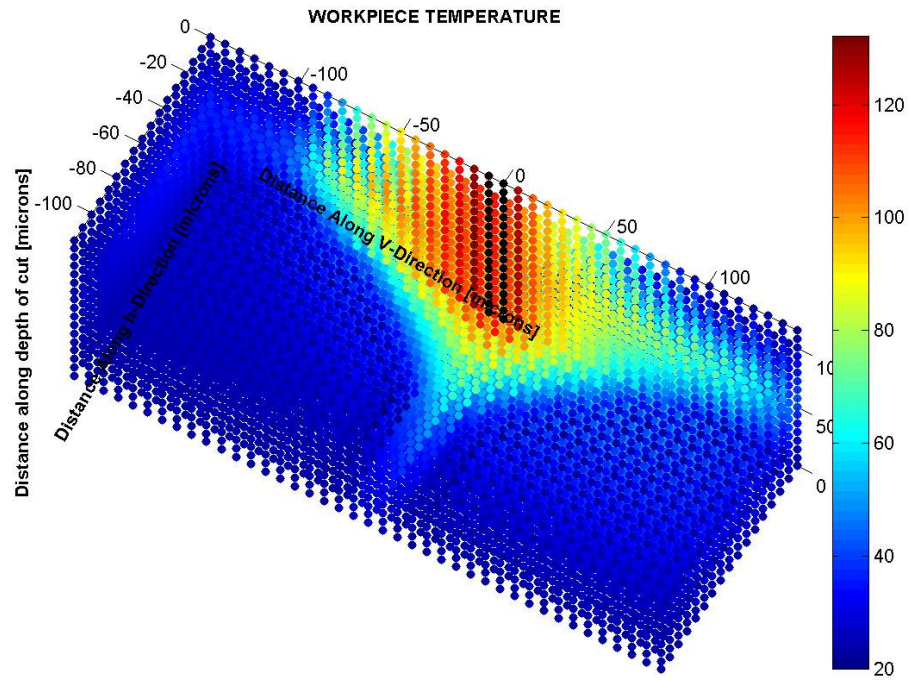
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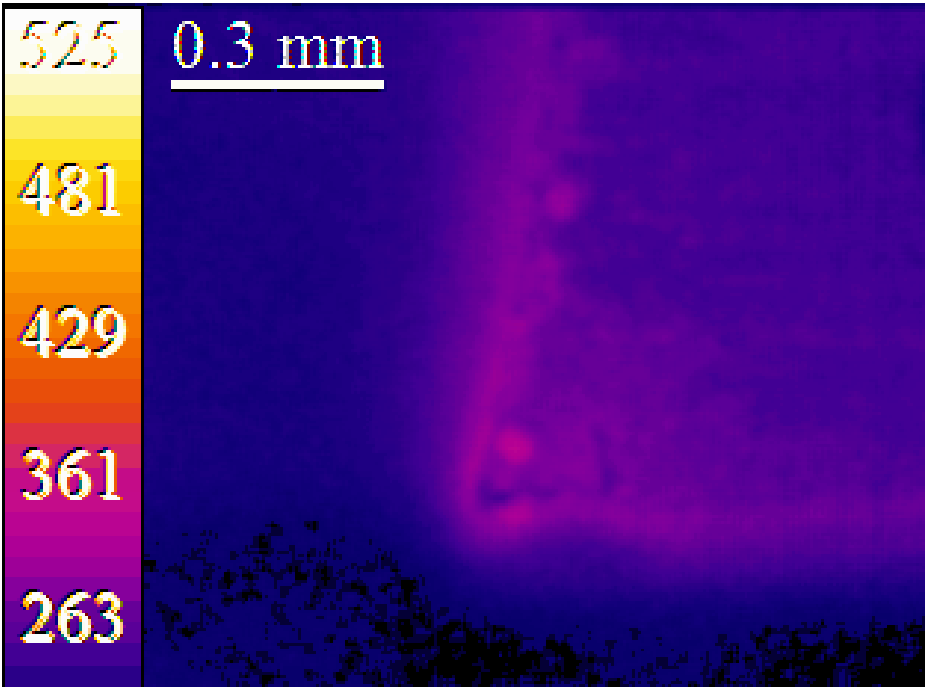


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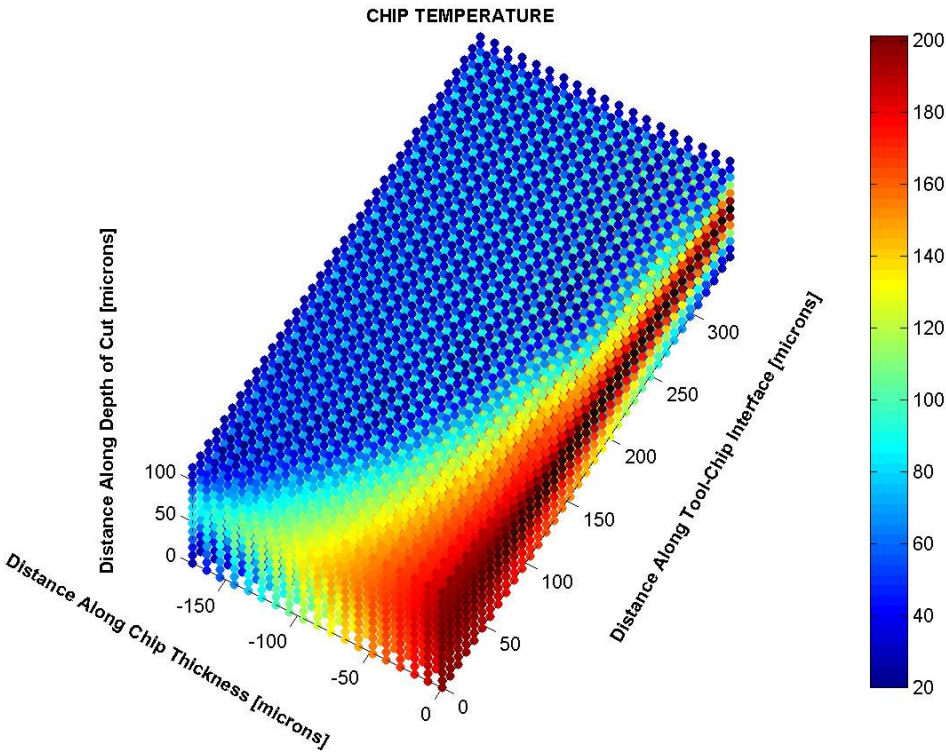


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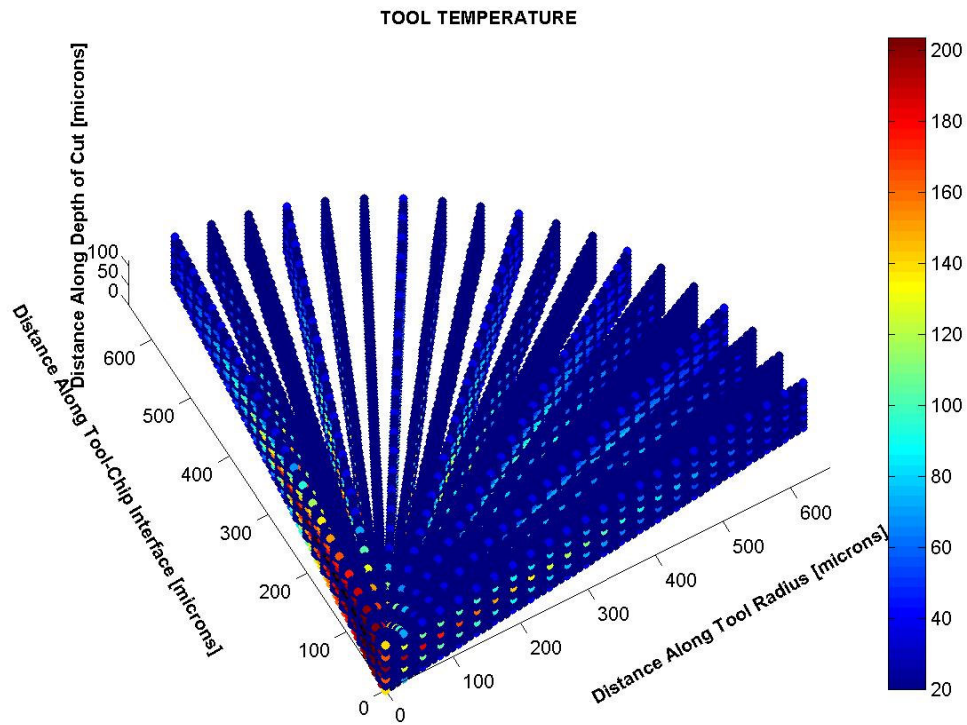
Fig. A.1: Validation for case 1. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



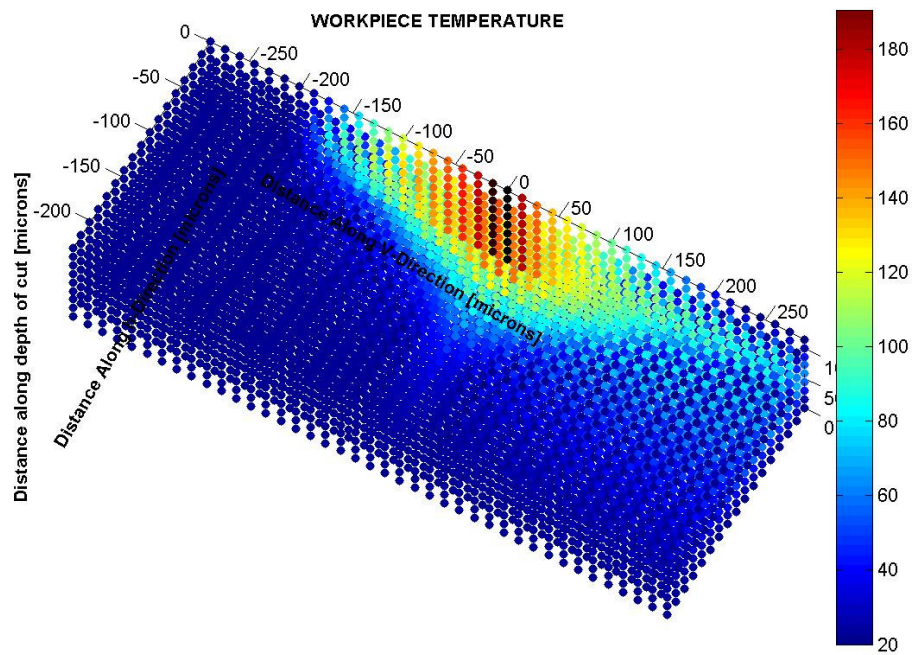
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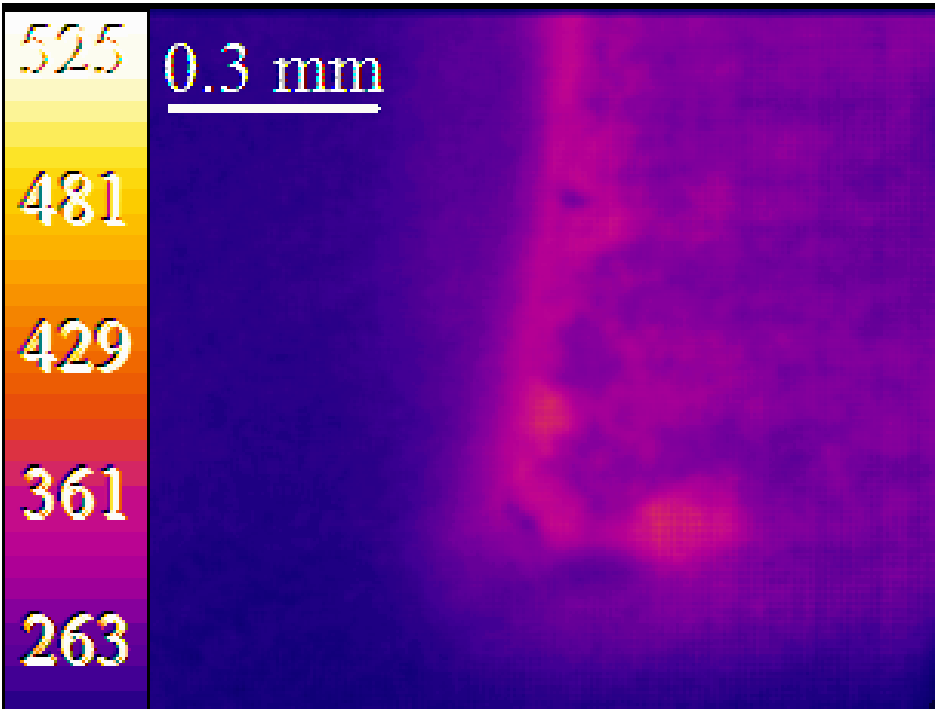


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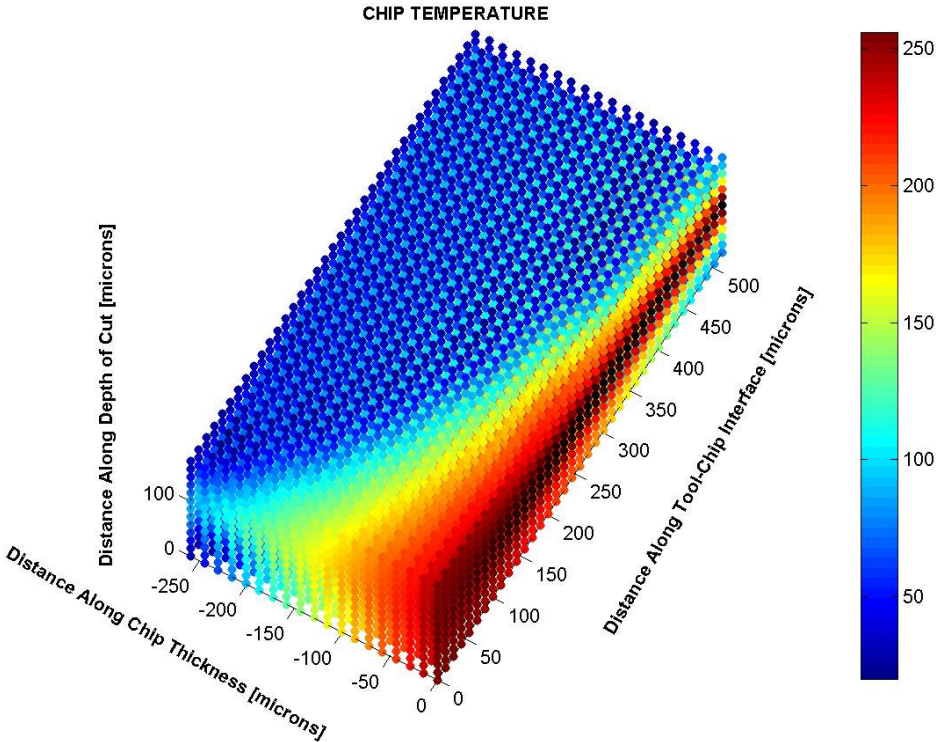


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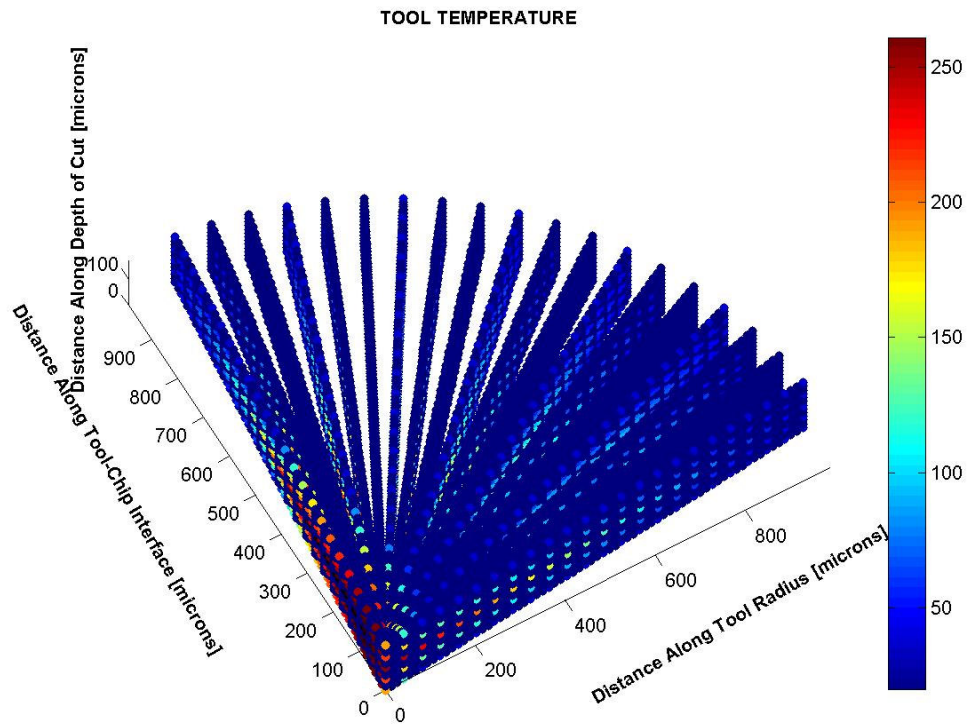
Fig. A.2: Validation for case 2. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



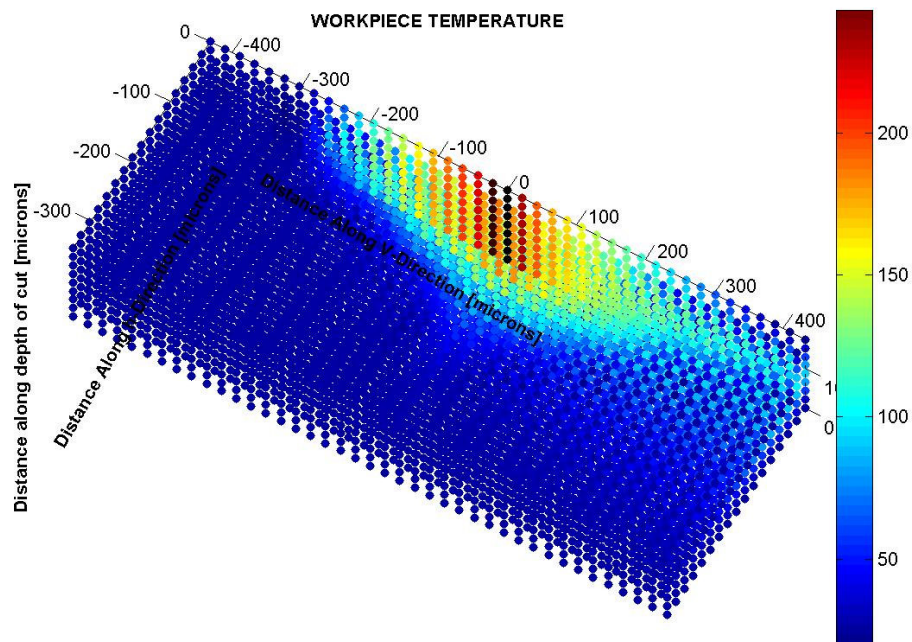
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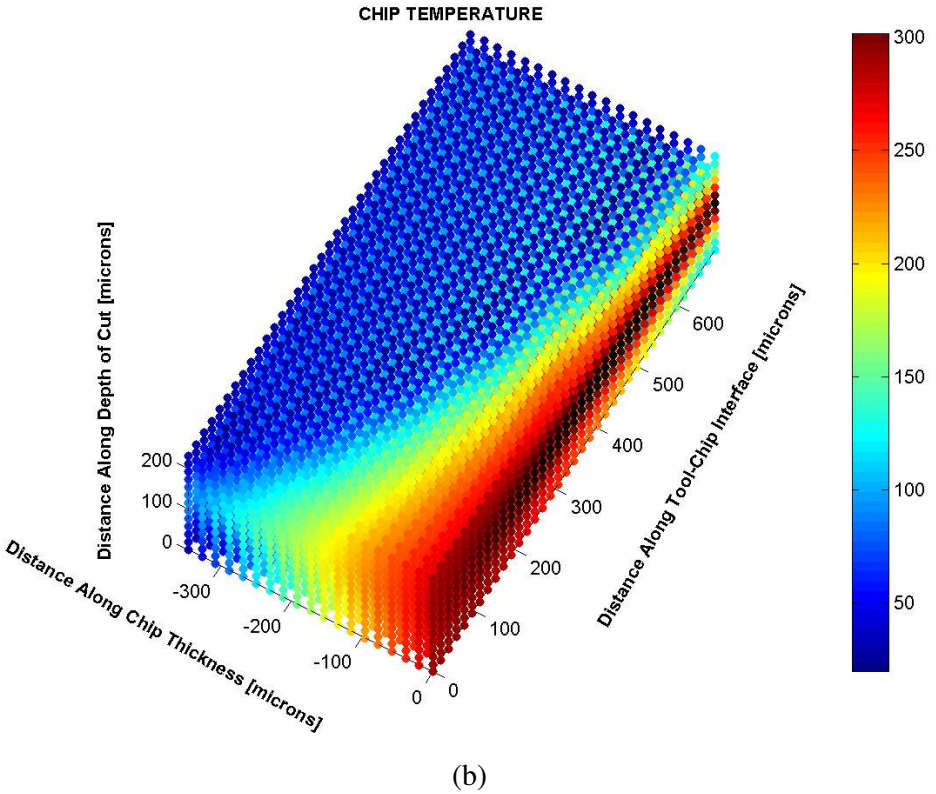
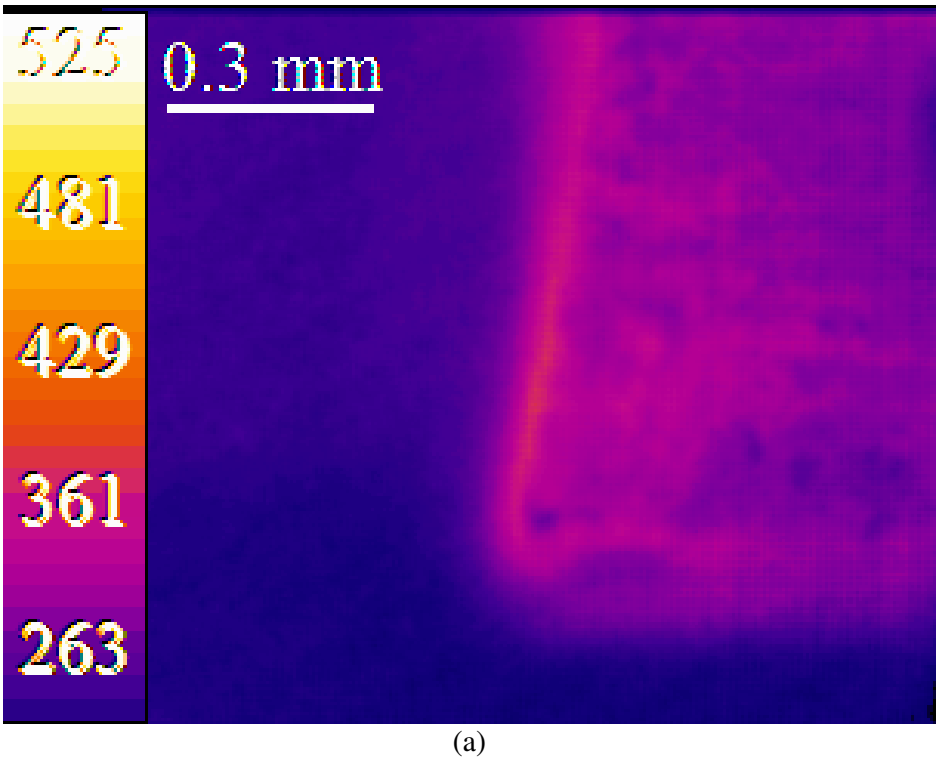


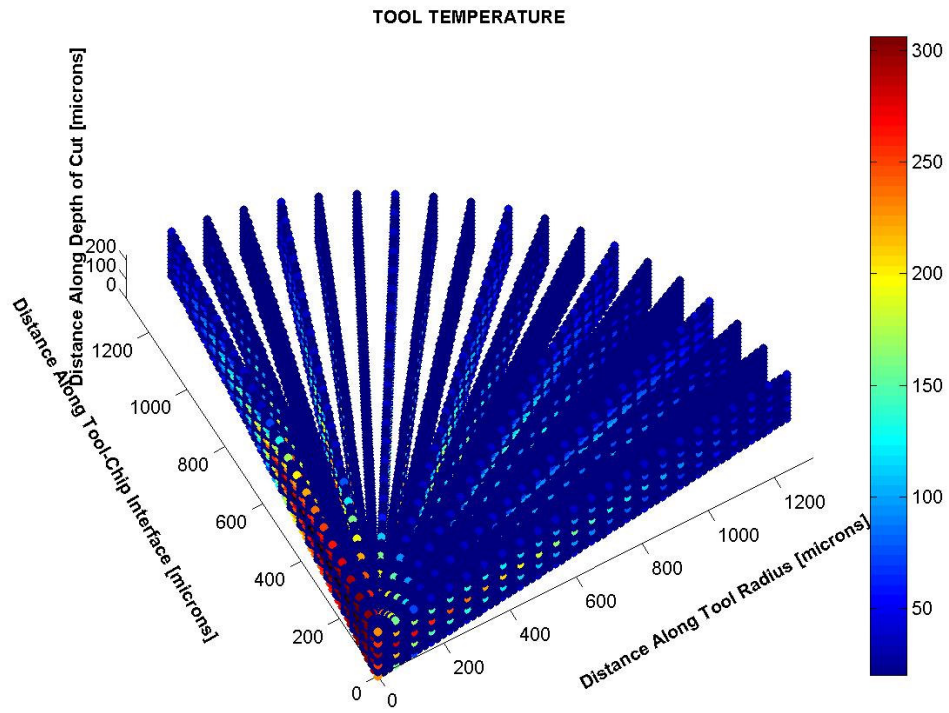
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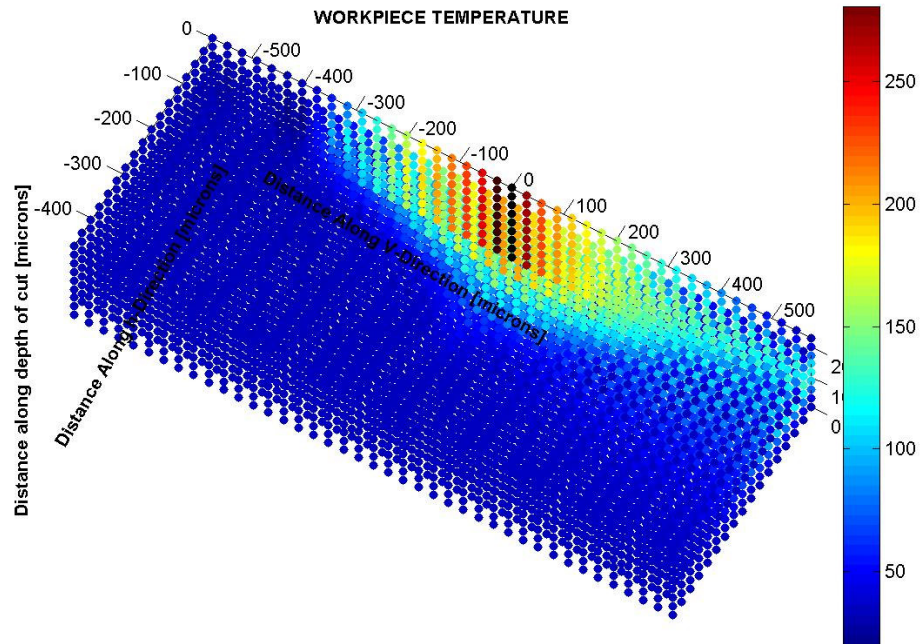
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Fig. A.3: Validation for case 3. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



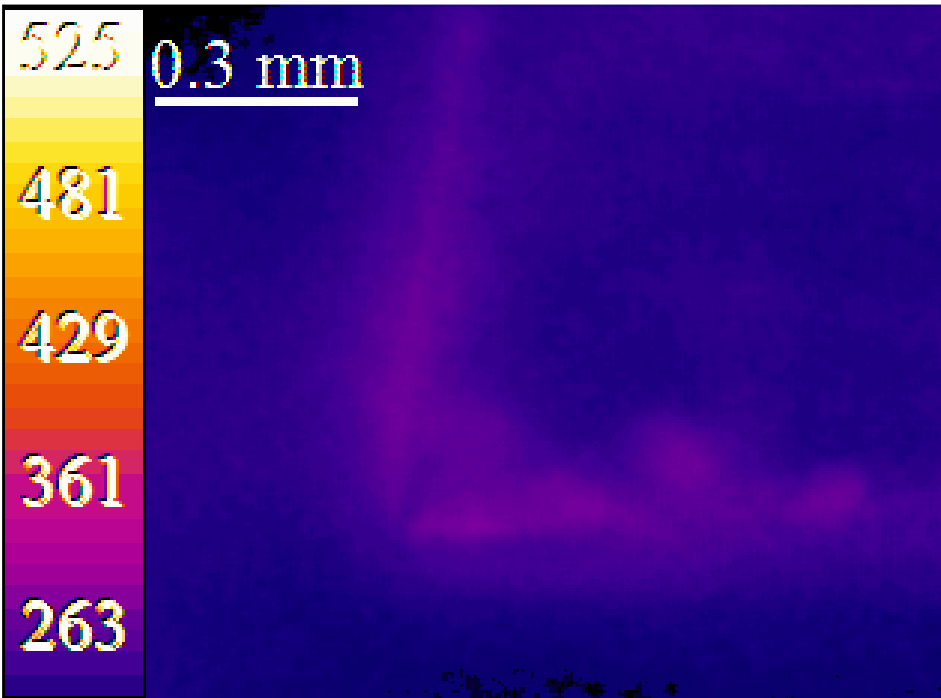


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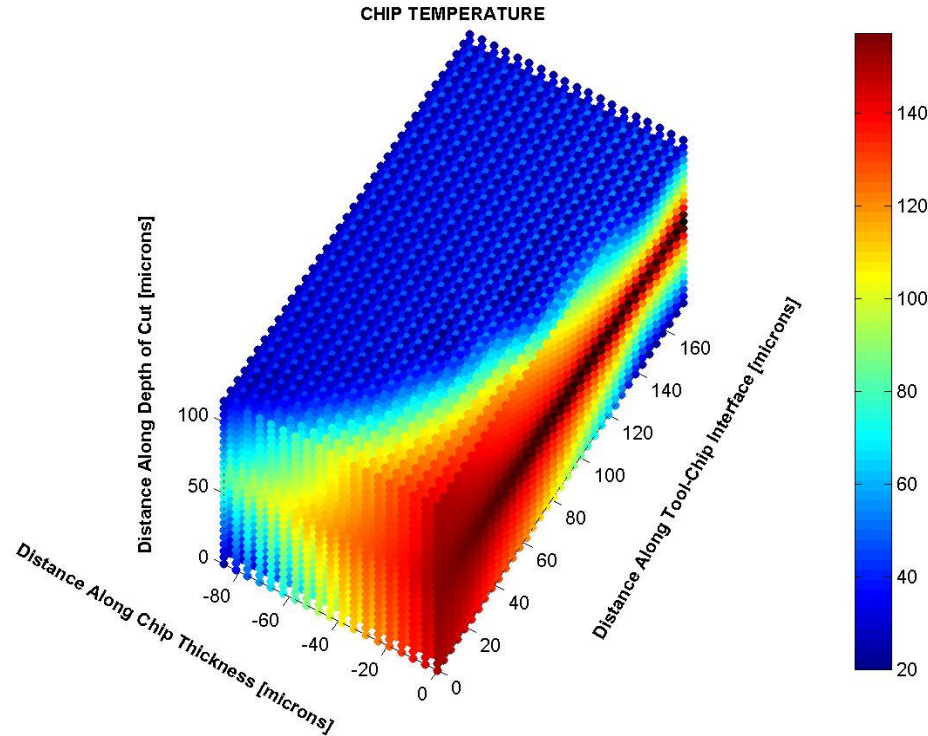


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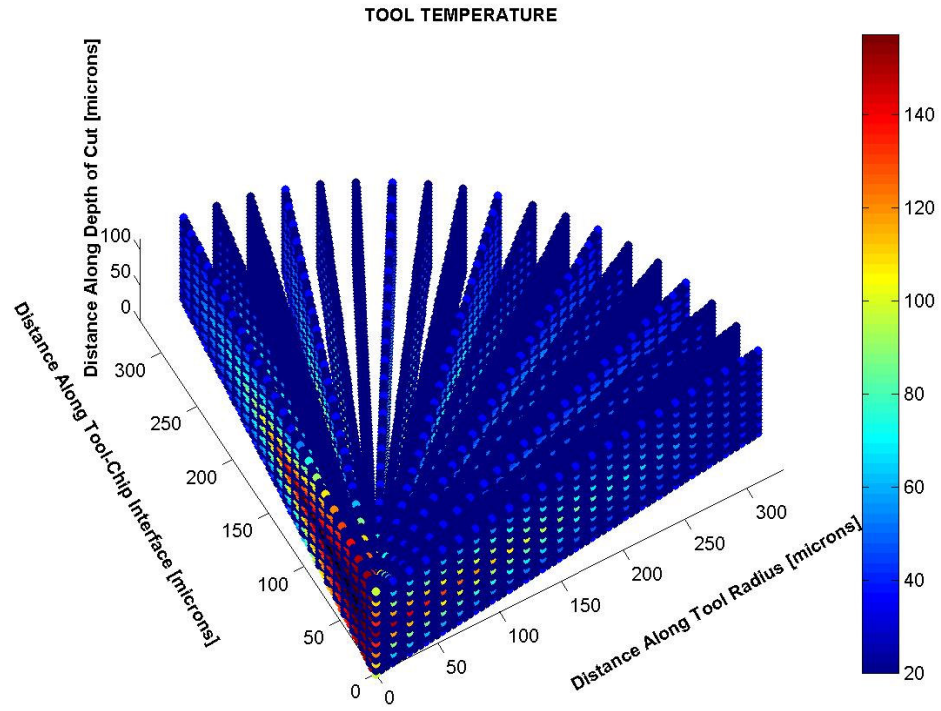
Fig. A.4: Validation for case 4. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



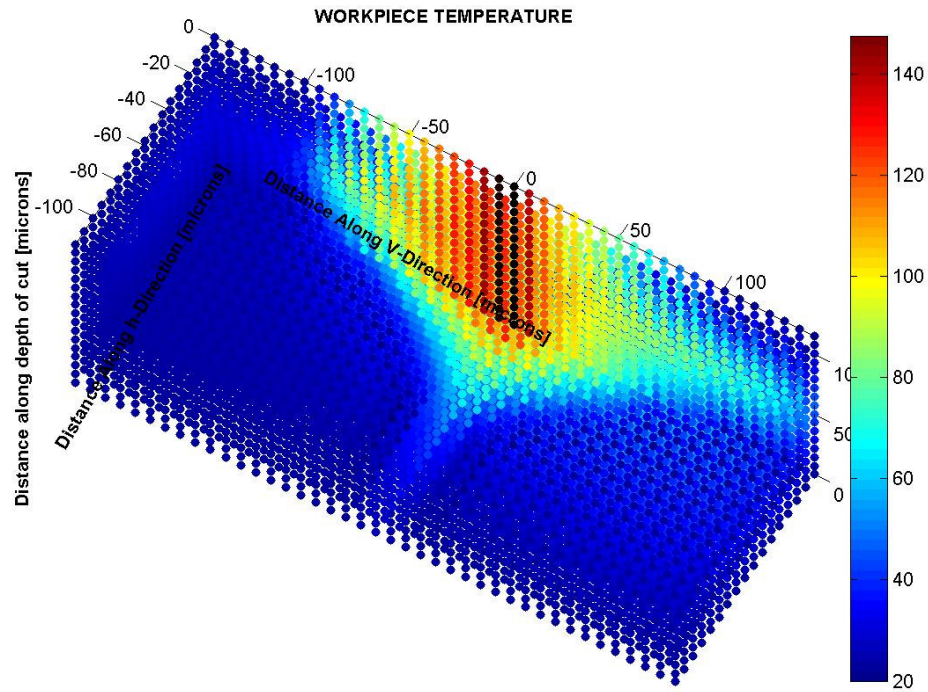
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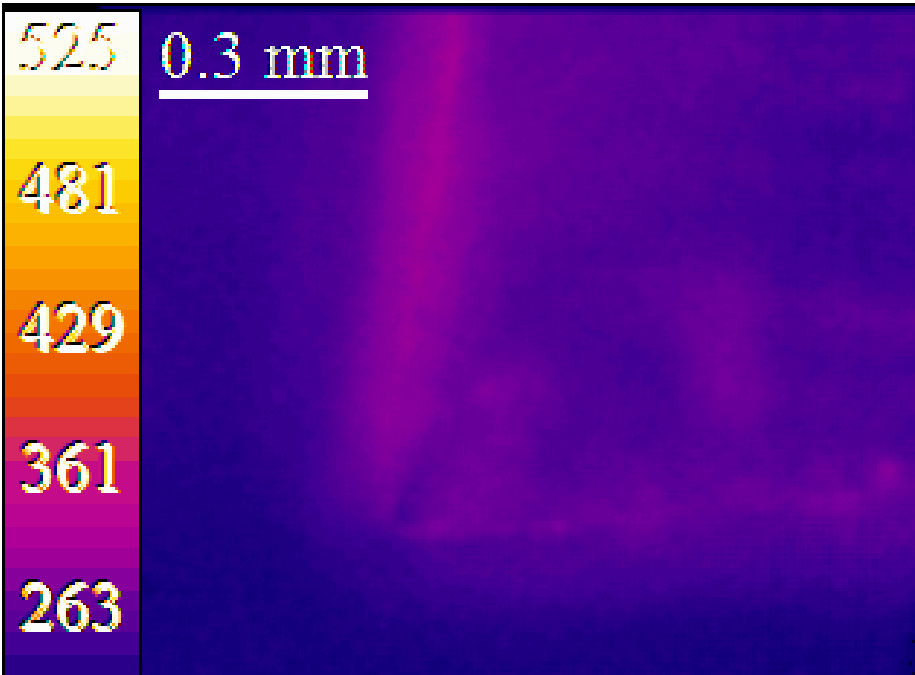


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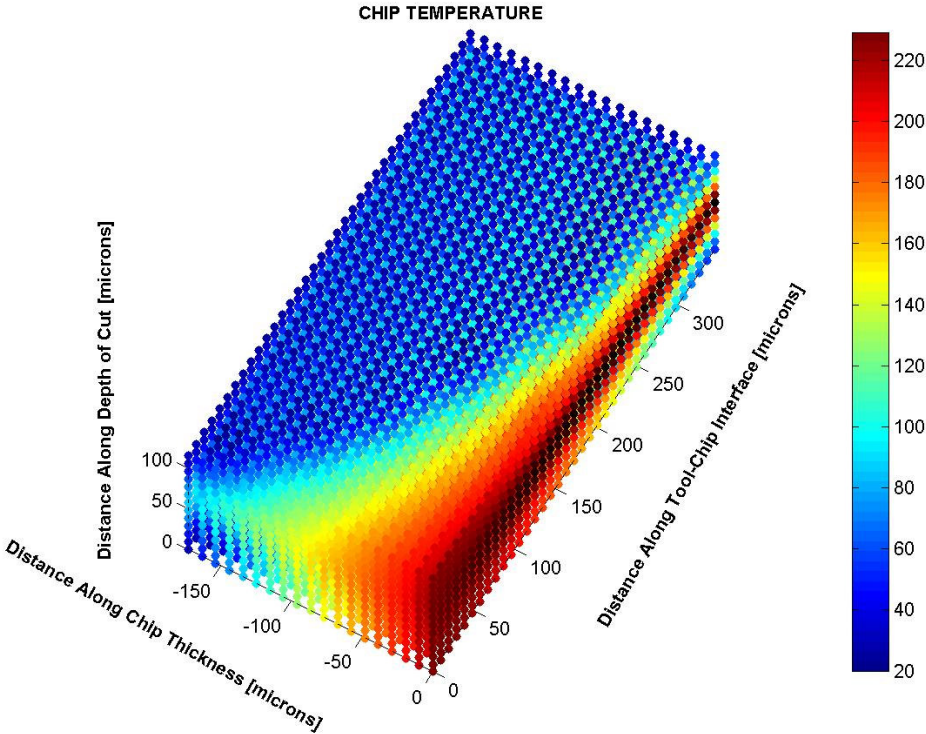


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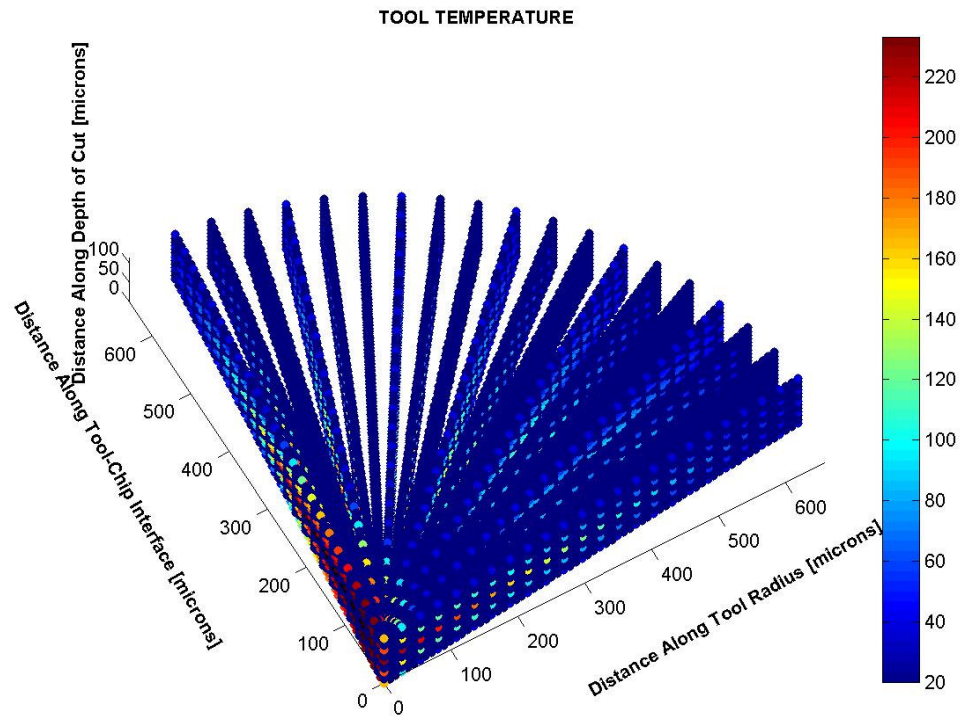
Fig. A.5: Validation for case 5. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



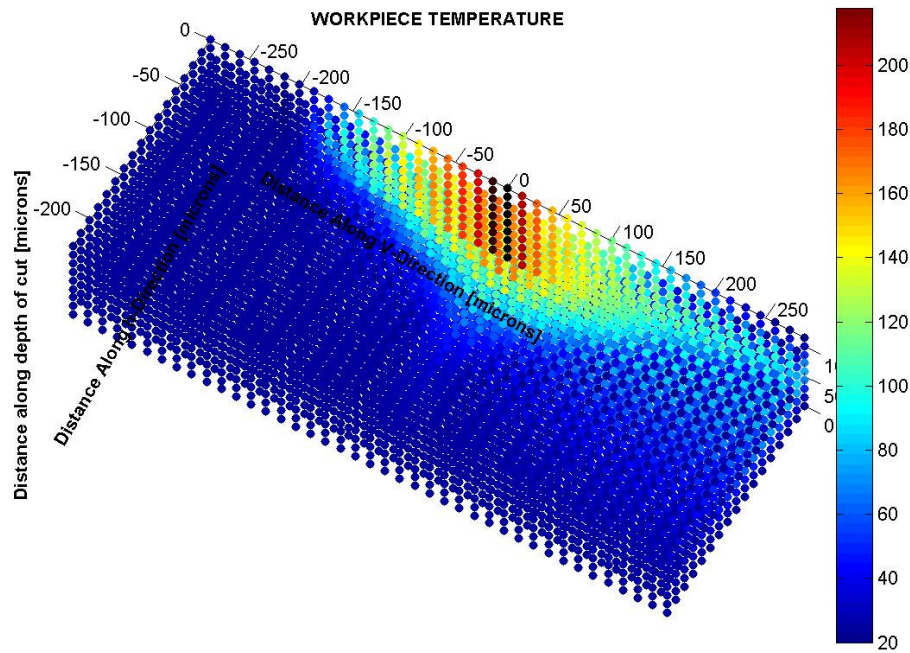
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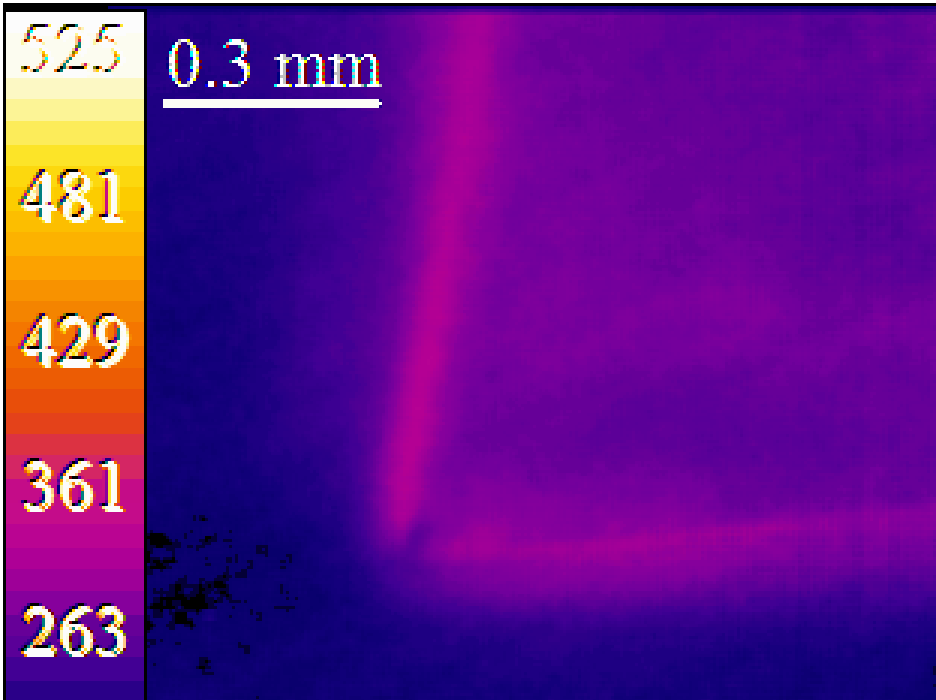


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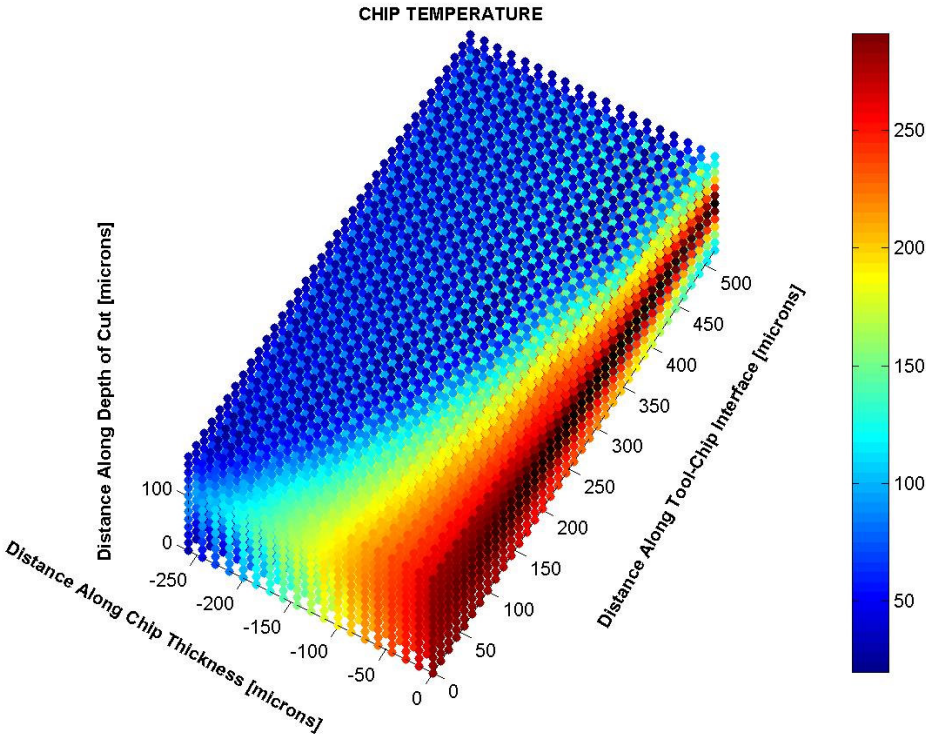


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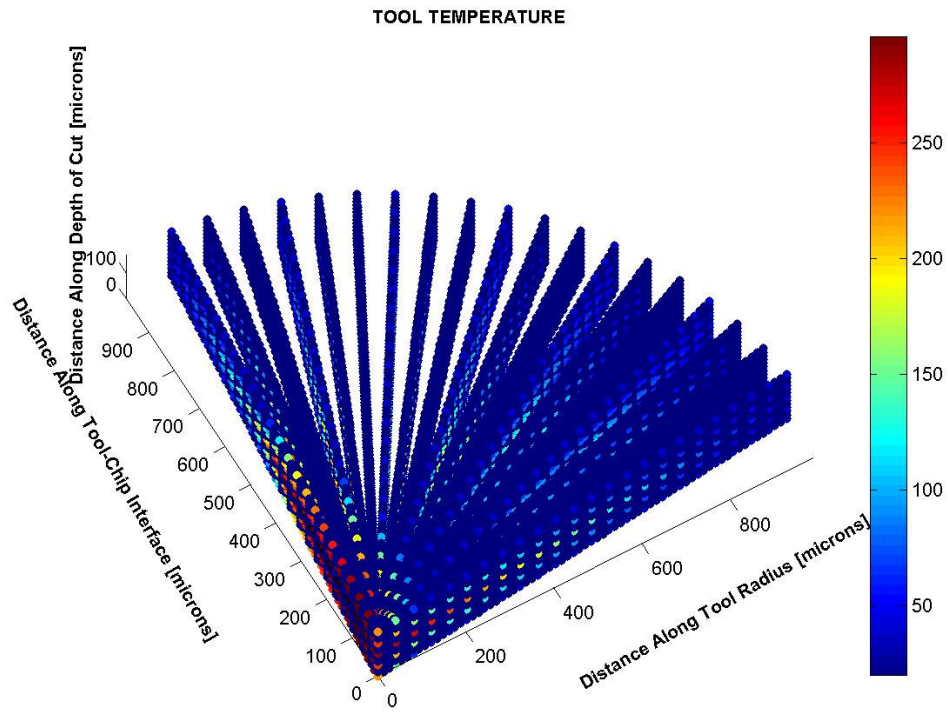
Fig. A.6: Validation for case 6. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



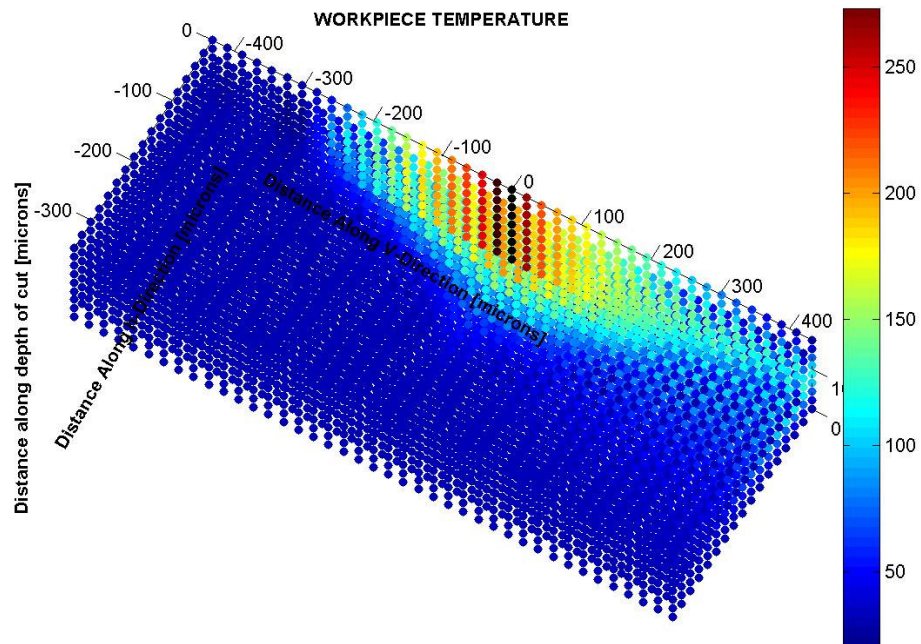
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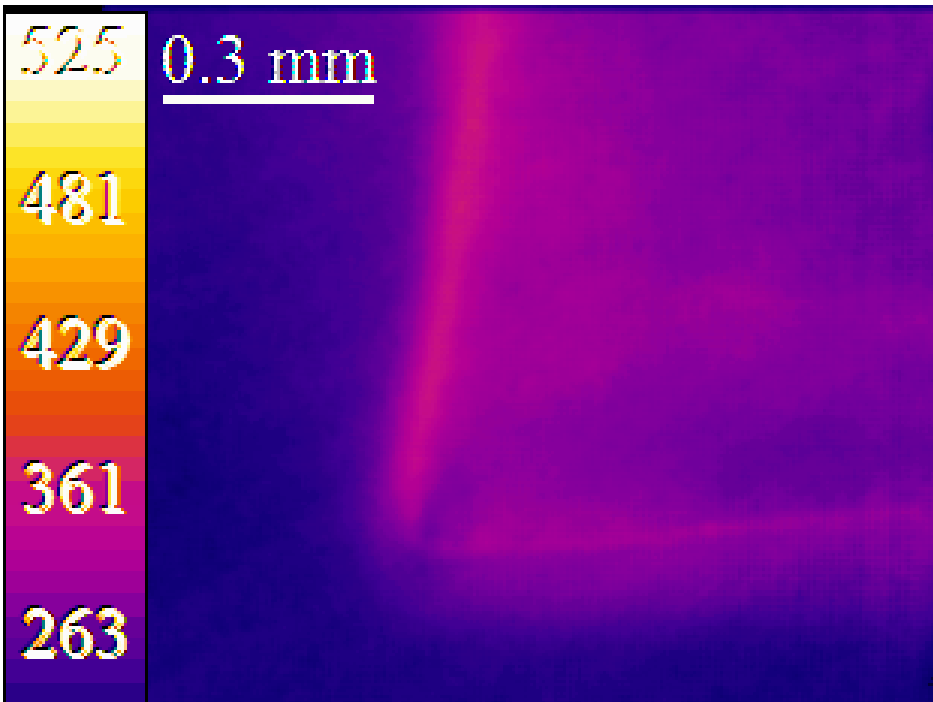


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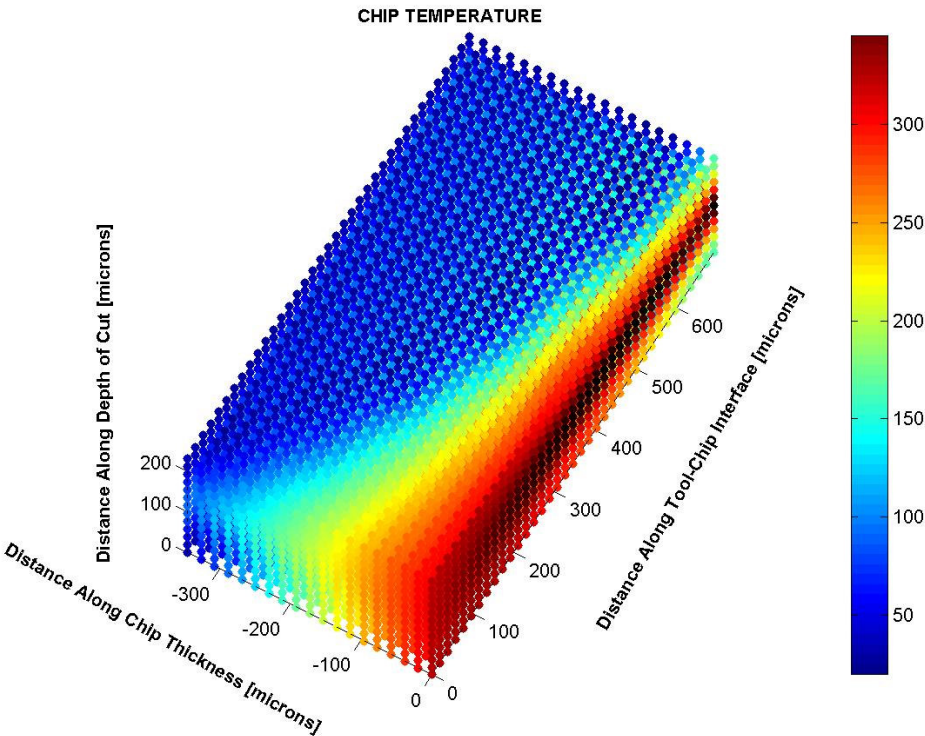


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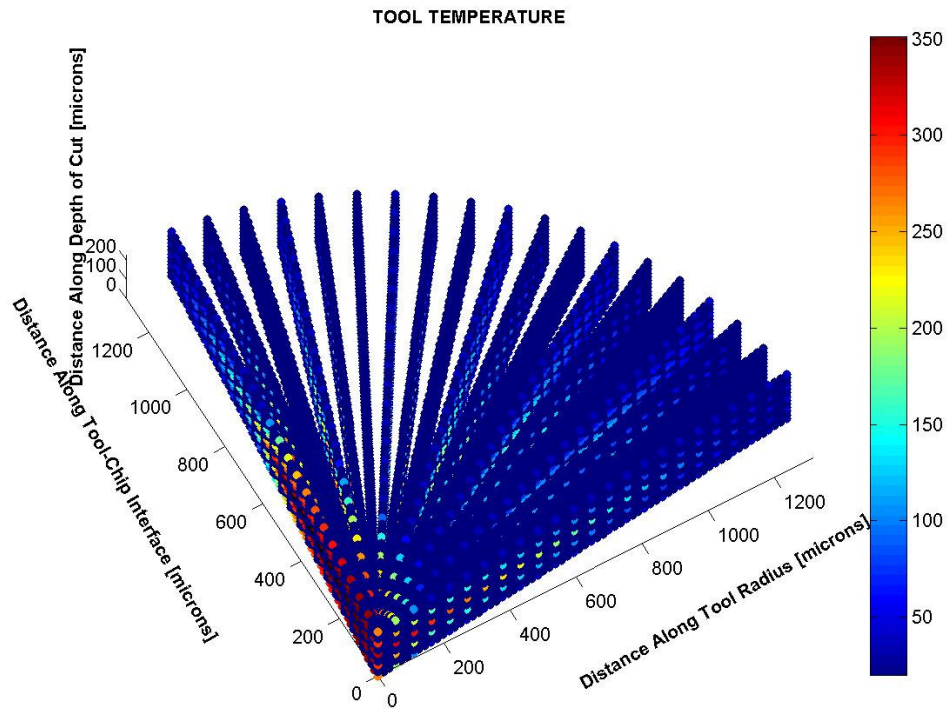
Fig. A.7: Validation for case 7. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



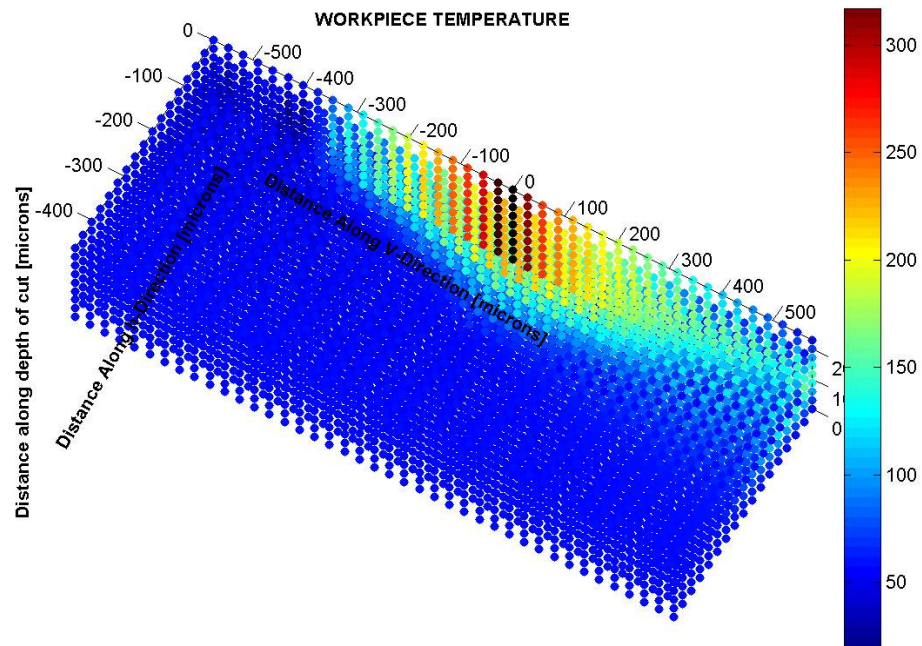
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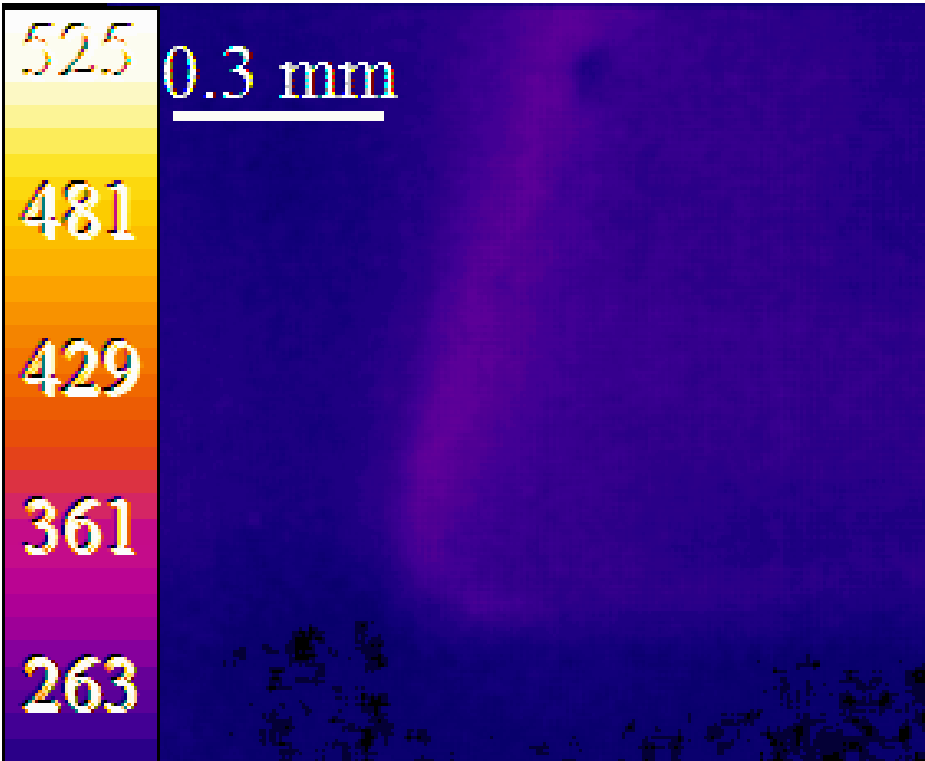


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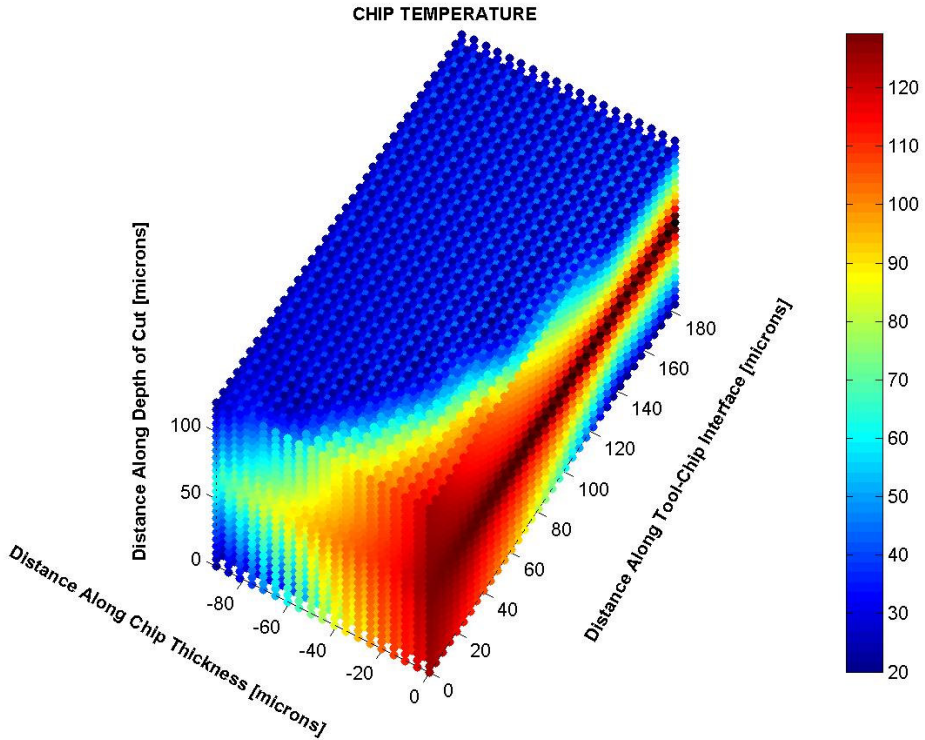


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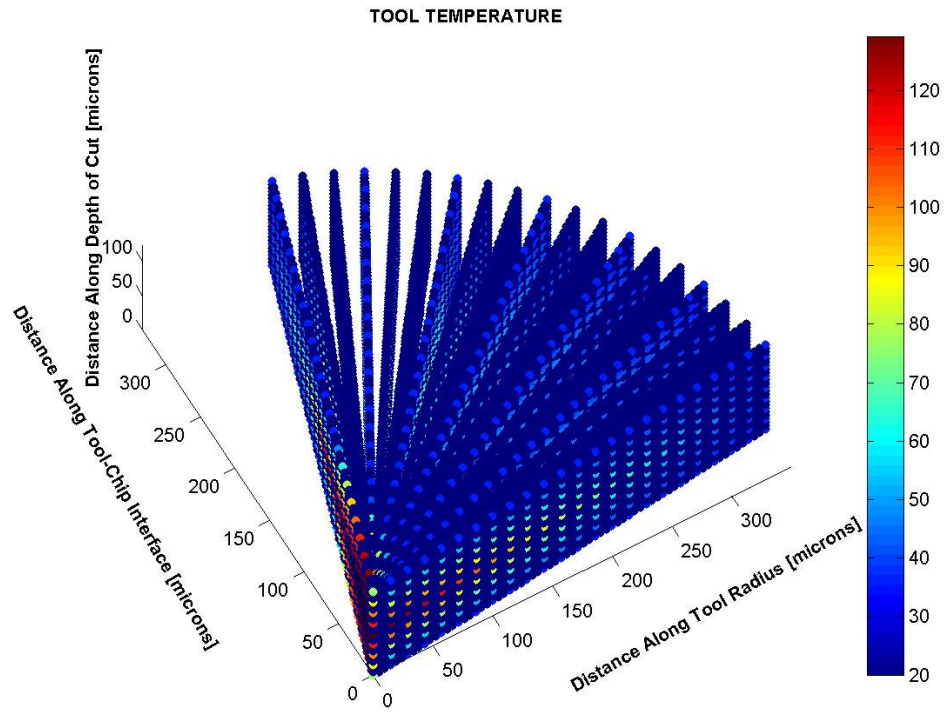
Fig. A.8: Validation for case 8. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



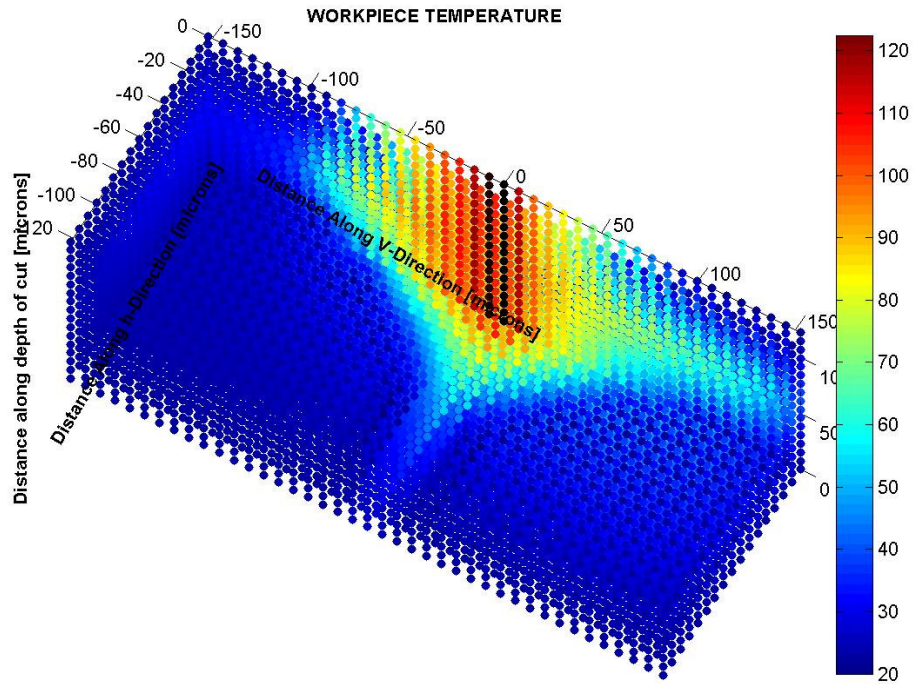
(a)



(b)

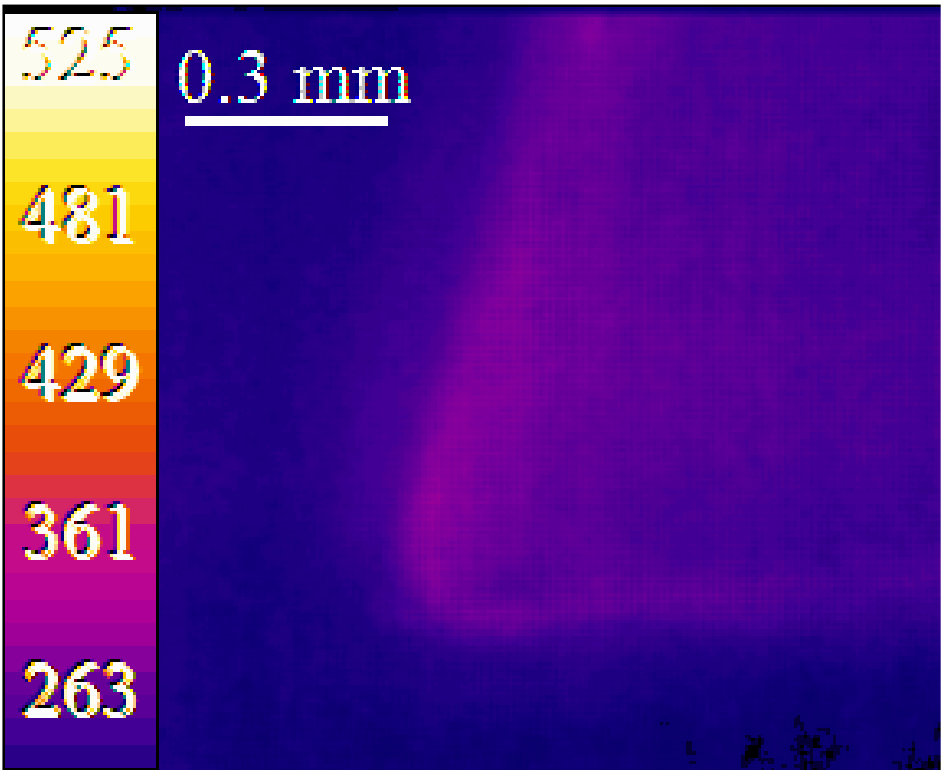


(c)

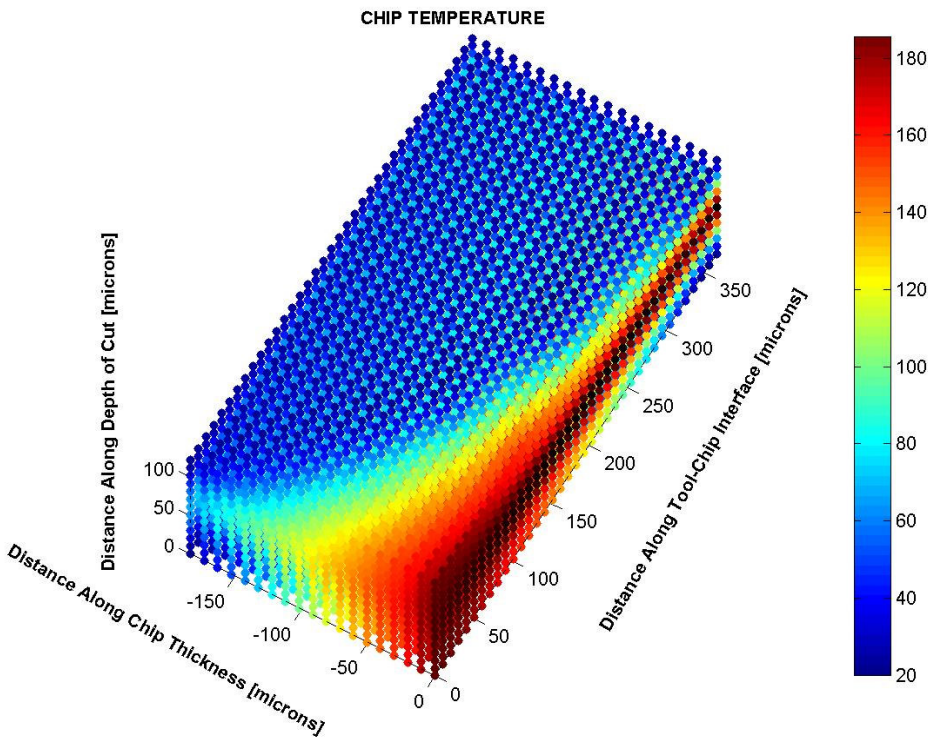


(d)

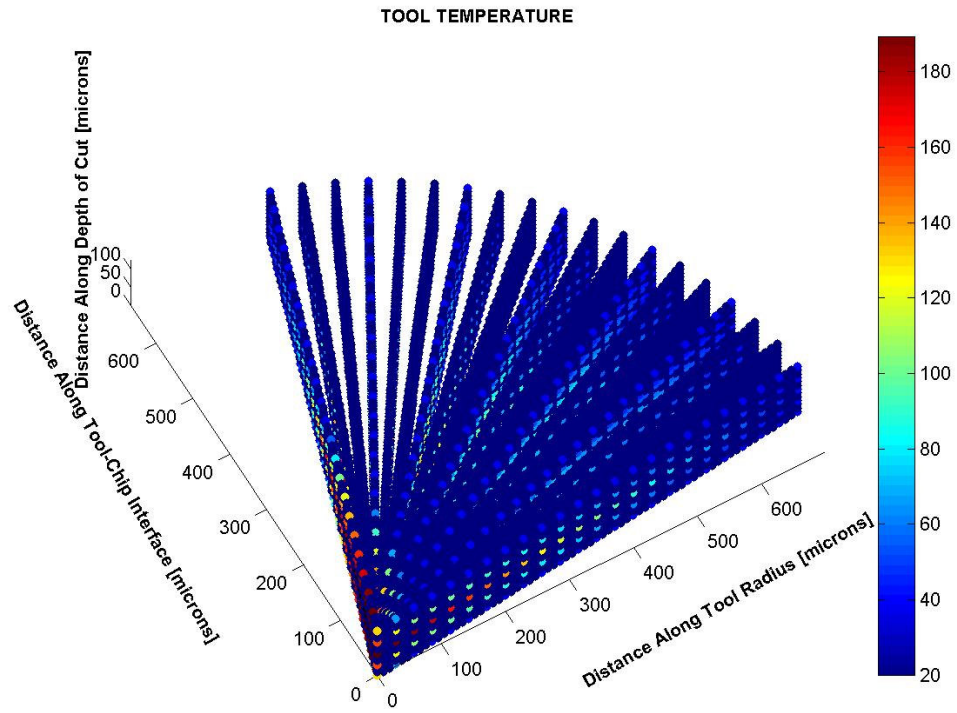
Fig. A.9: Validation for case 9. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



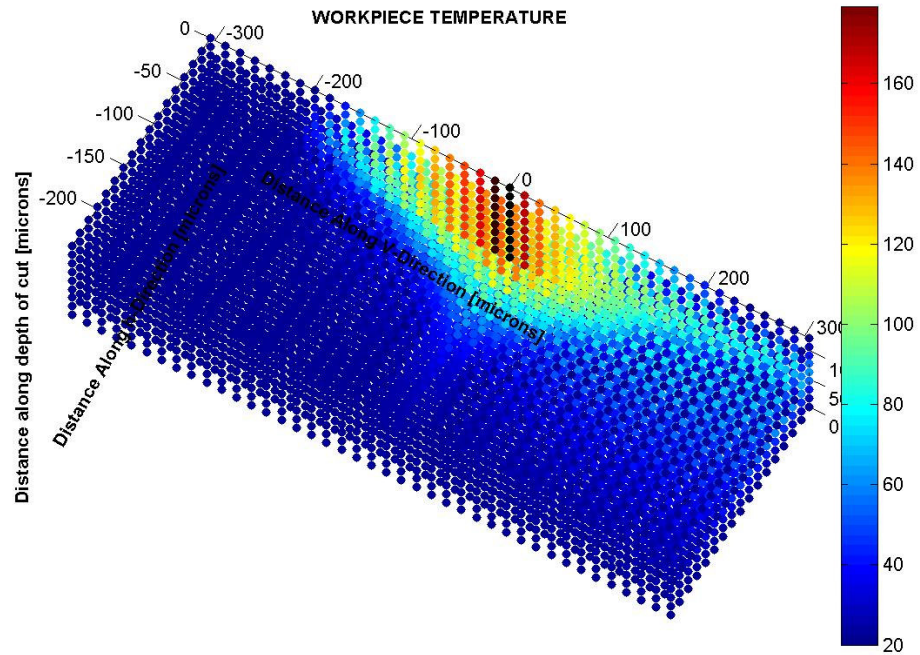
(a)



(b)

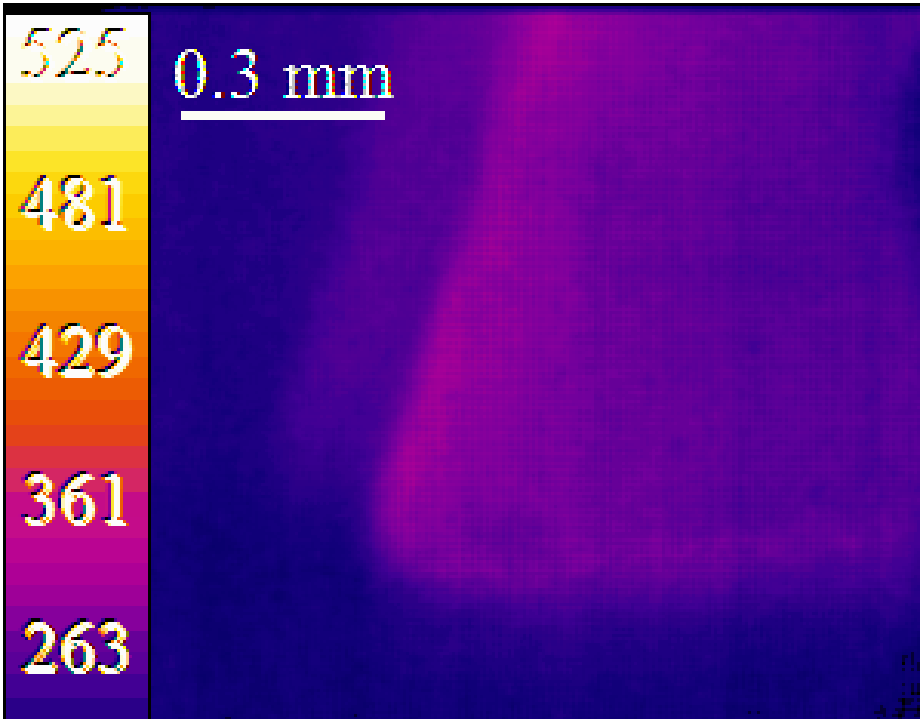


(c)

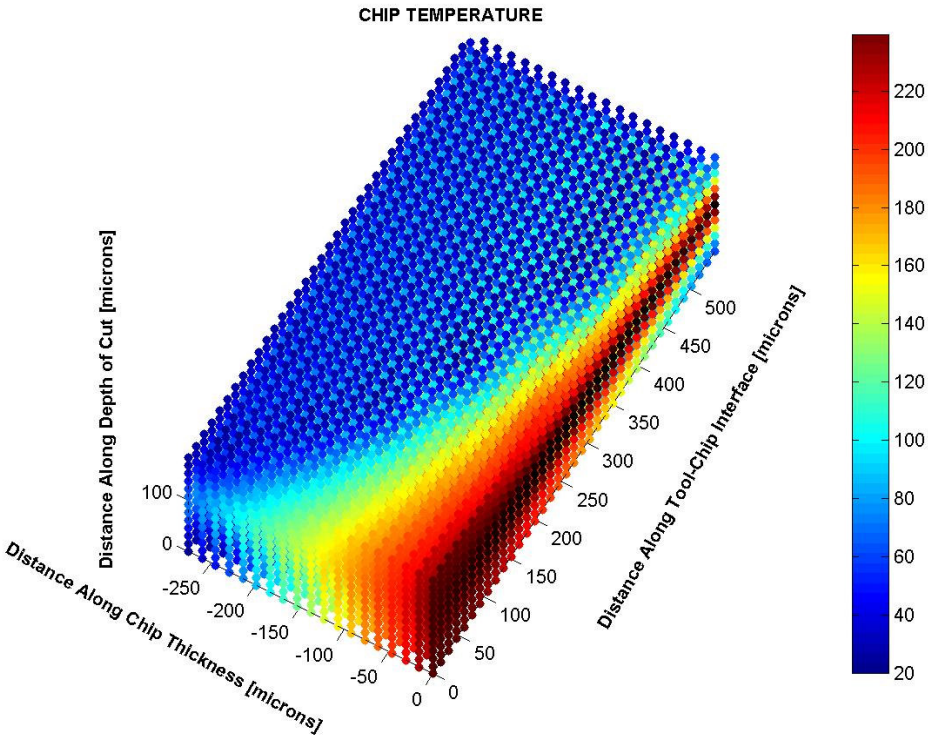


(d)

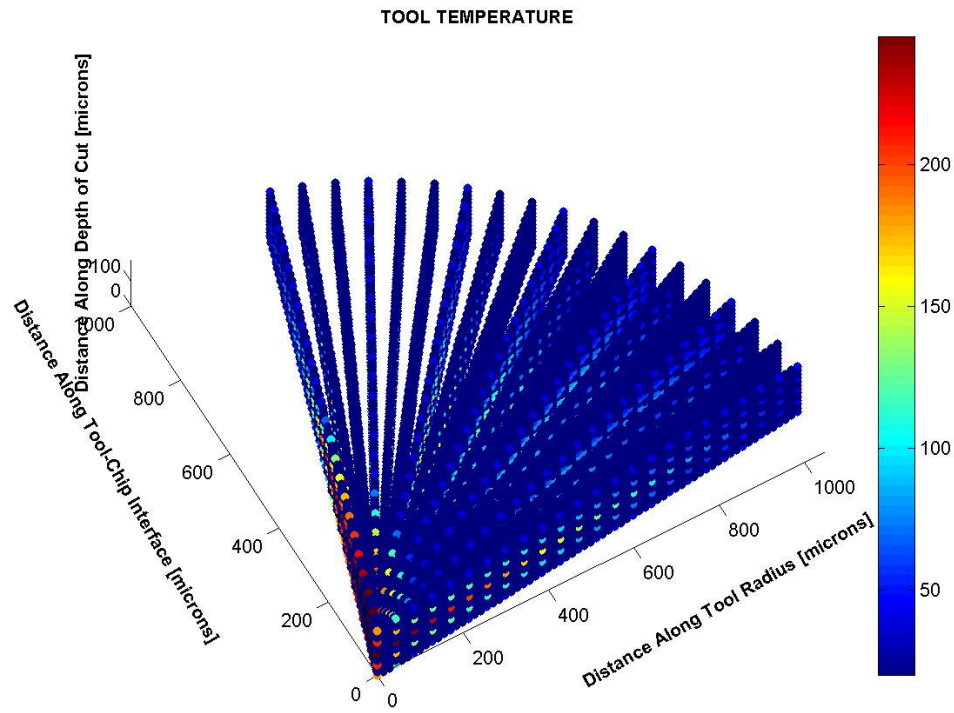
Fig. A.10: Validation for case 10. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



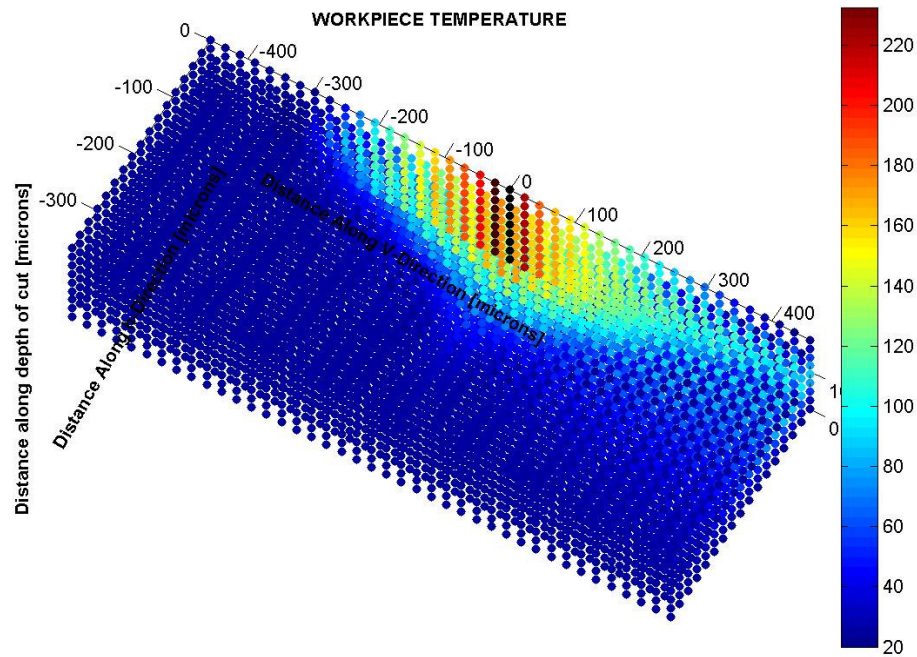
(a)



(b)

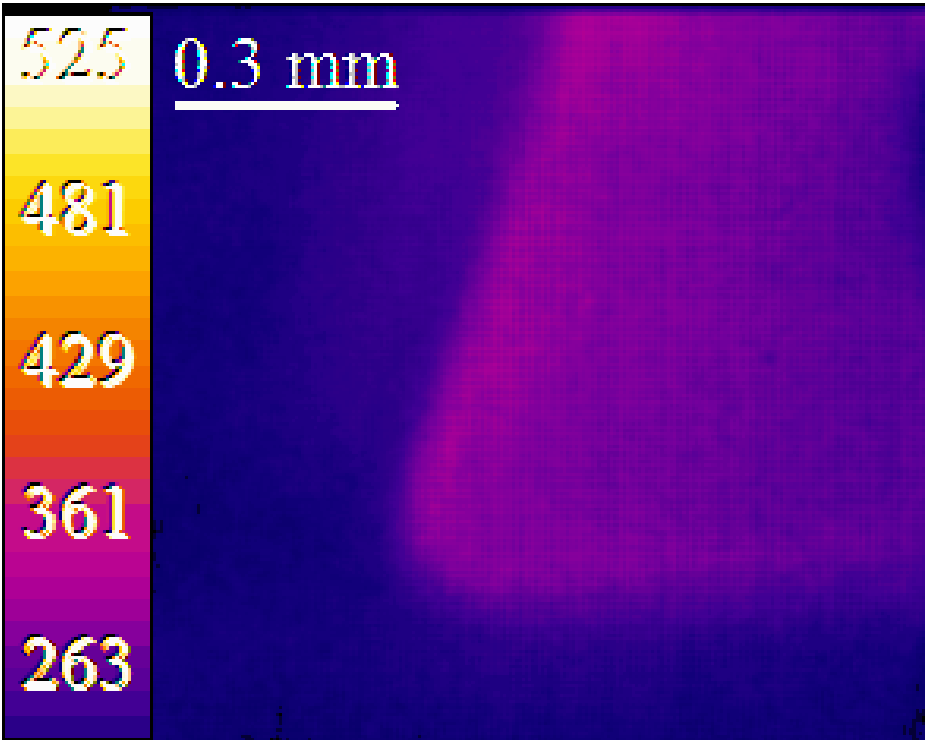


(c)

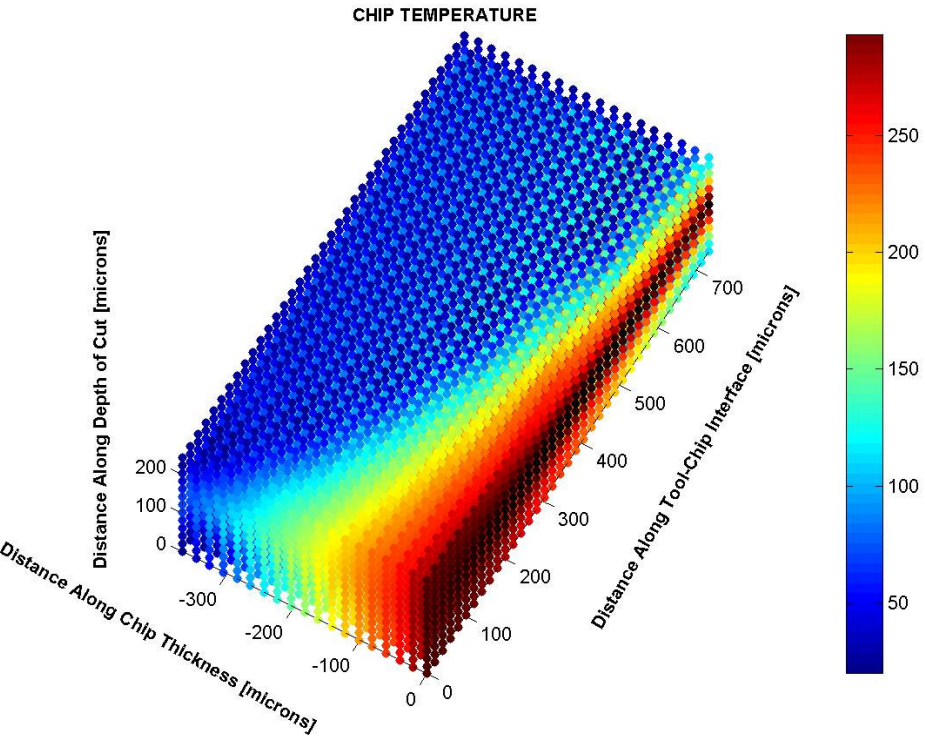


(d)

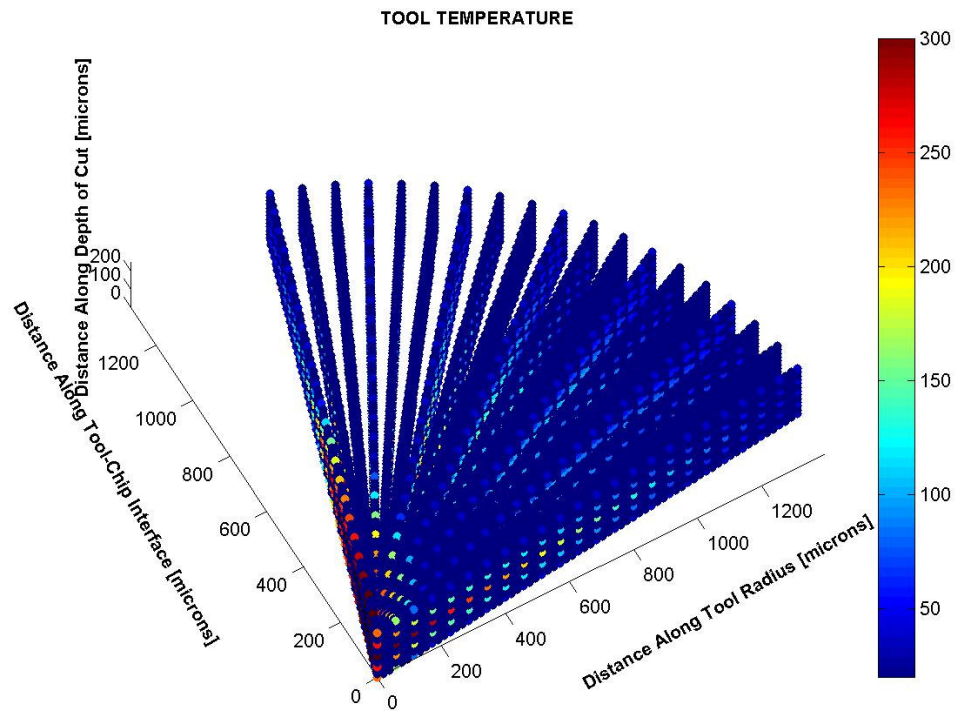
Fig. A.11: Validation for case 11. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



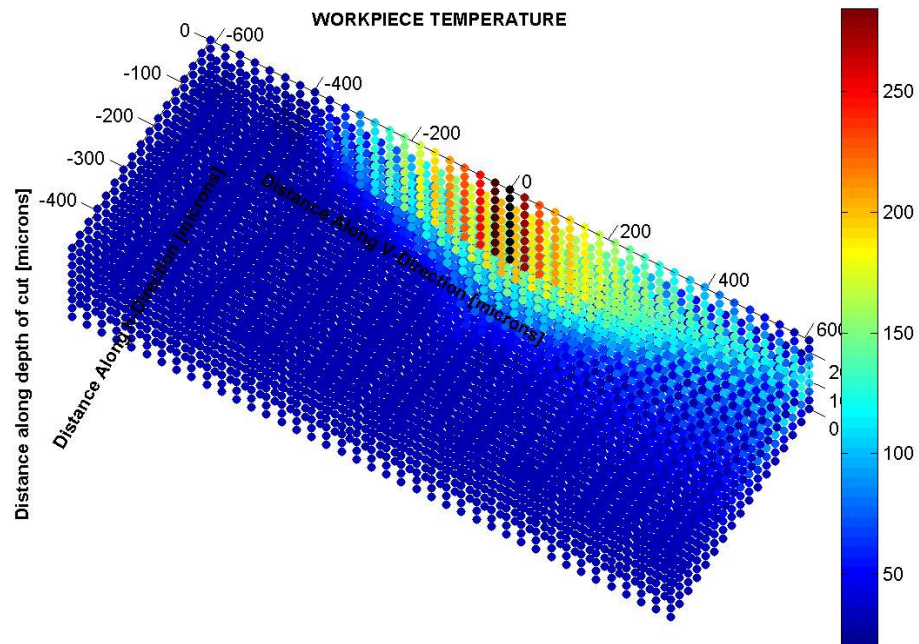
(a)



(b)

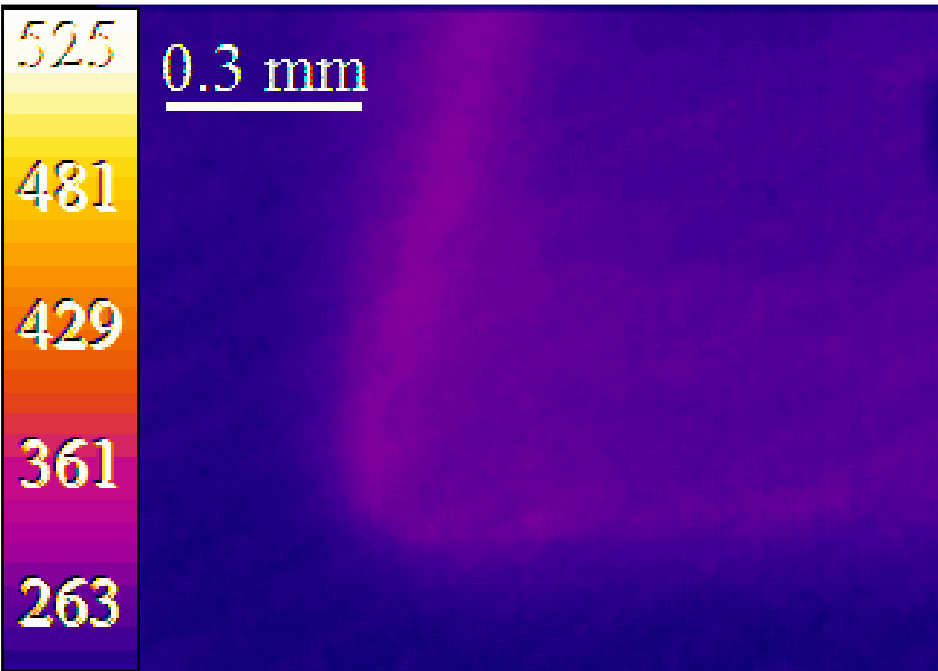


(c)

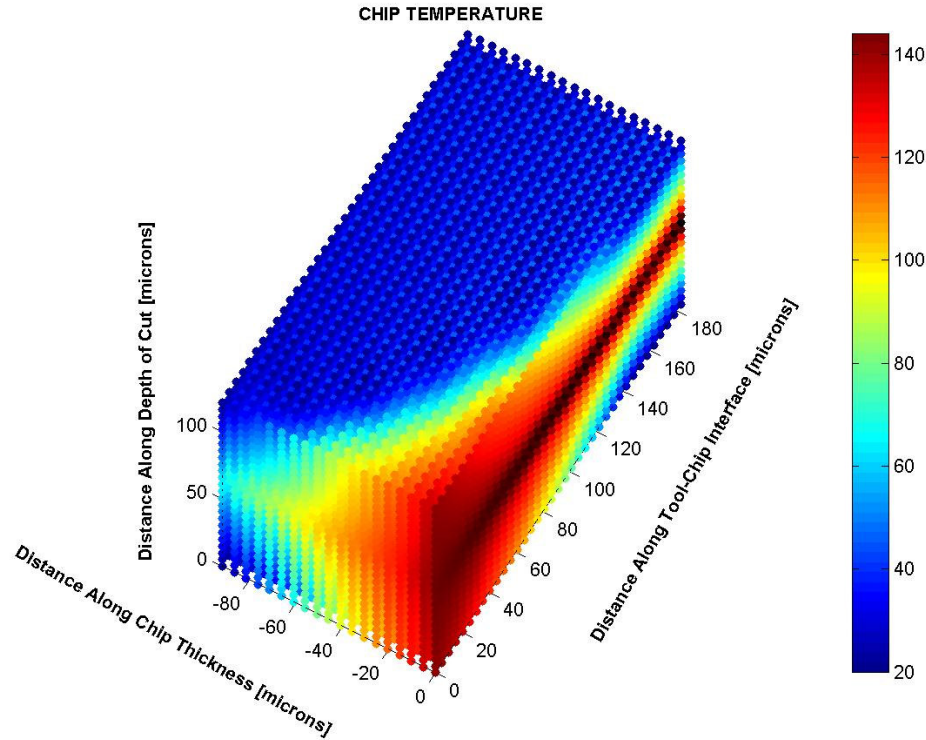


(d)

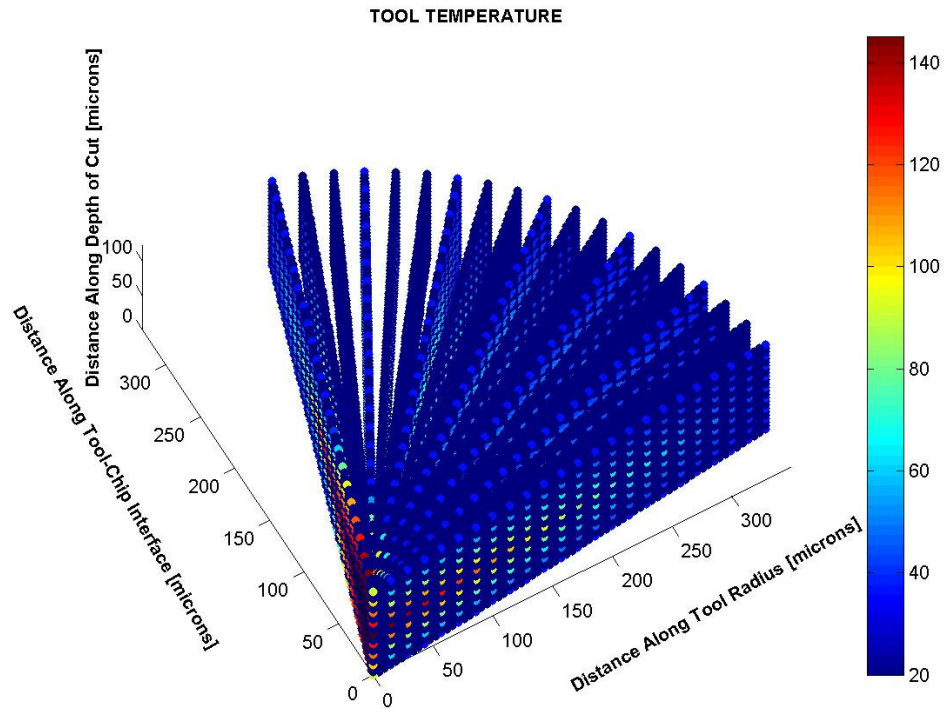
Fig. A.12: Validation for case 12. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



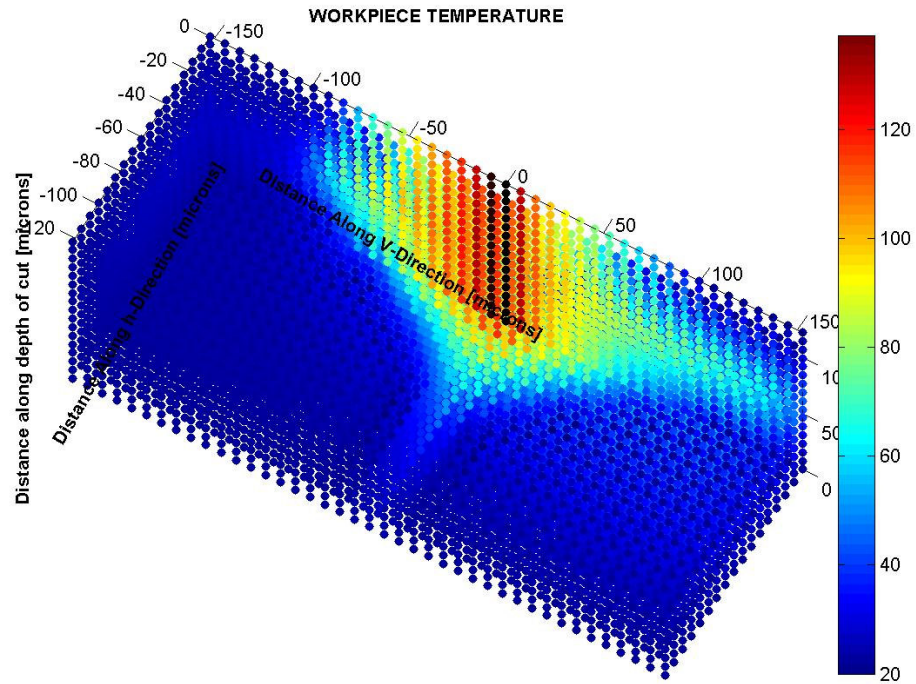
(a)



(b)

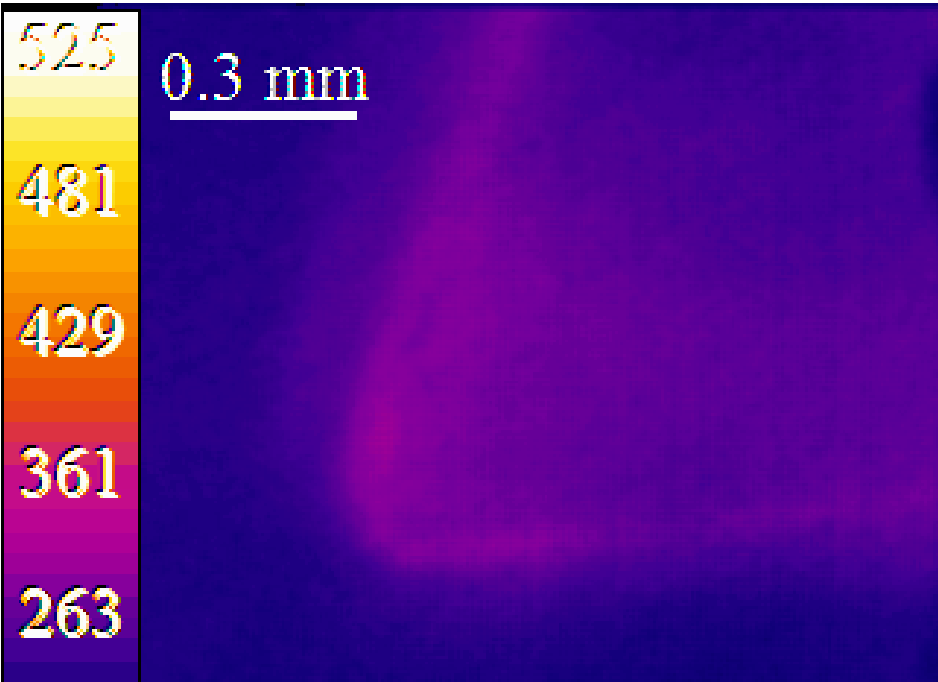


(c)

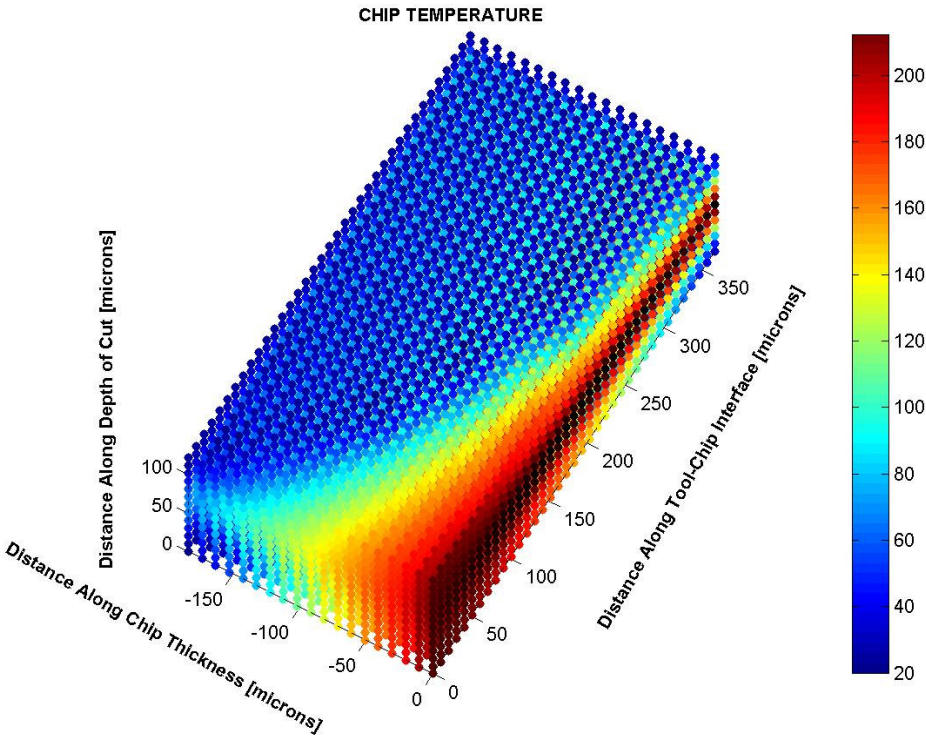


(d)

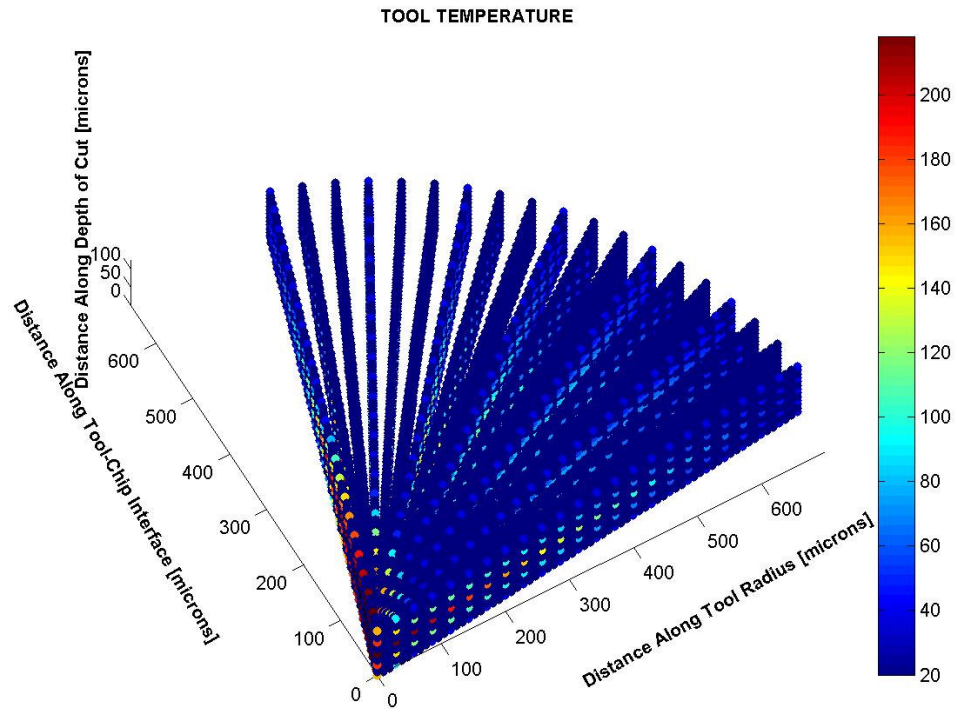
Fig. A.13: Validation for case 13. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



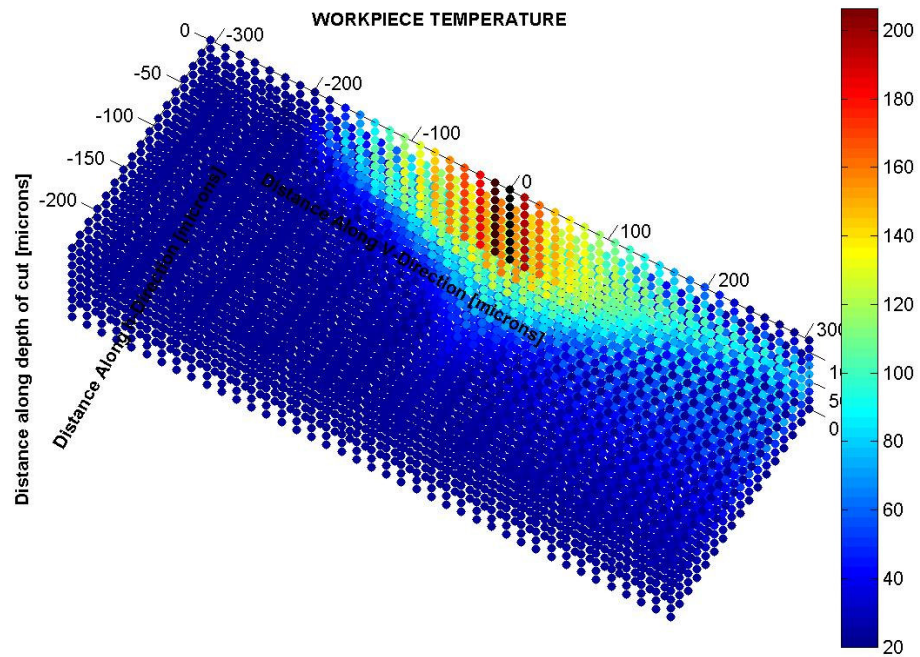
(a)



(b)

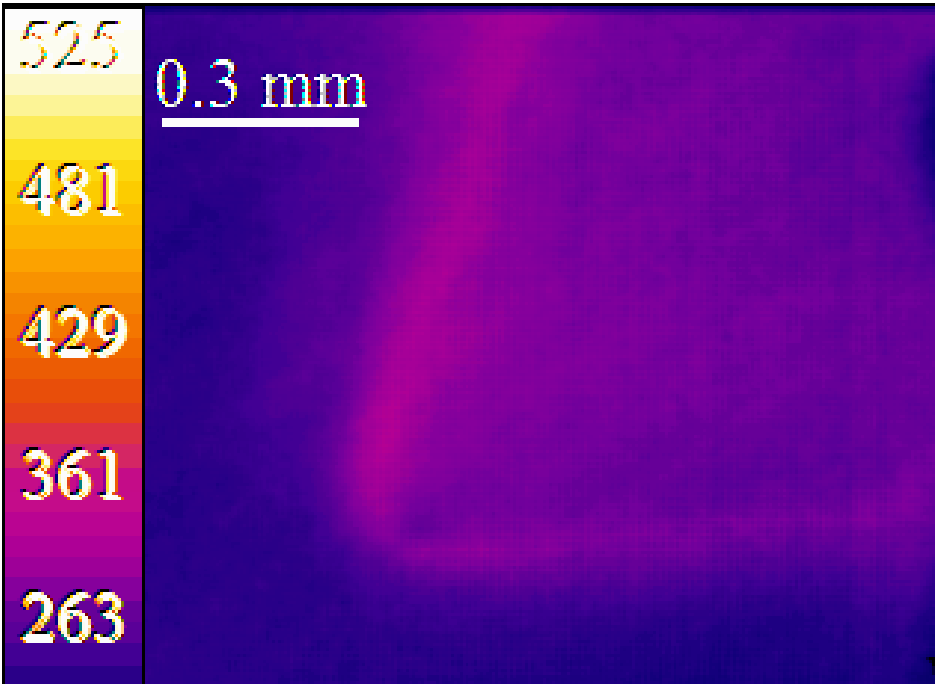


(c)

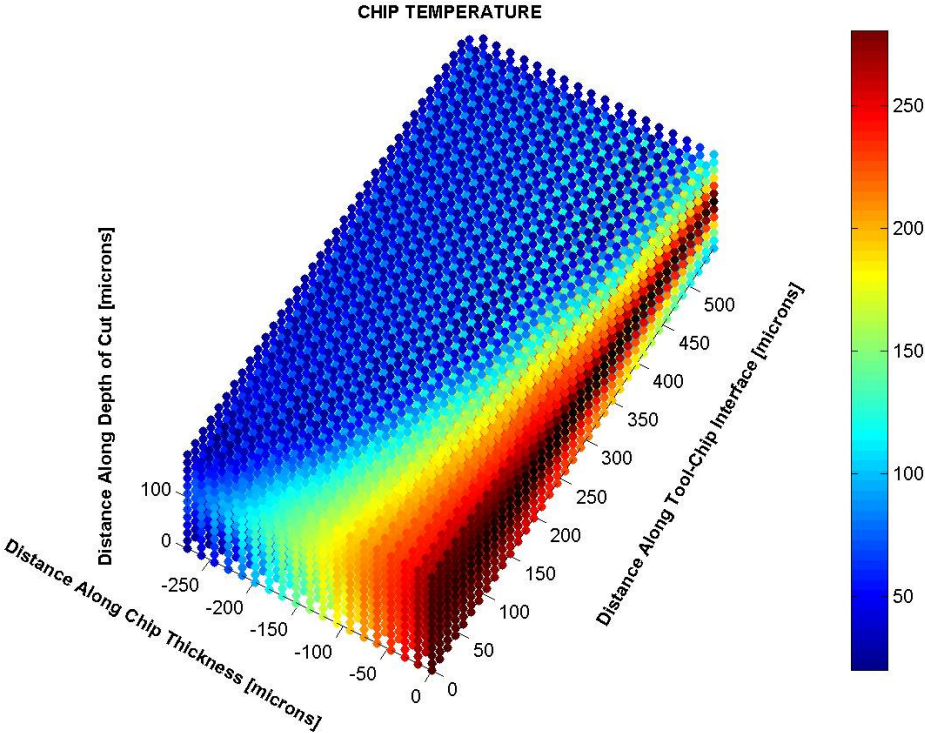


(d)

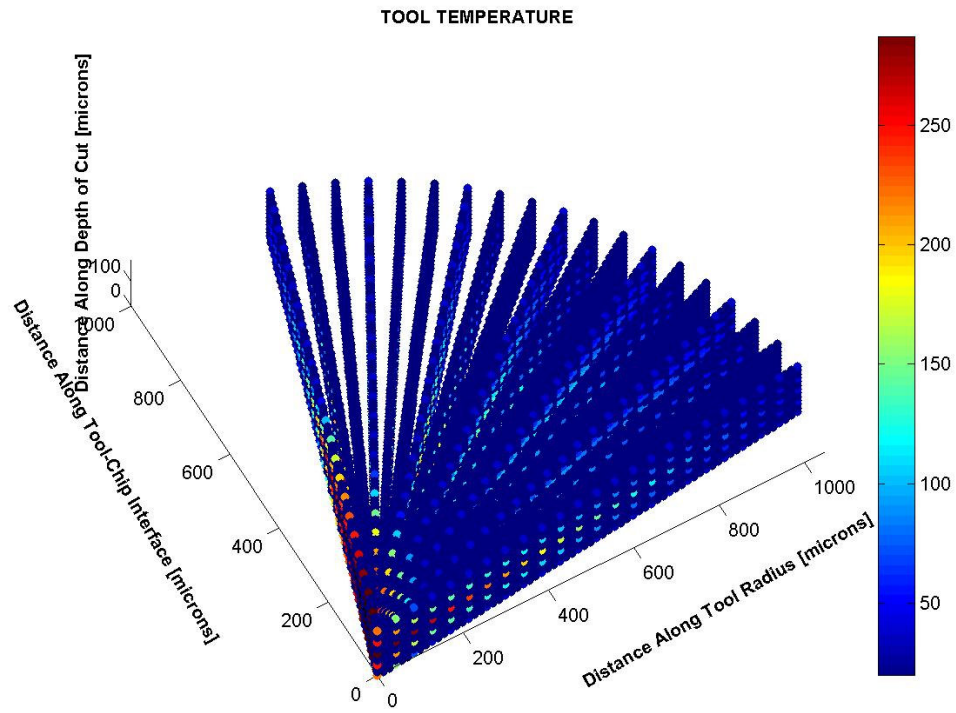
Fig. A.14: Validation for case 14. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



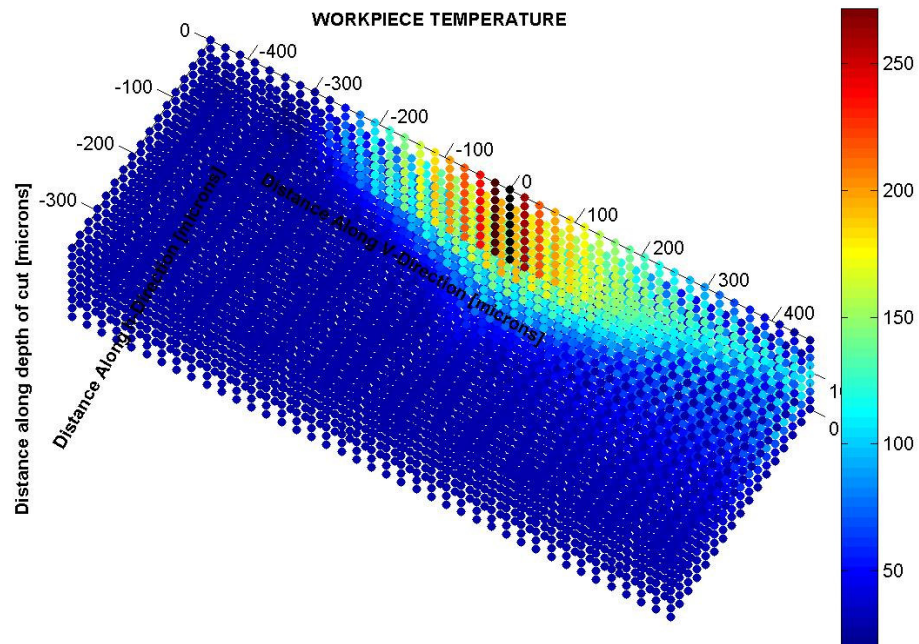
(a)



(b)

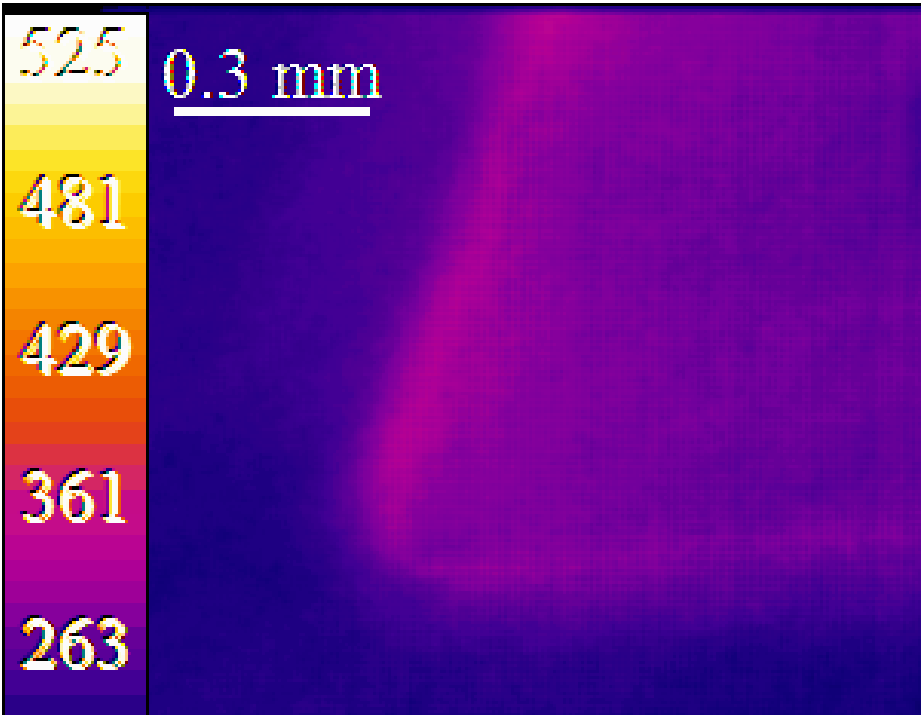


(c)

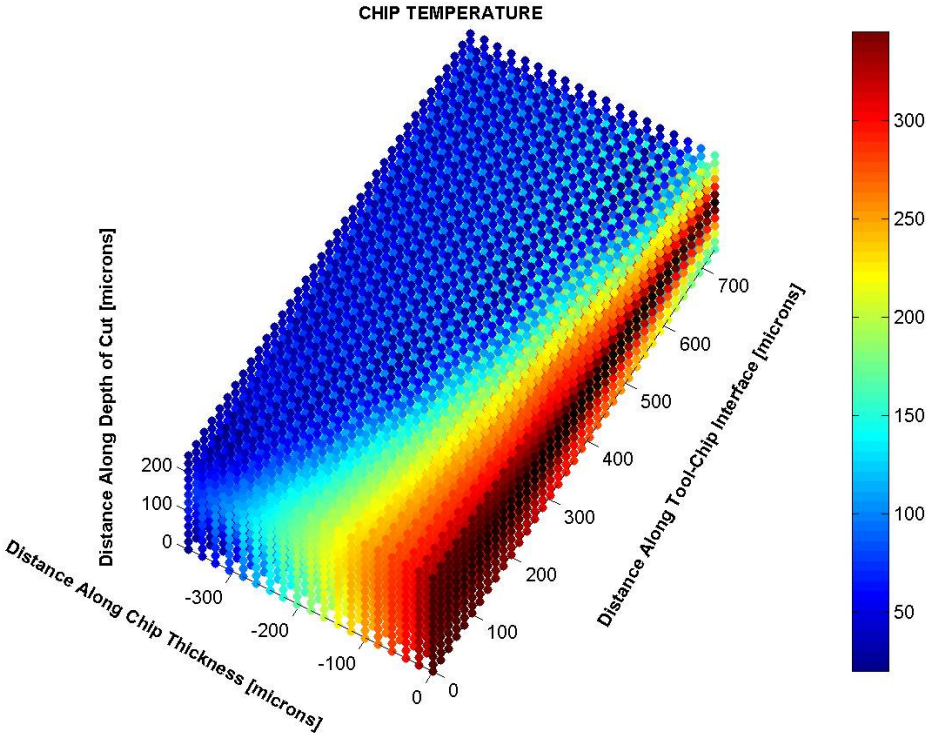


(d)

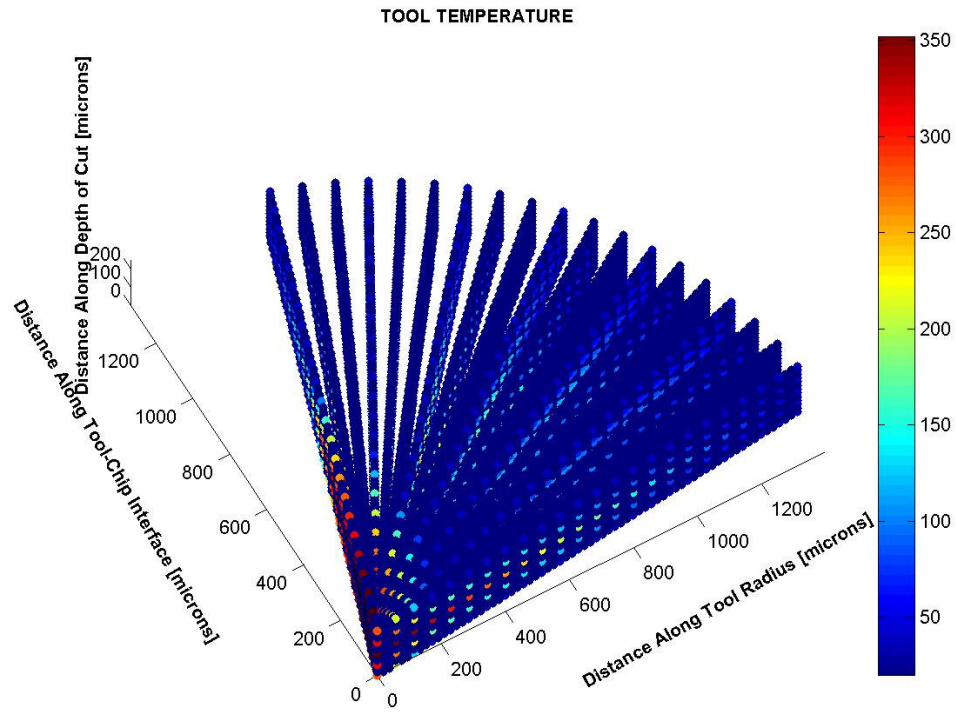
Fig. A.15: Validation for case 15. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



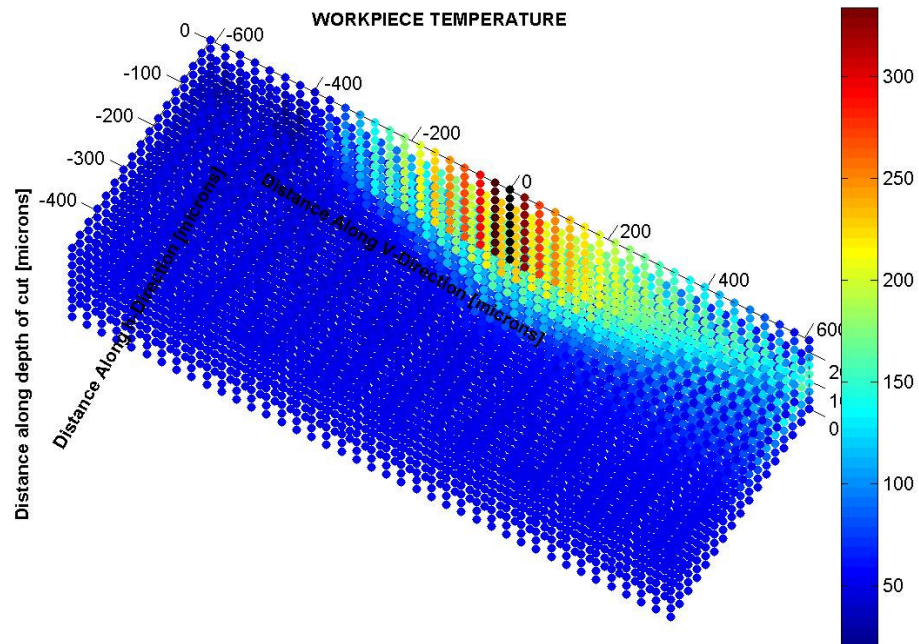
(a)



(b)

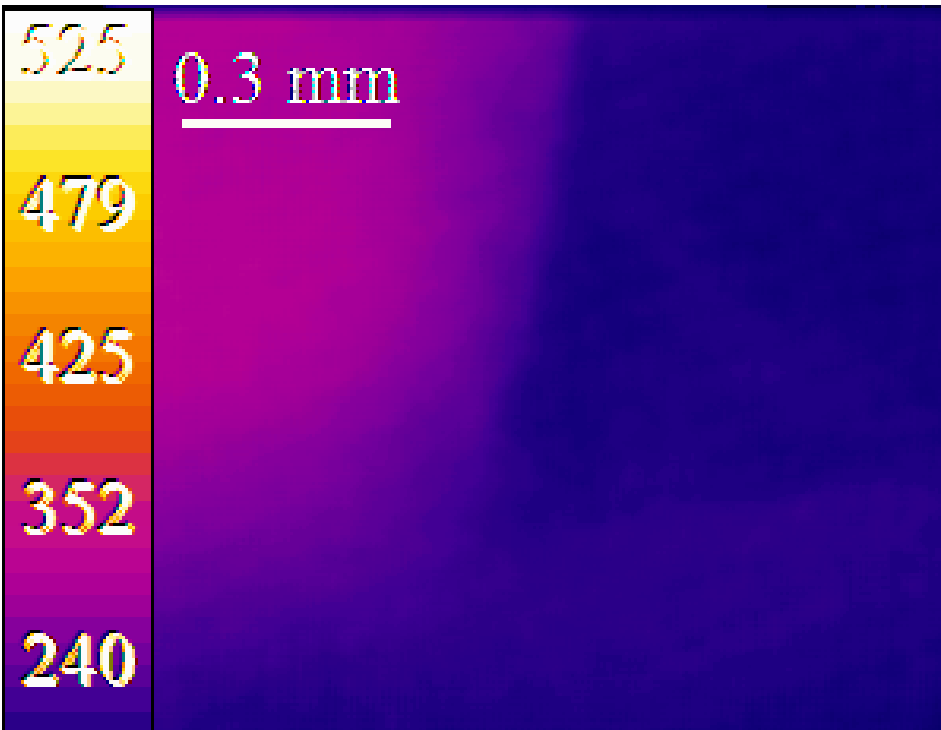


(c)

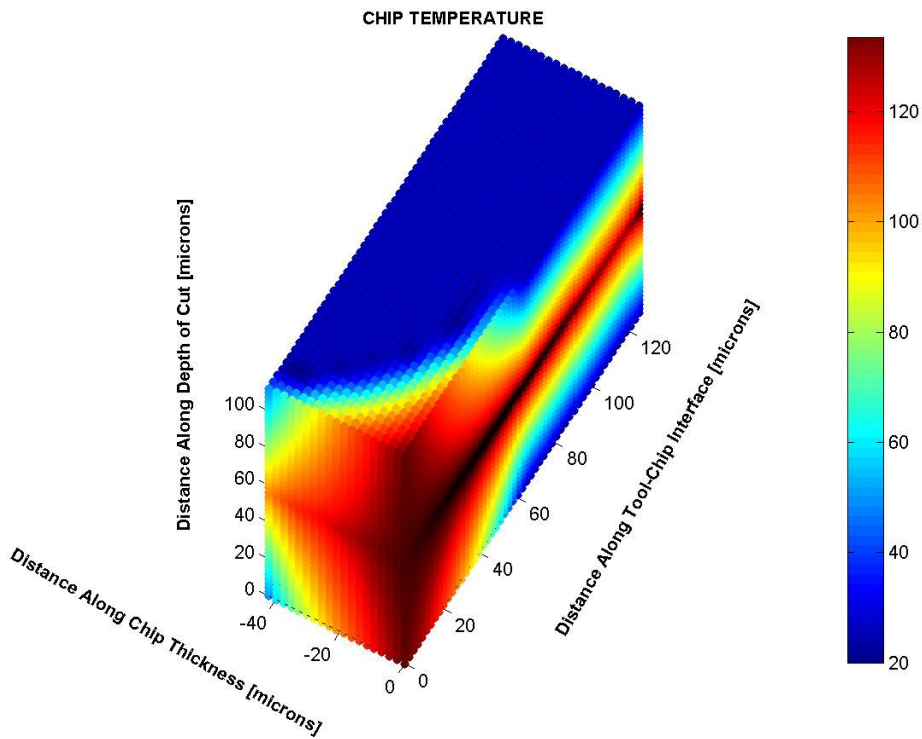


(d)

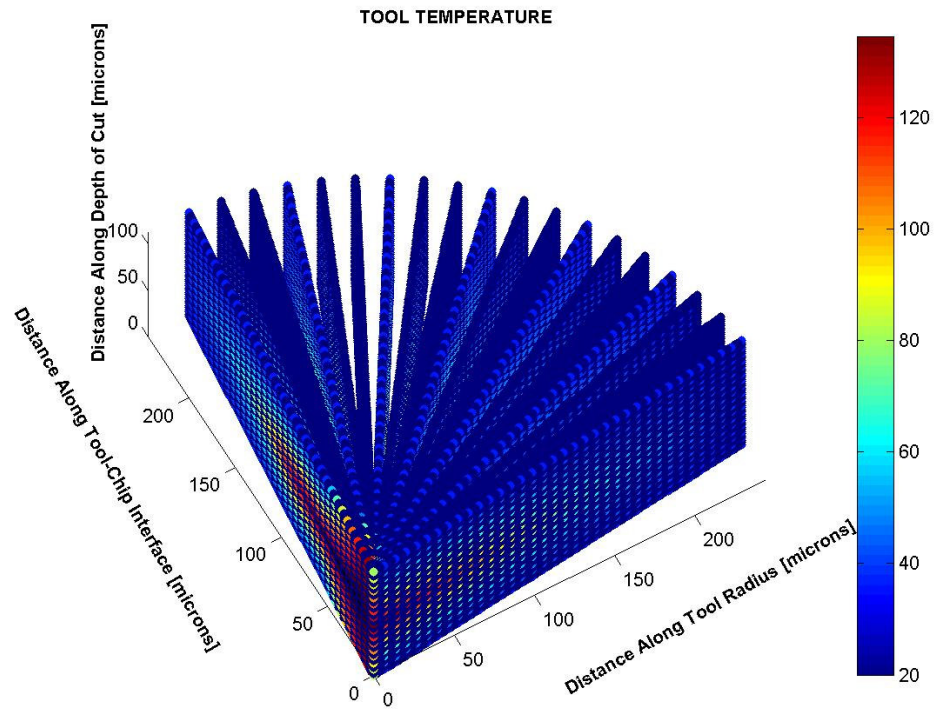
Fig. A.16: Validation for case 16. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



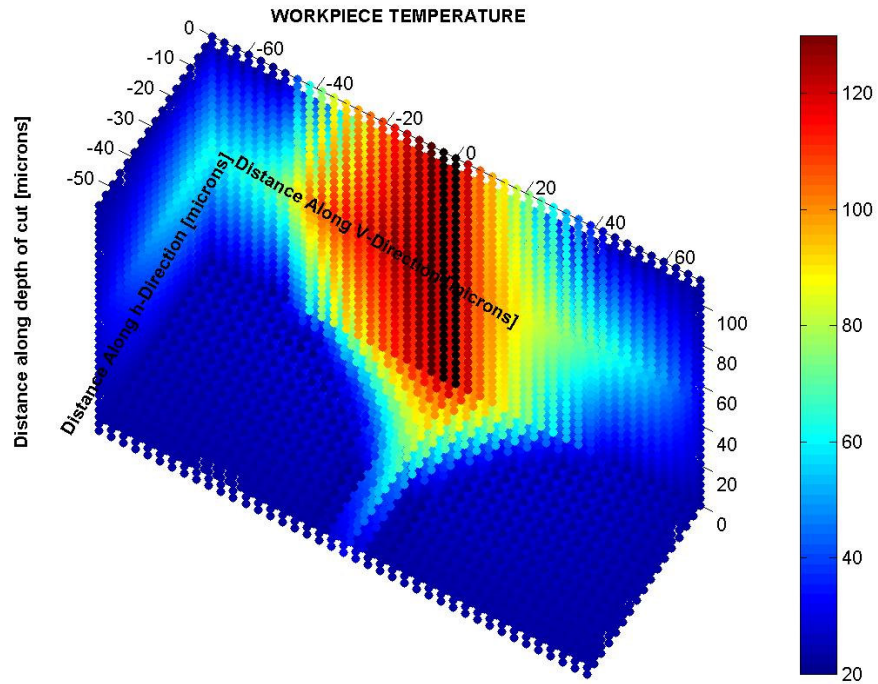
(a)



(b)

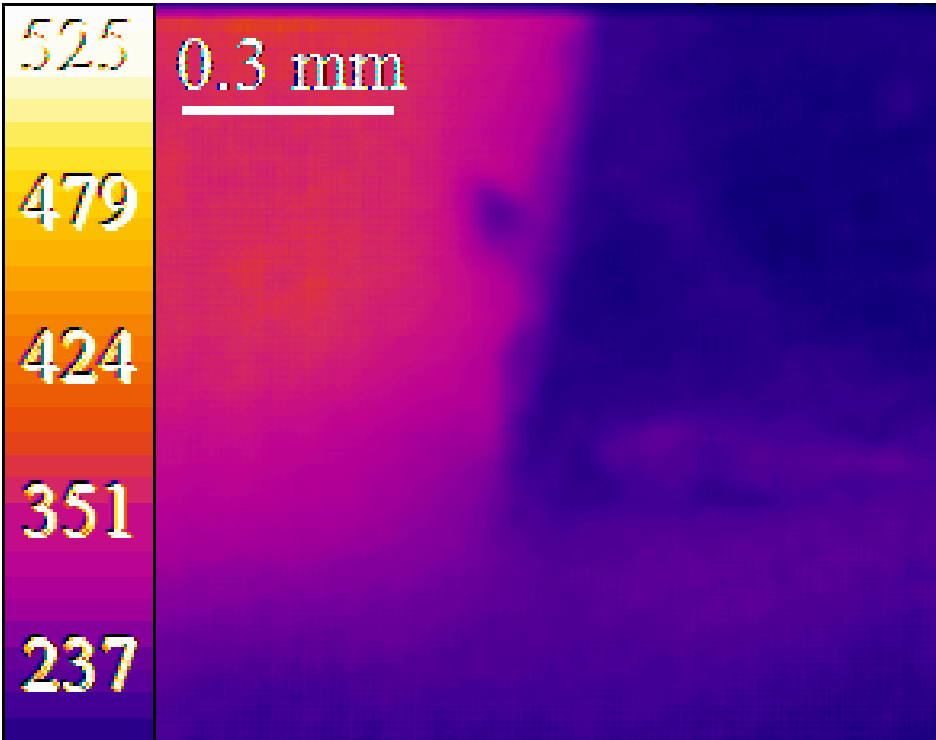


(c)

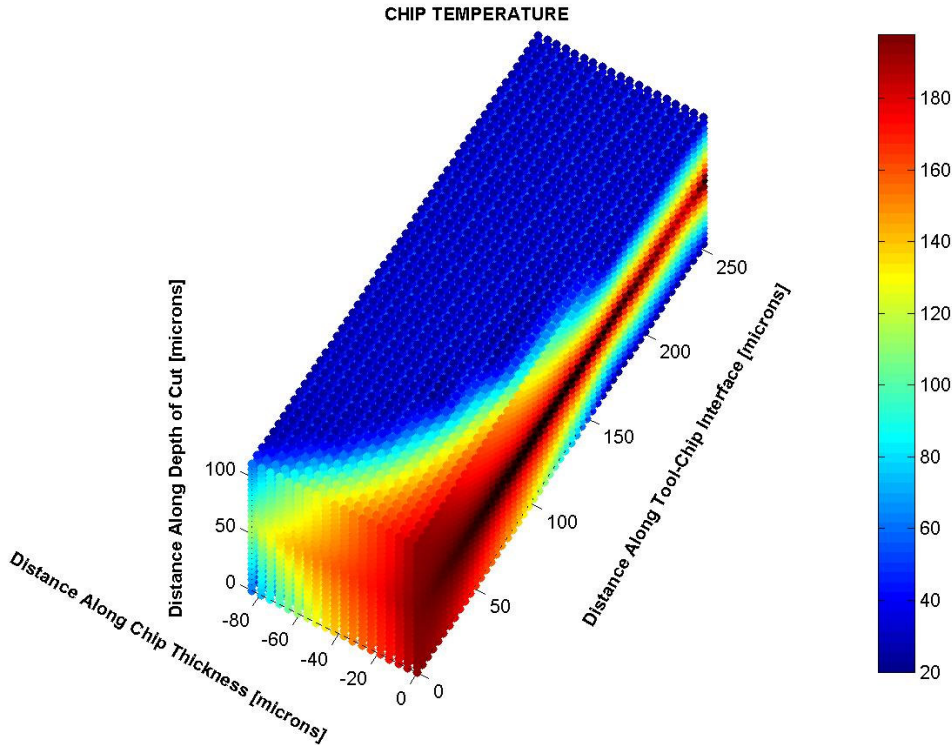


(d)

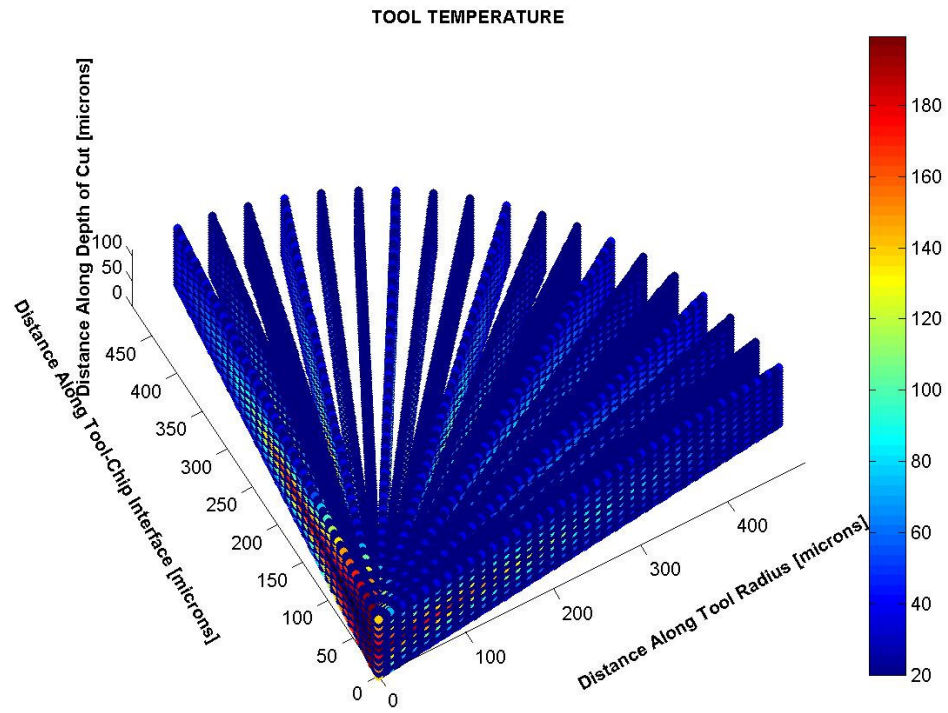
Fig. A.17: Validation for case 17. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



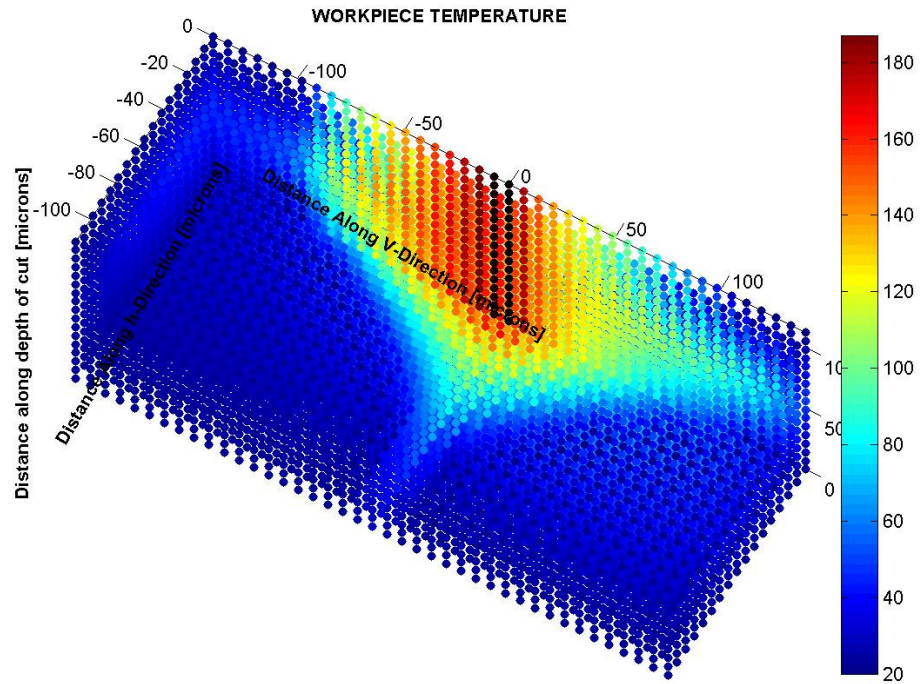
(a)



(b)

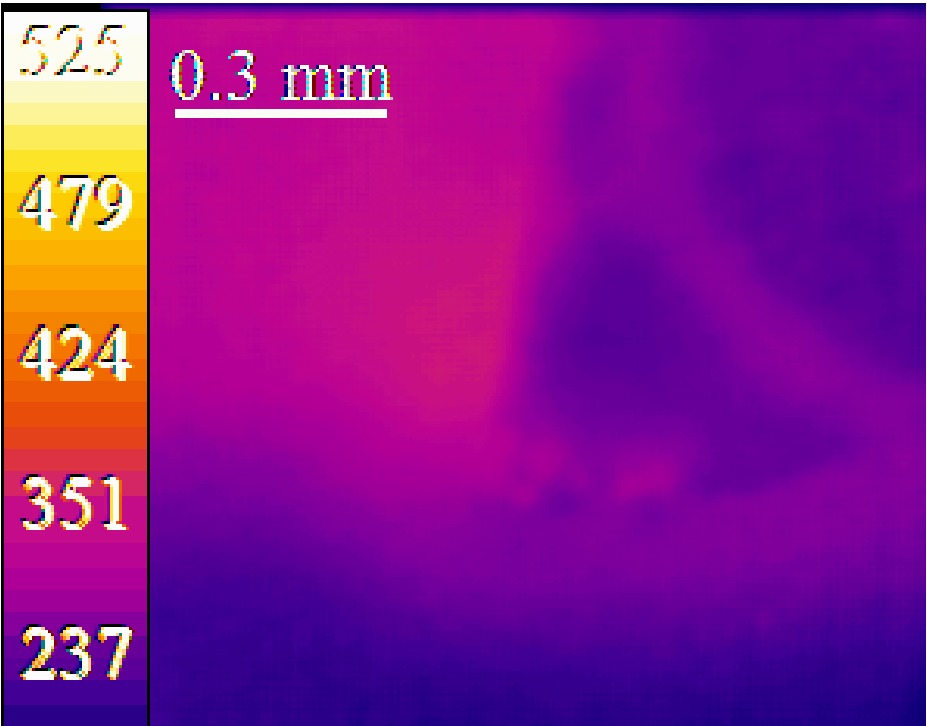


(c)

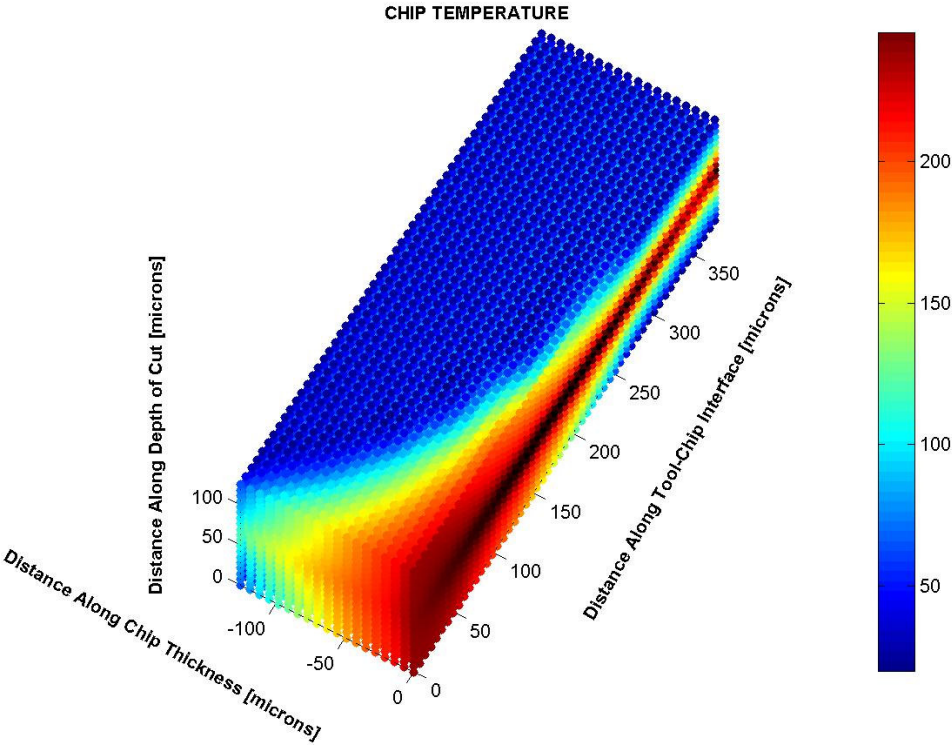


(d)

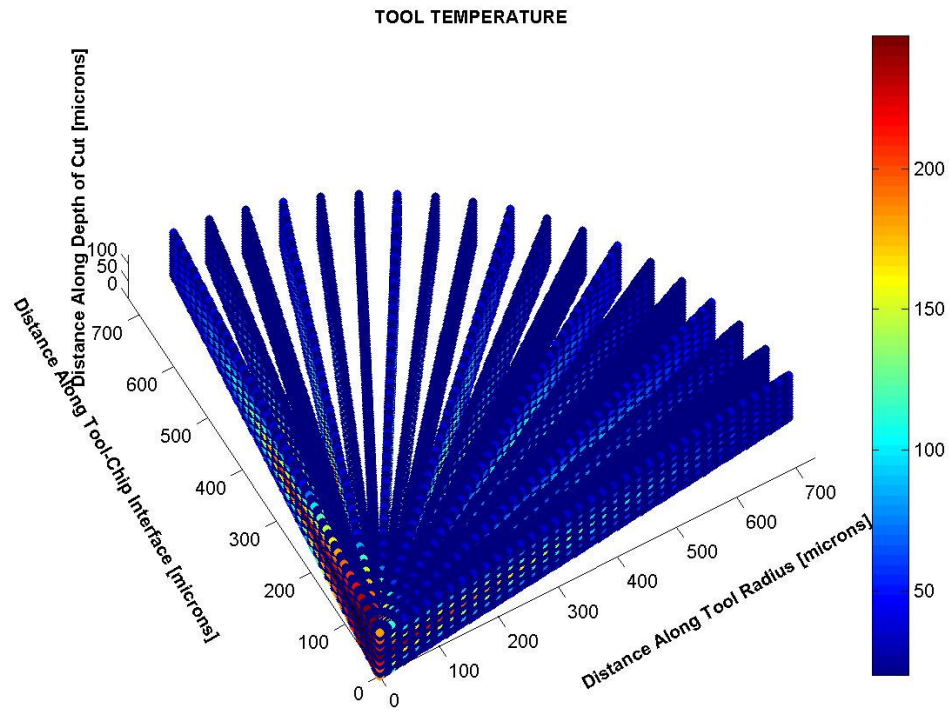
Fig. A.18: Validation for case 18. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



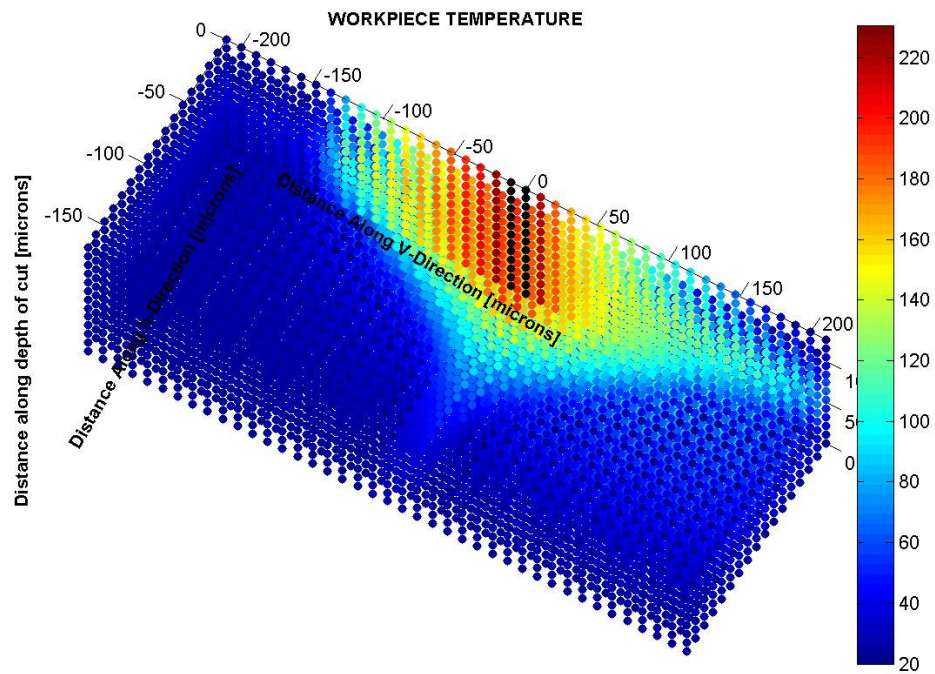
(a)



(b)

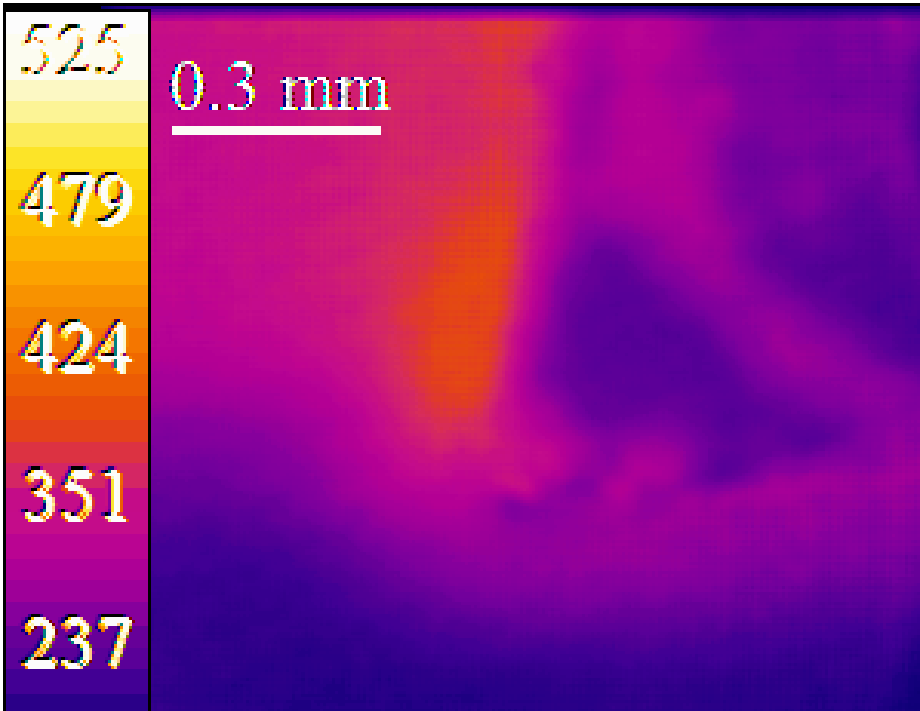


(c)

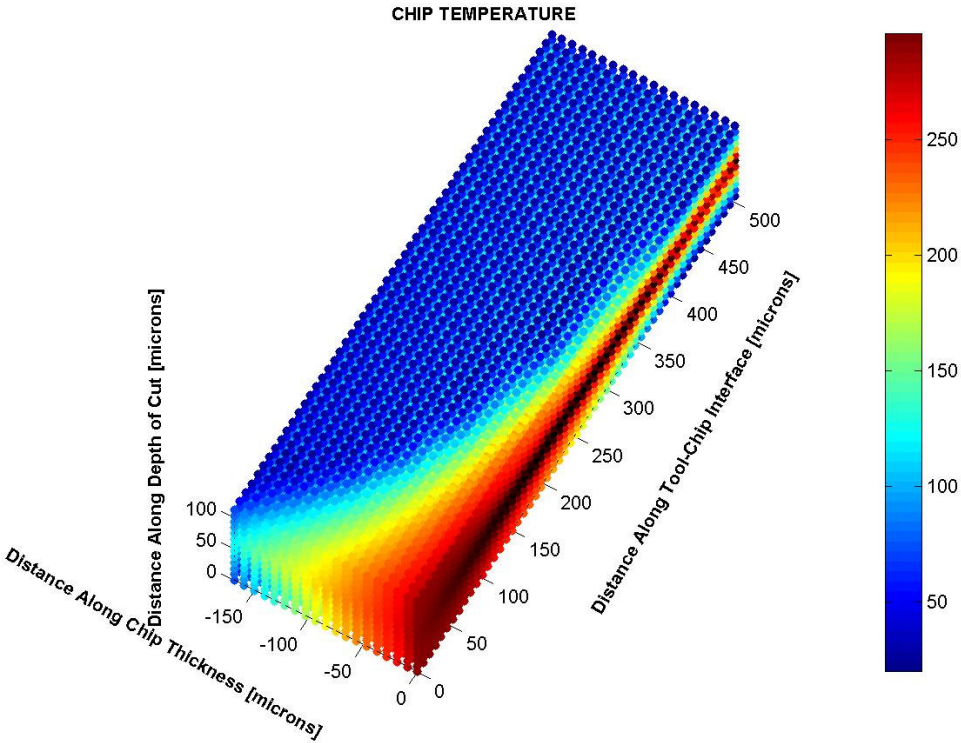


(d)

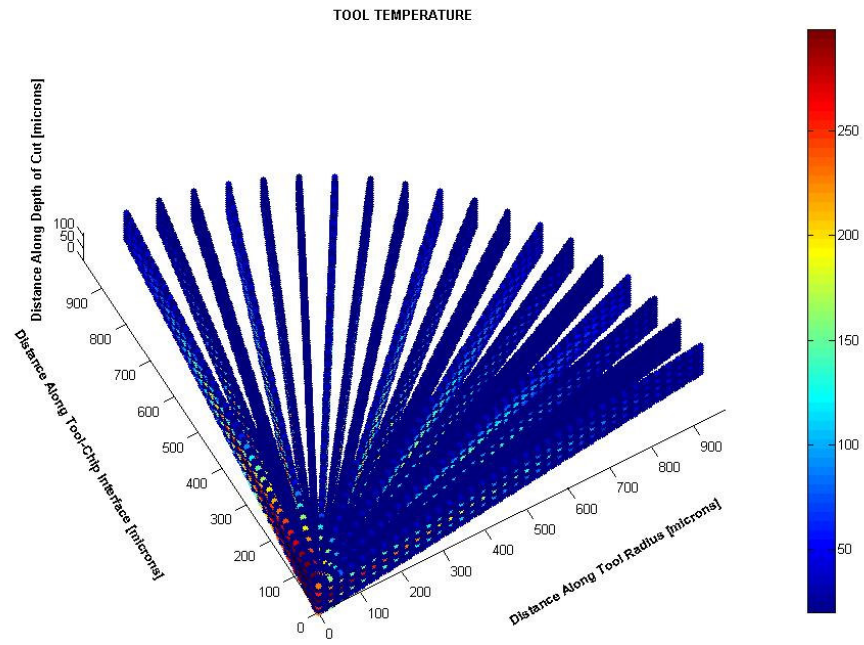
Fig. A.19: Validation for case 19. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



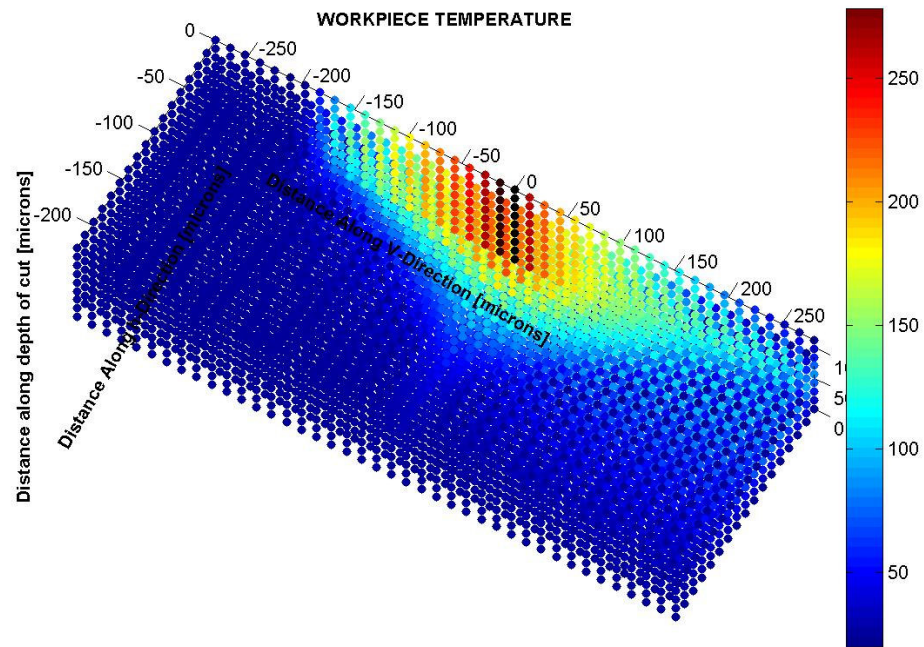
(a)



(b)

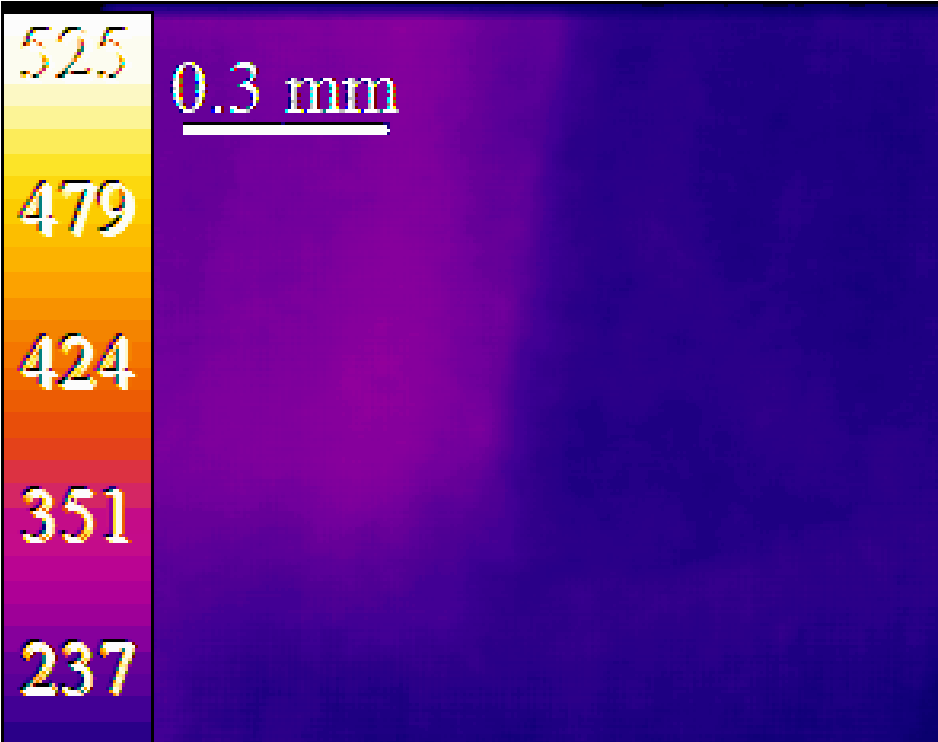


(c)

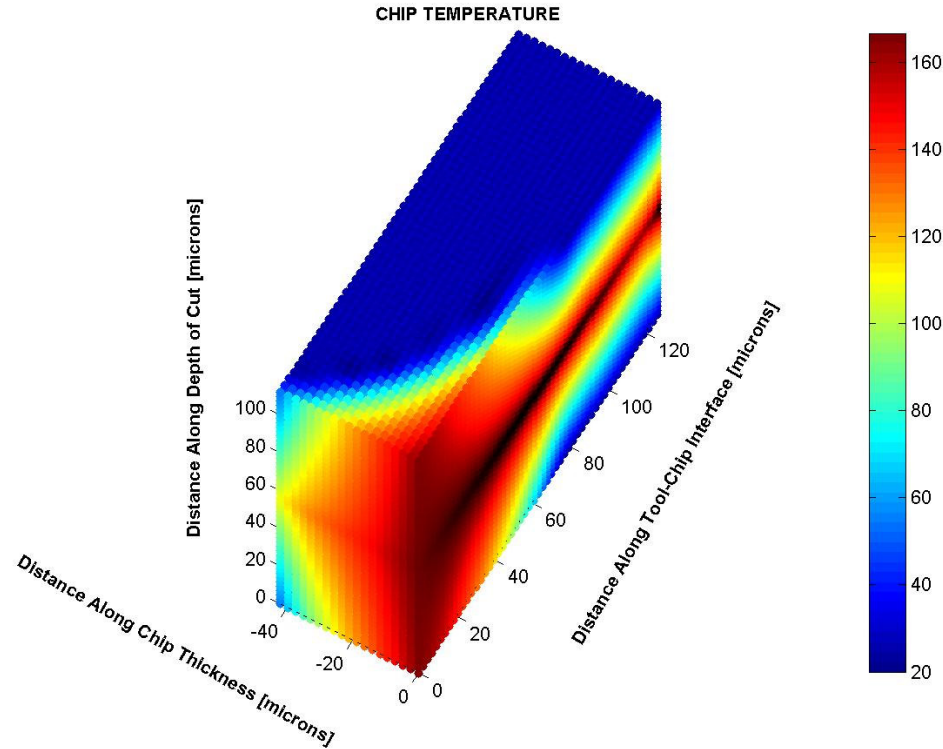


(d)

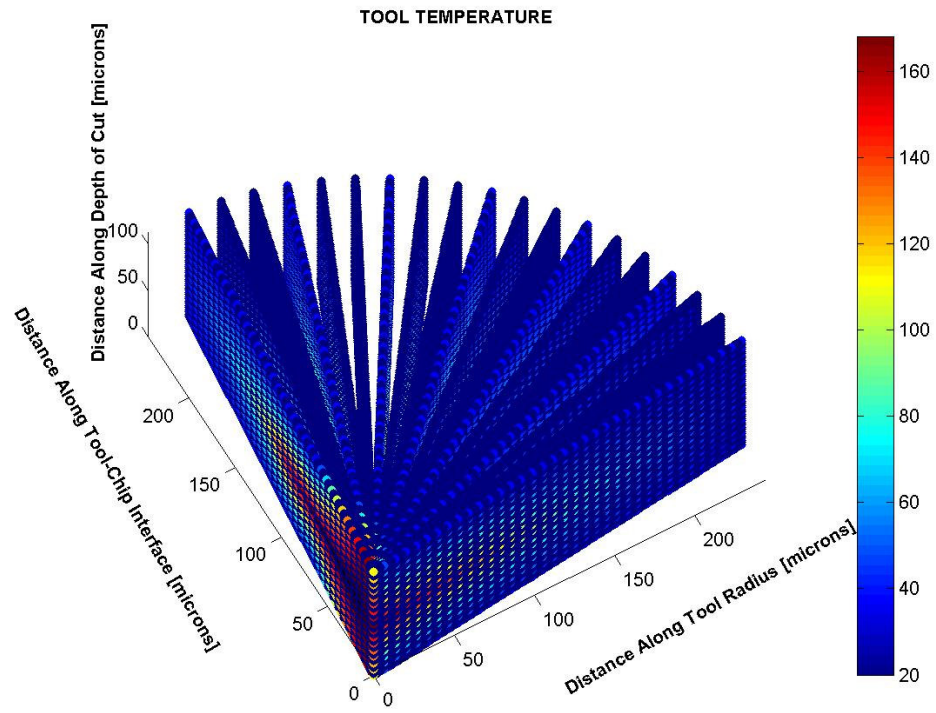
Fig. A.20: Validation for case 20. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



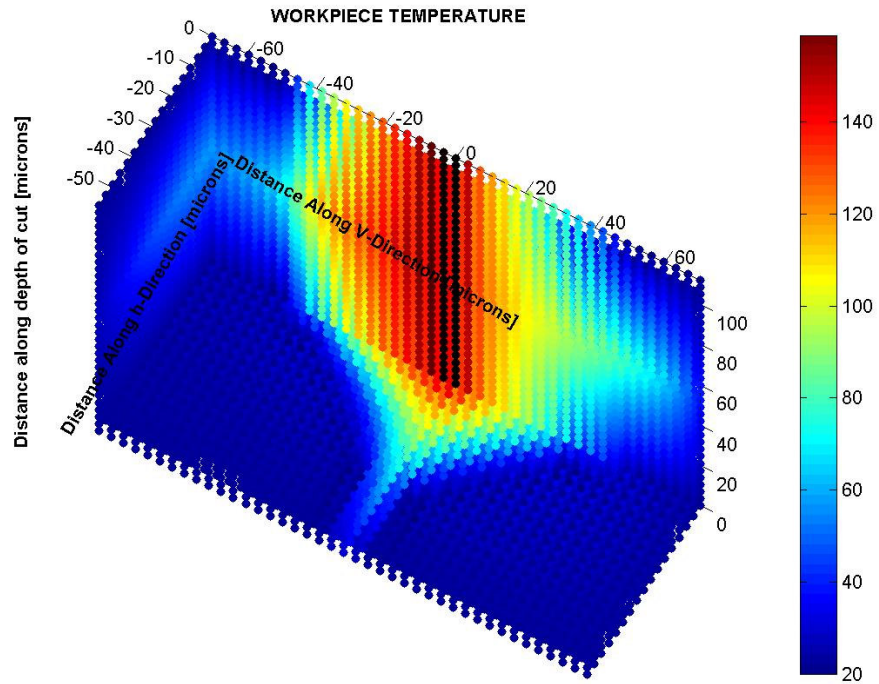
(a)



(b)

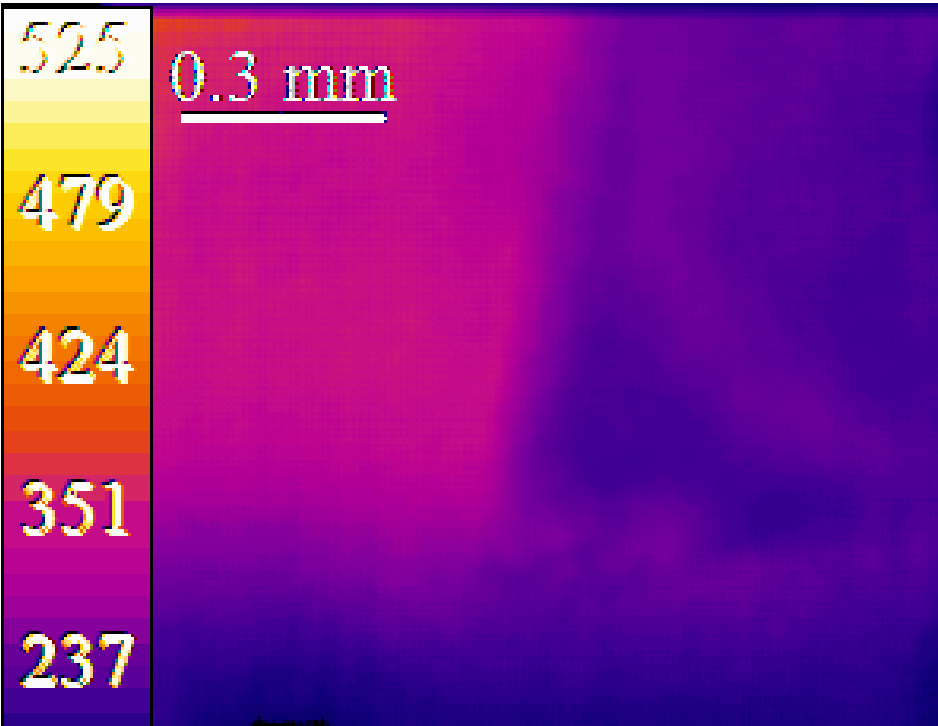


(c)

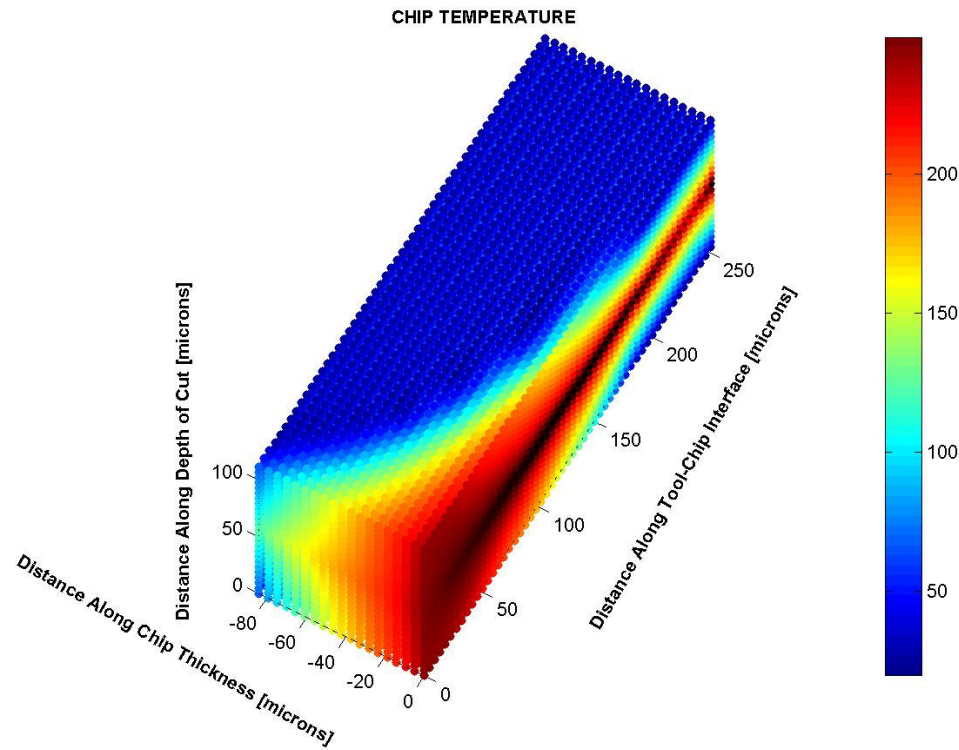


(d)

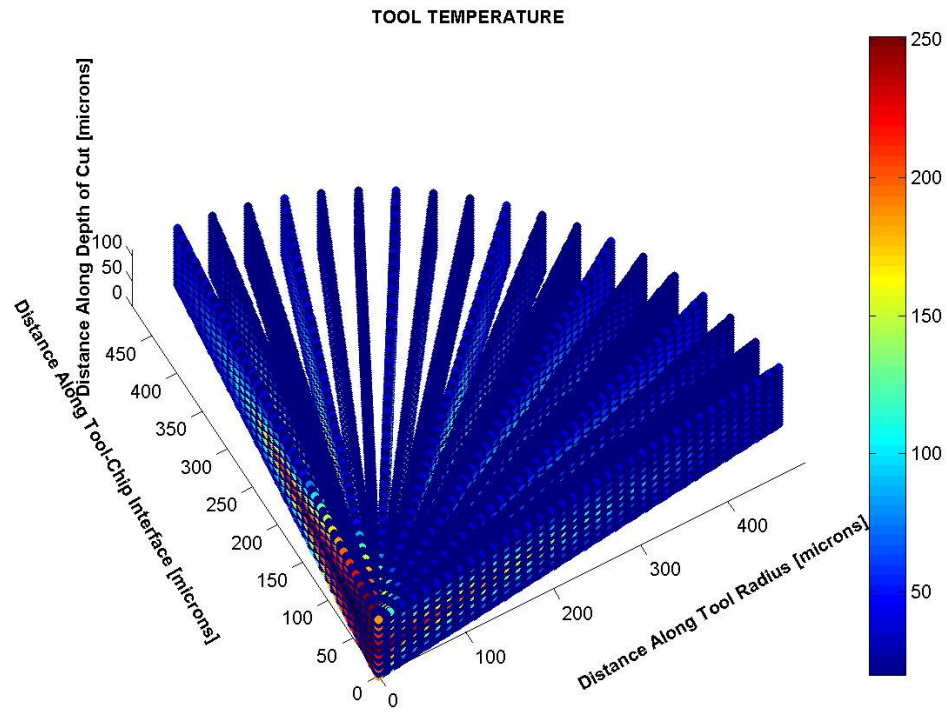
Fig. A.21: Validation for case 21. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



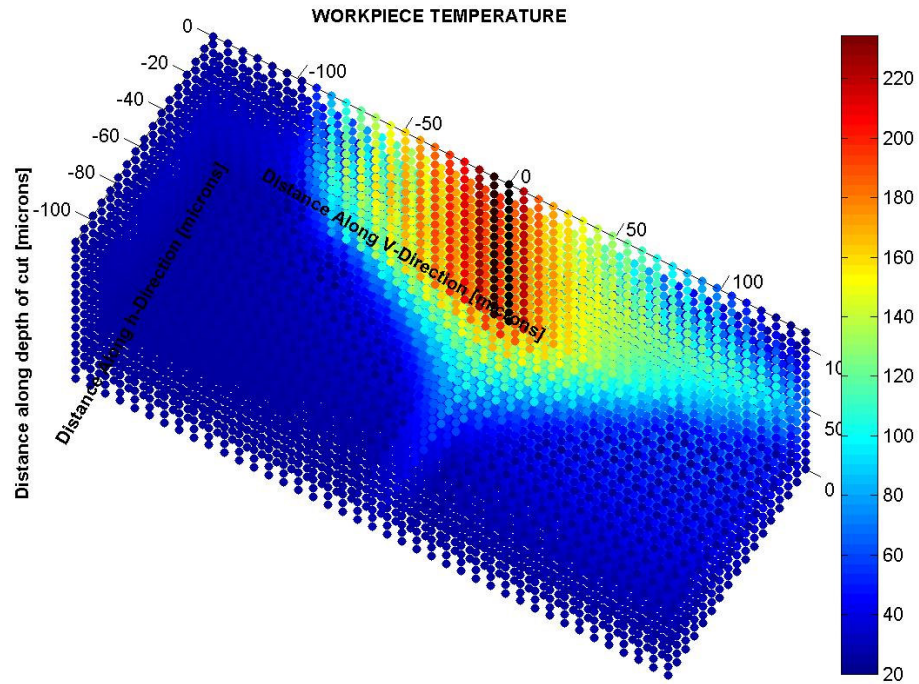
(a)



(b)

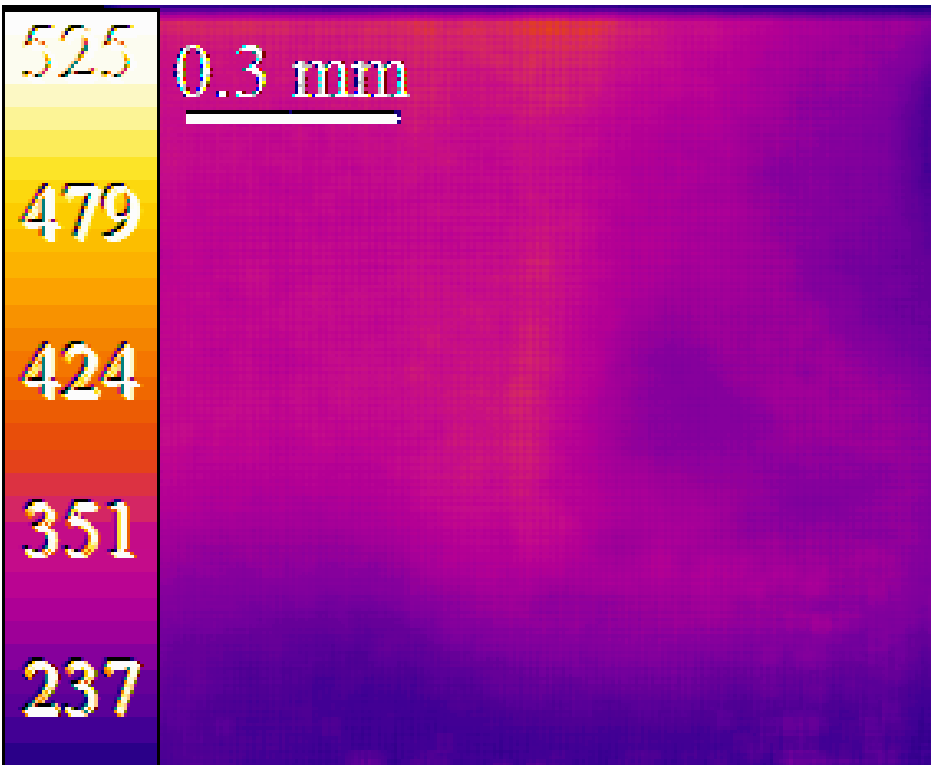


(c)

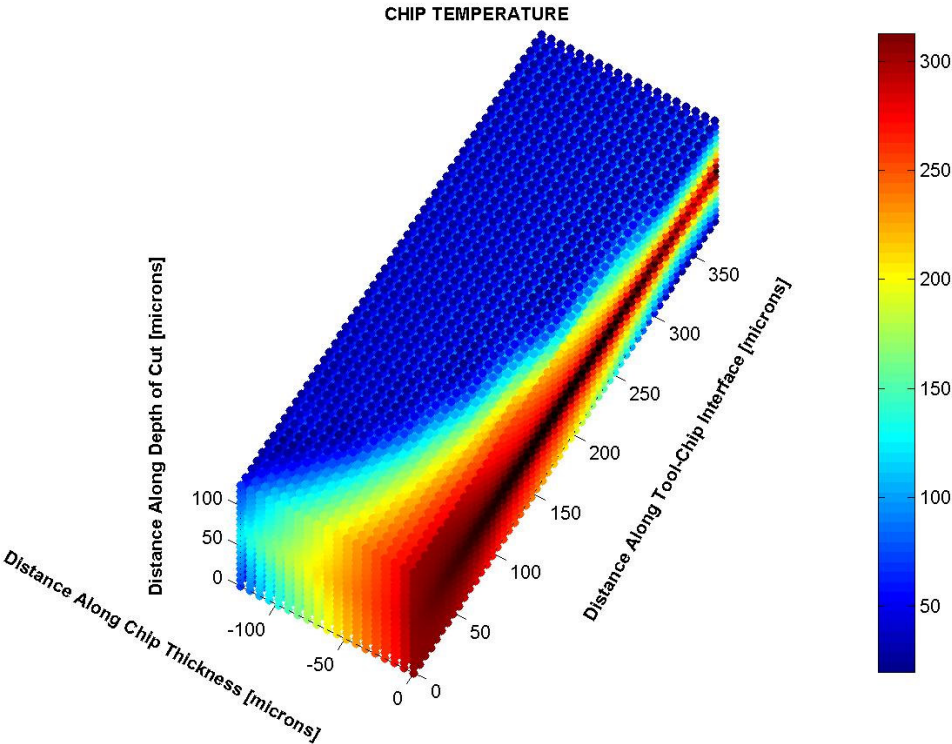


(d)

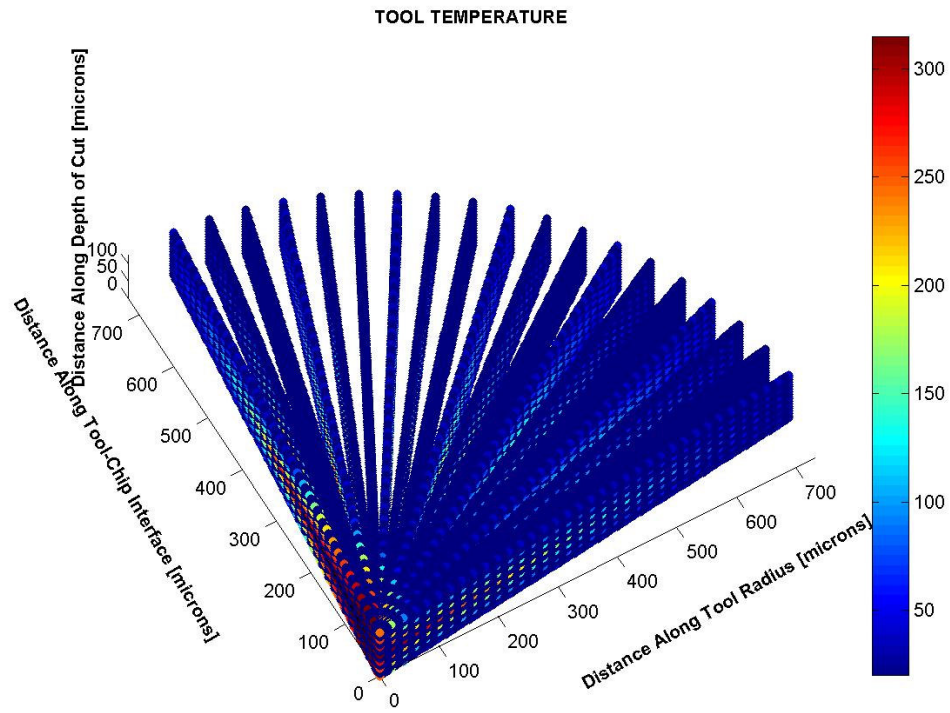
Fig. A.22: Validation for case 22. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



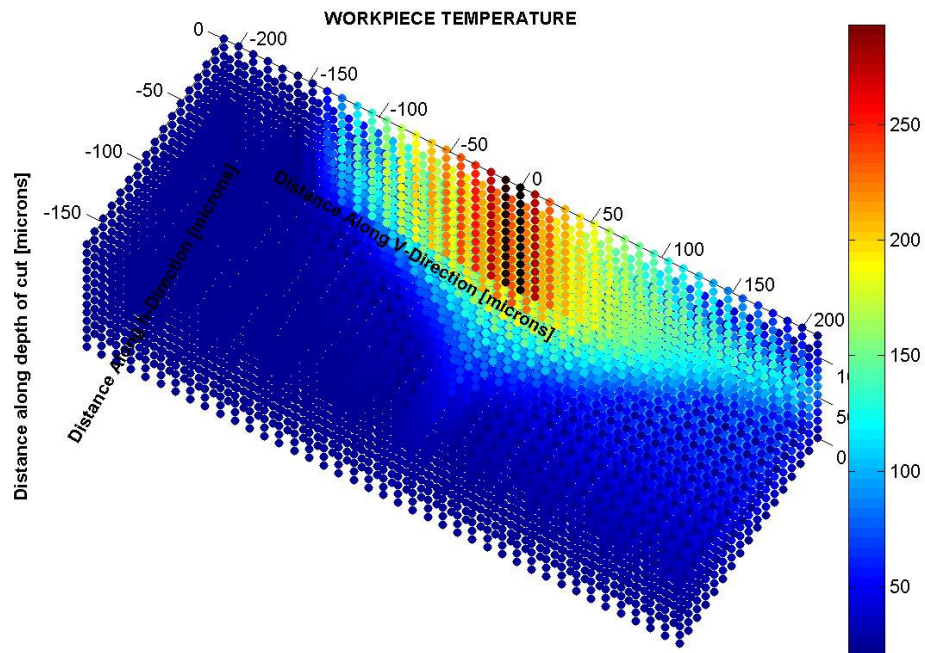
(a)



(b)

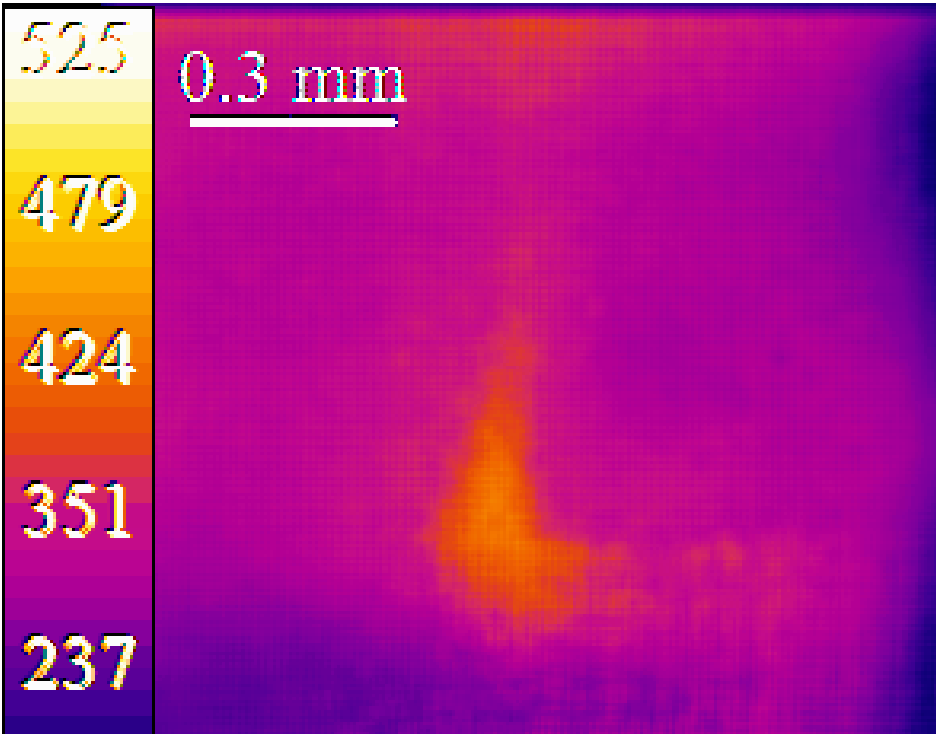


(c)

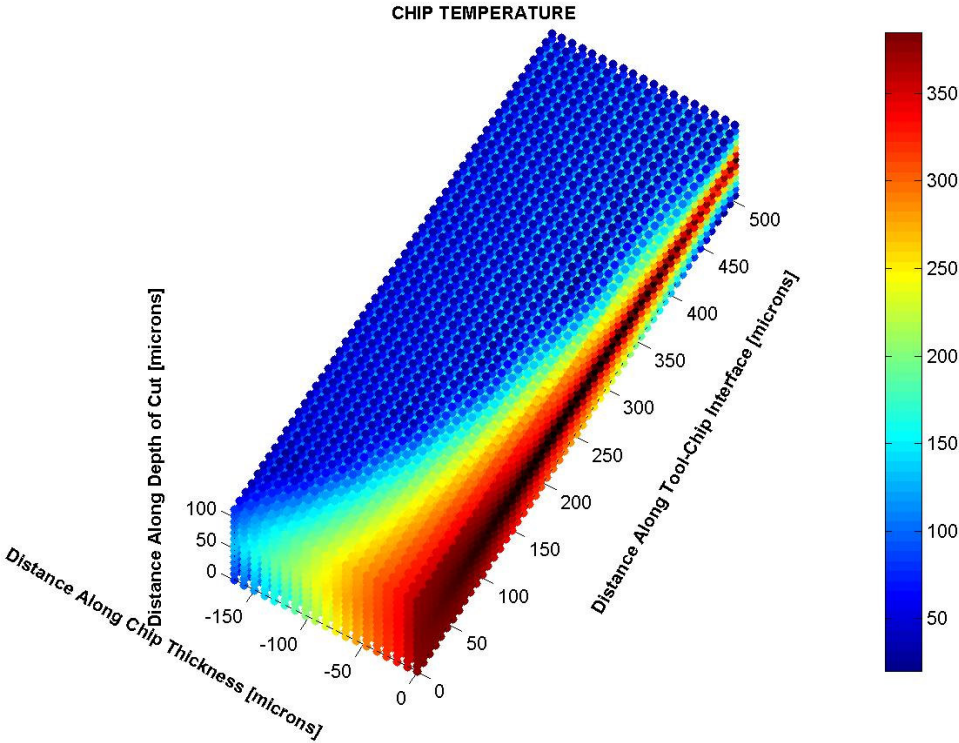


(d)

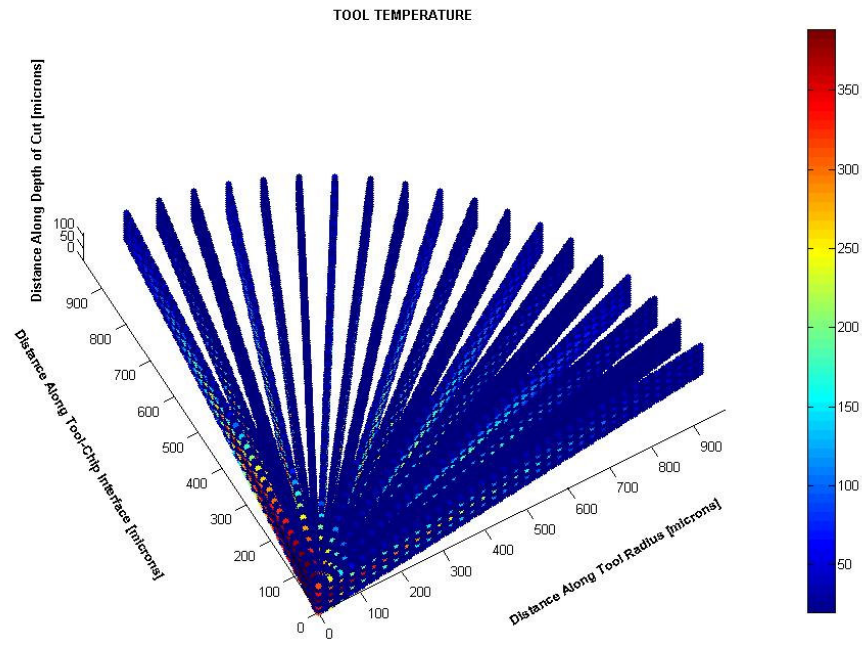
Fig. A.23: Validation for case 23. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



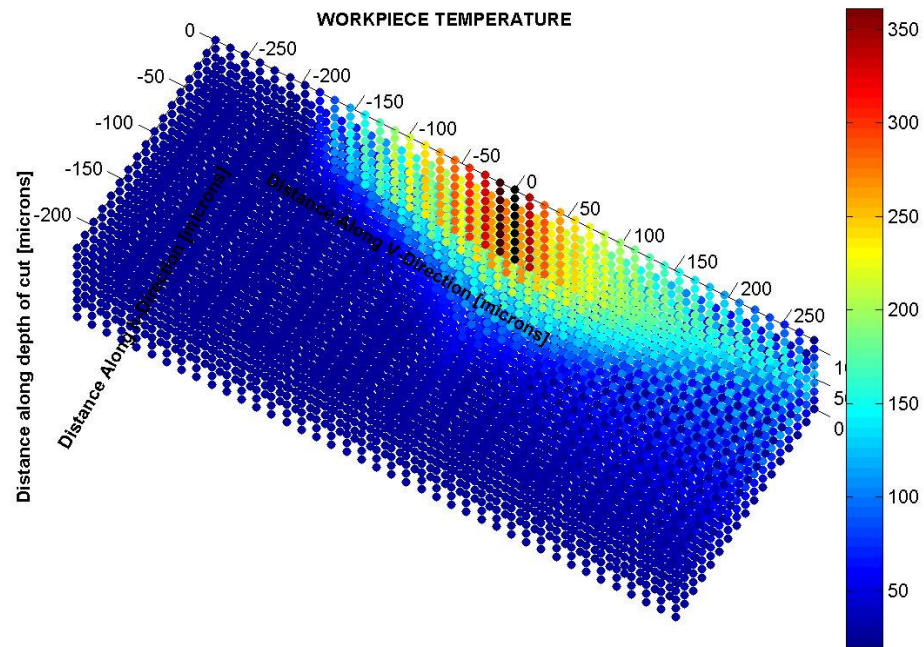
(a)



(b)

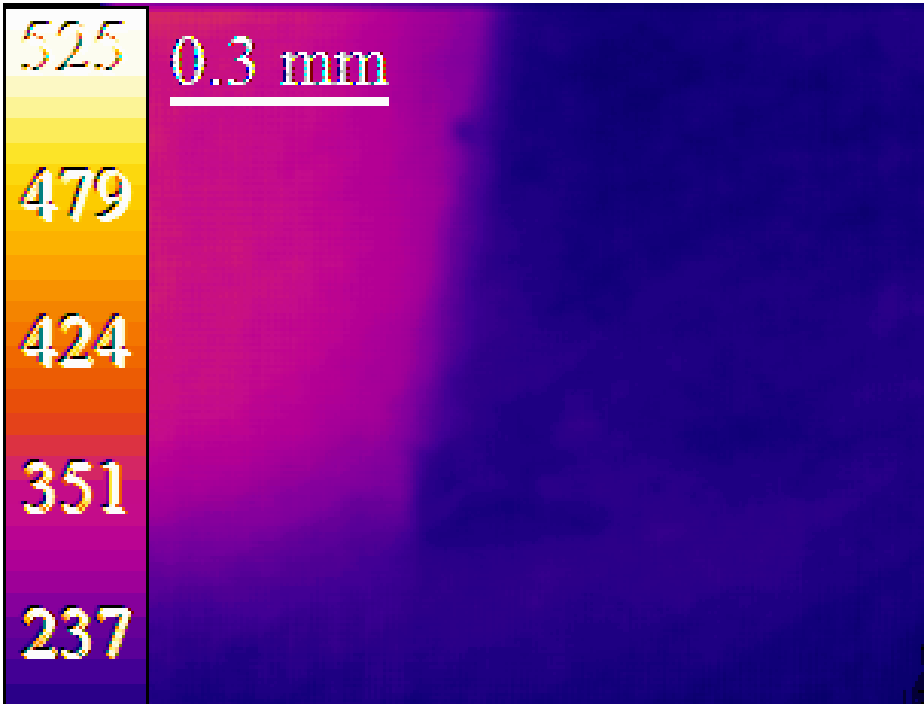


(c)

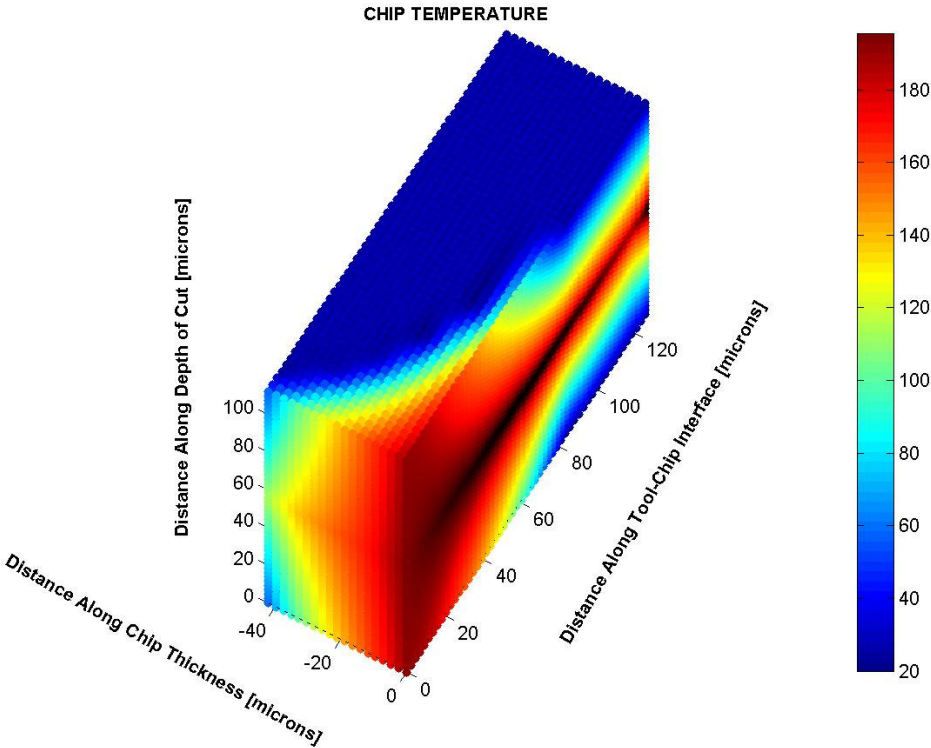


(d)

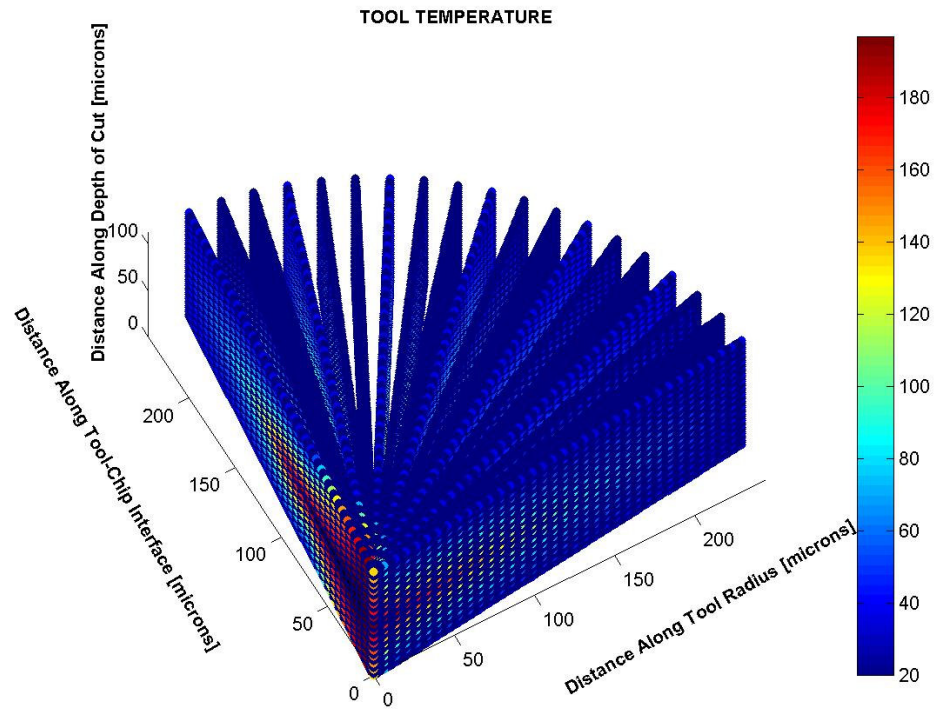
Fig. A.24: Validation for case 24. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



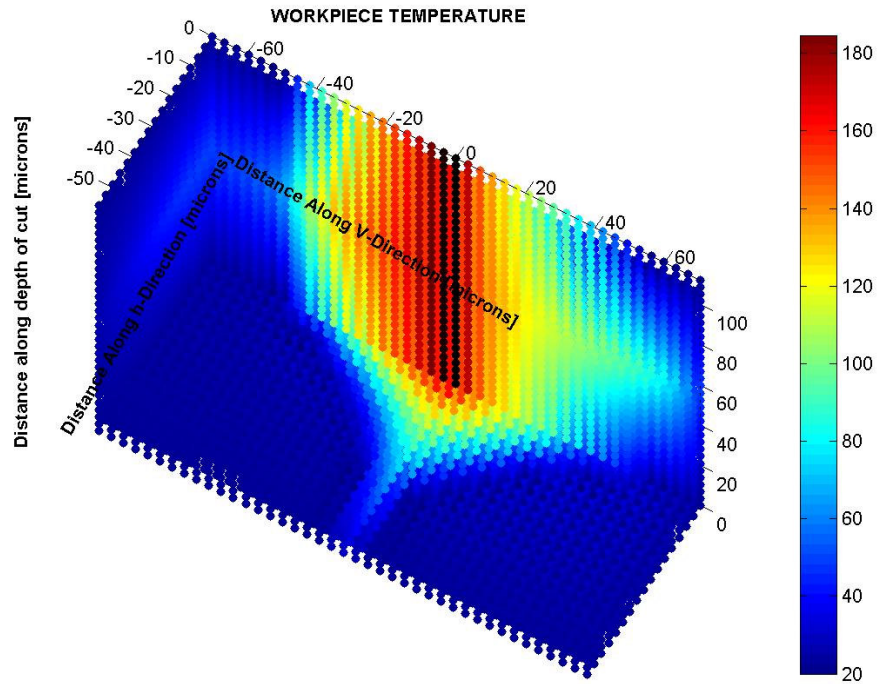
(a)



(b)

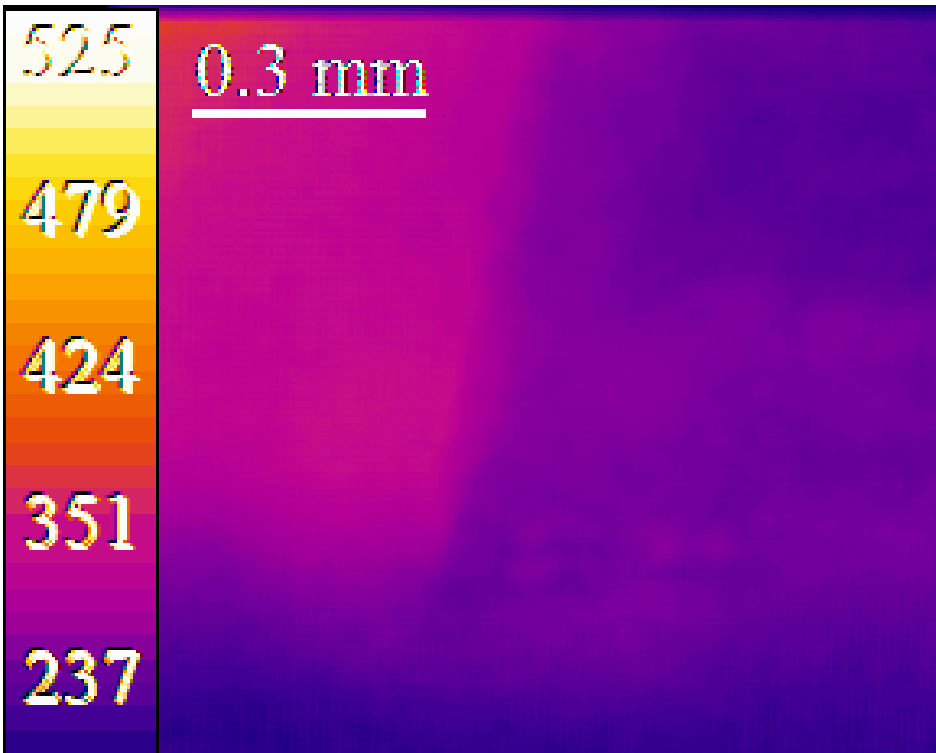


(c)

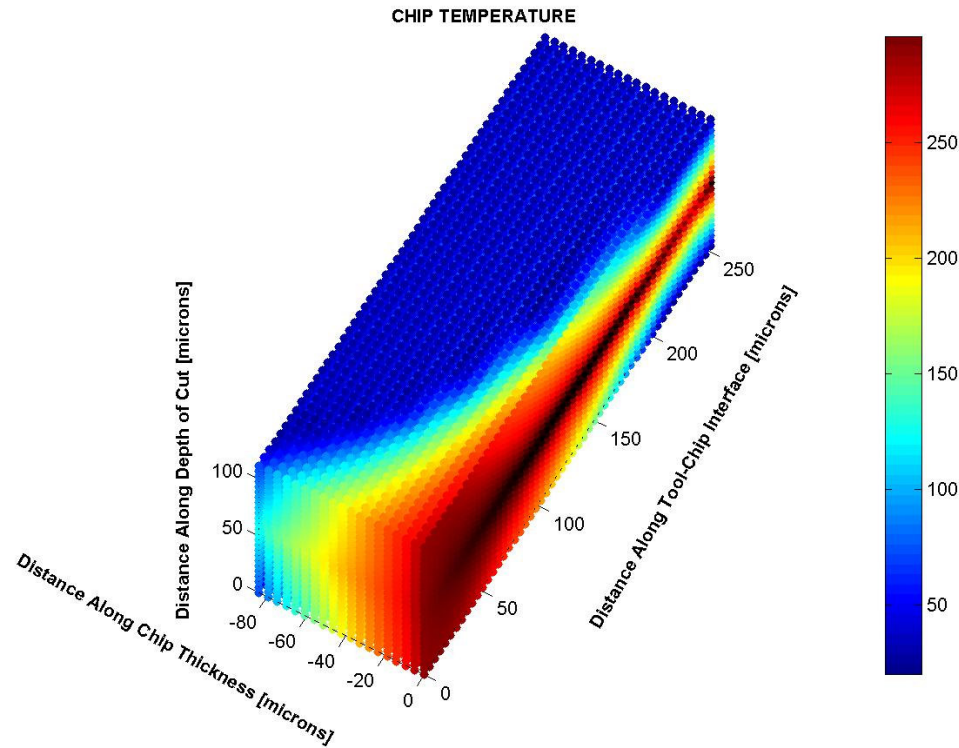


(d)

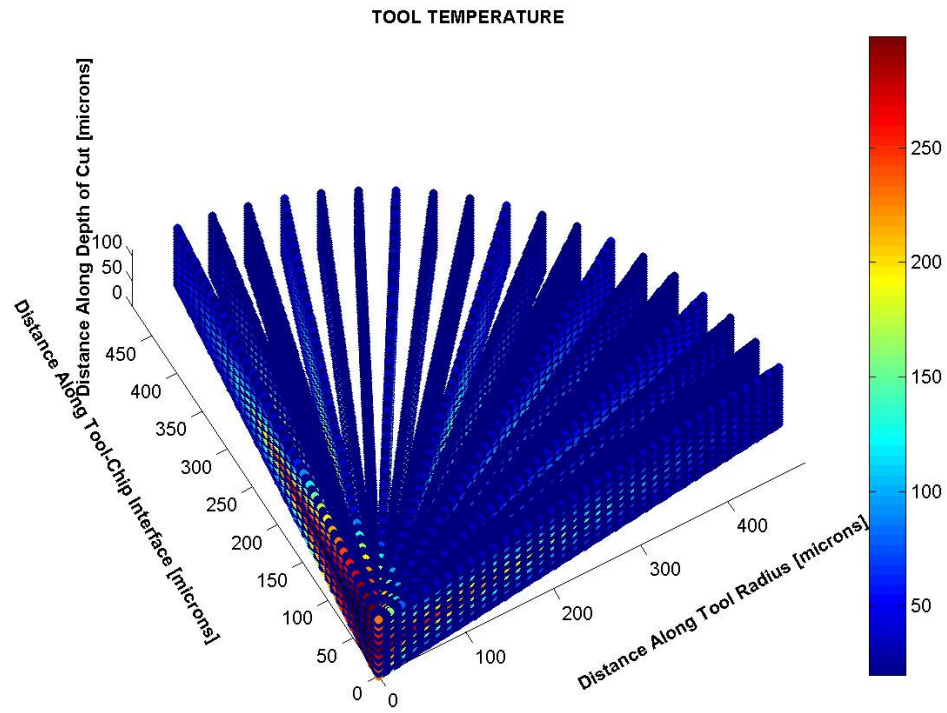
Fig. A.25: Validation for case 25. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



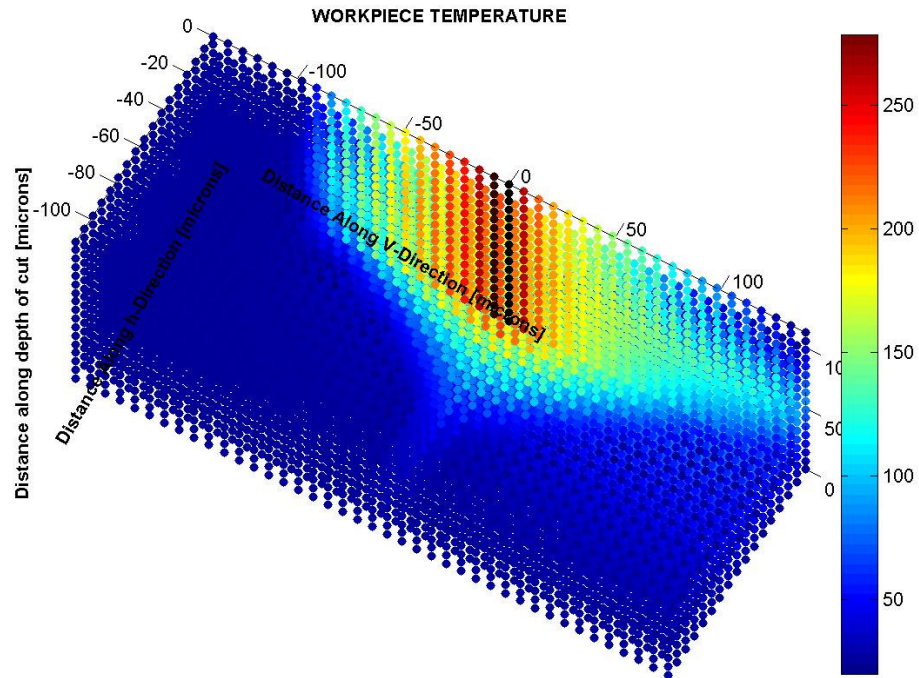
(a)



(b)

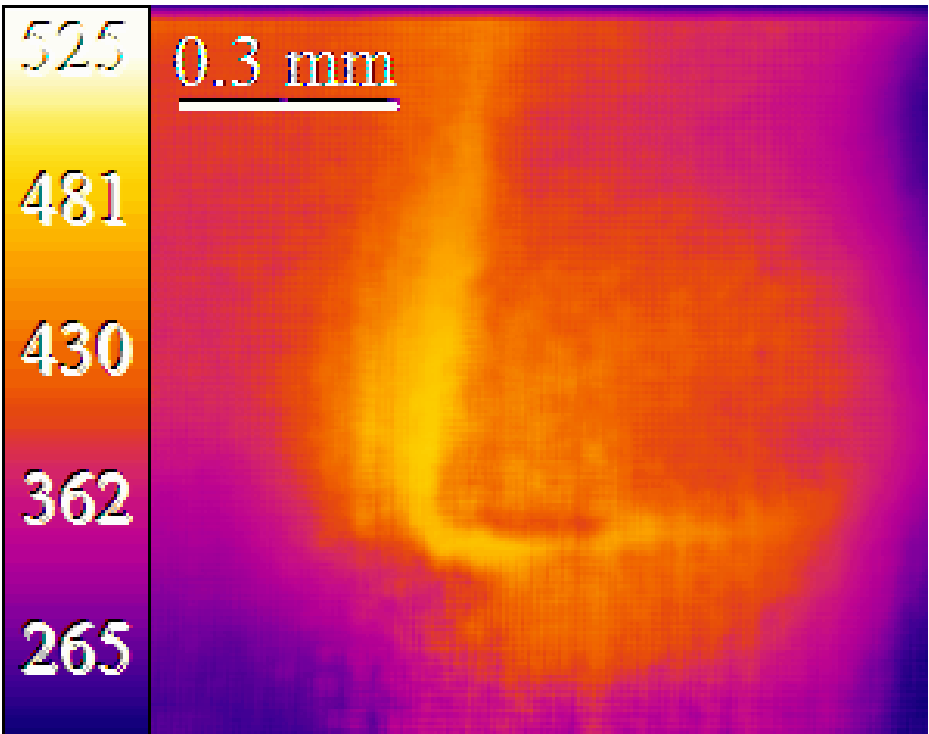


(c)

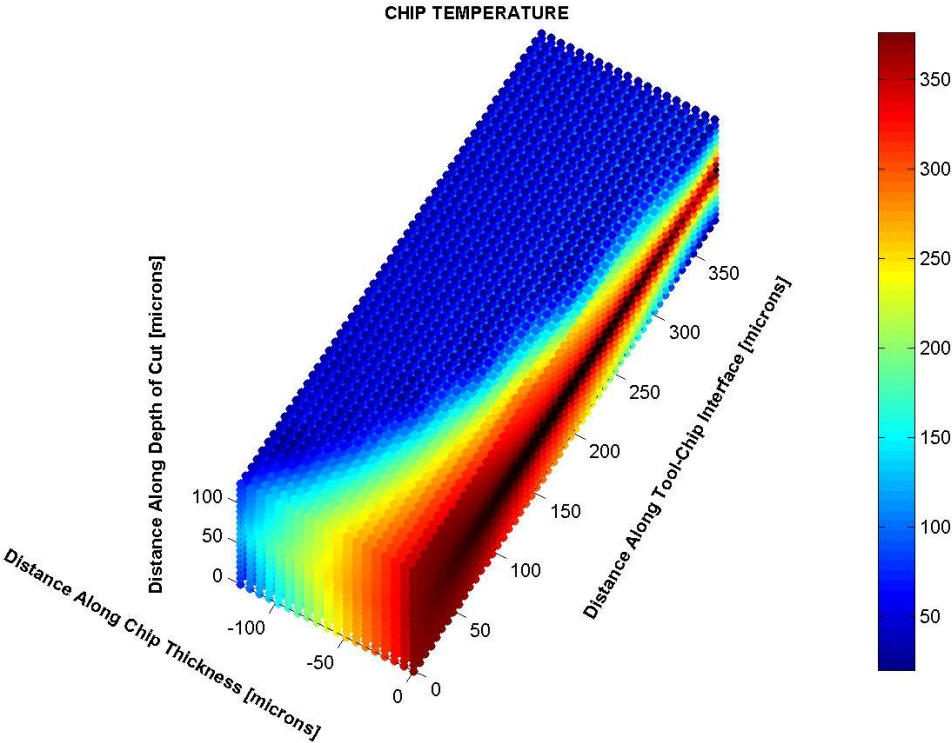


(d)

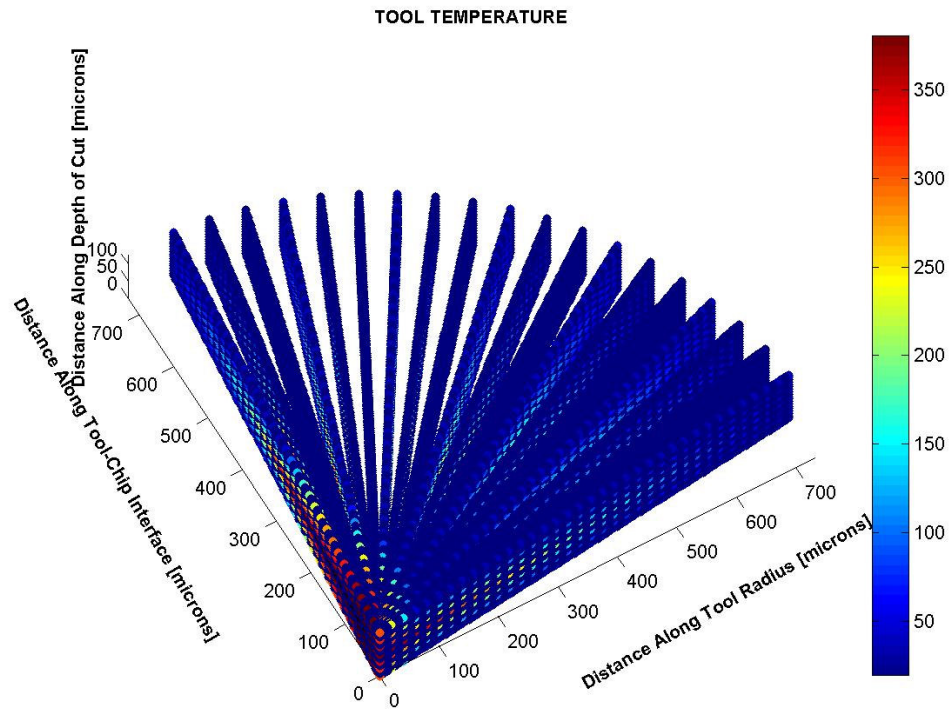
Fig. A.26: Validation for case 26. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



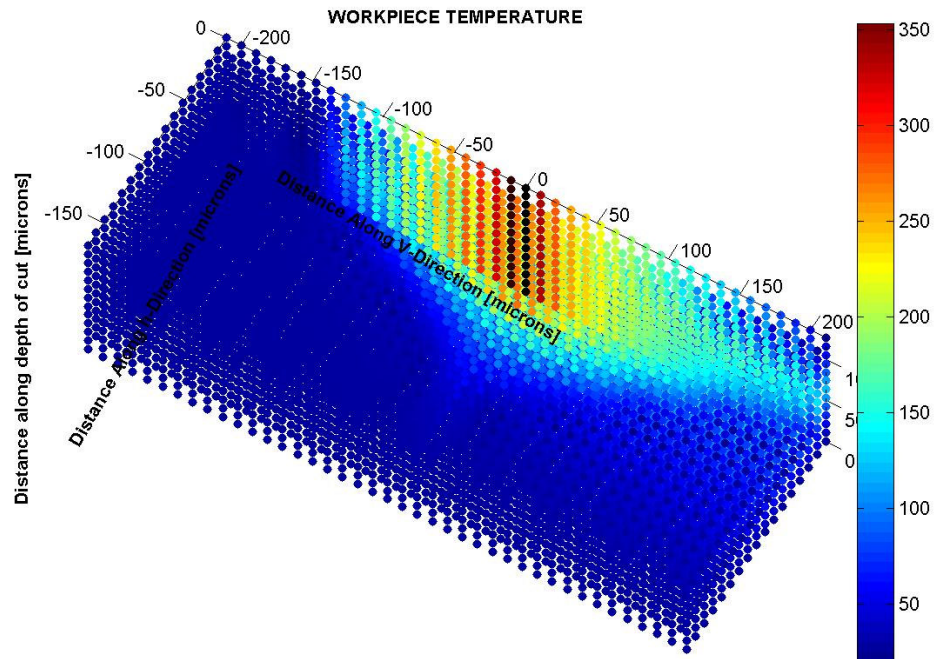
(a)



(b)

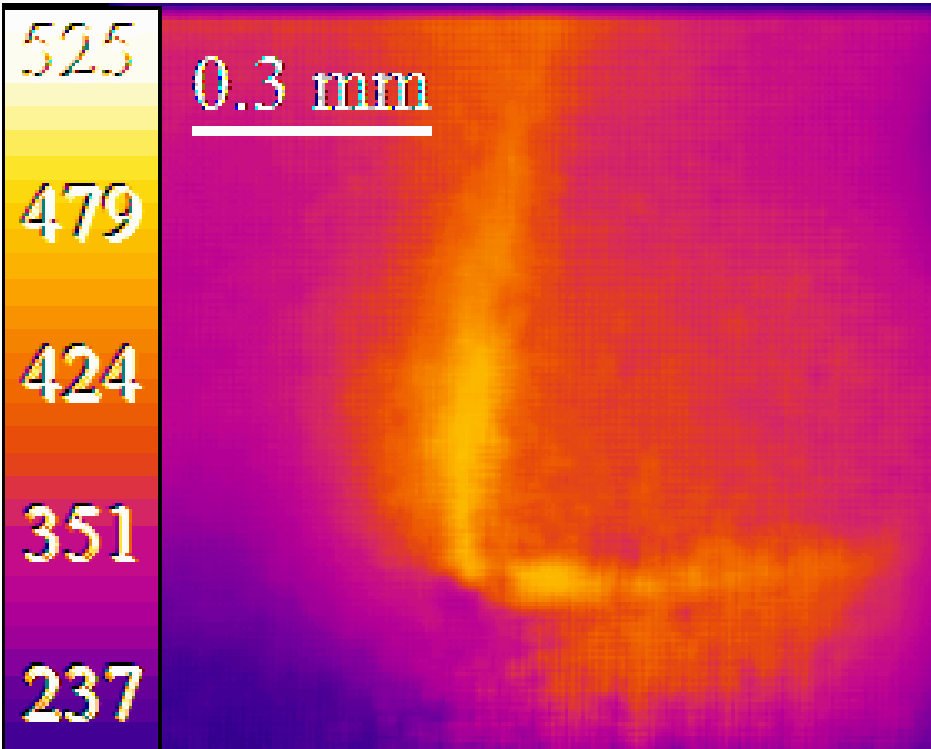


(c)

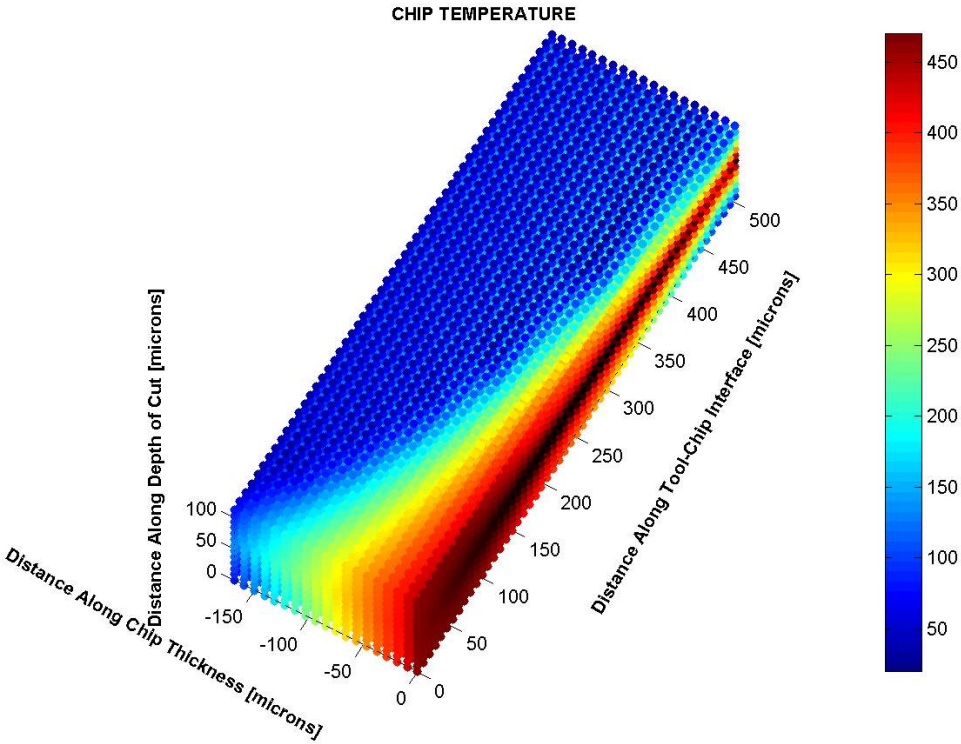


(d)

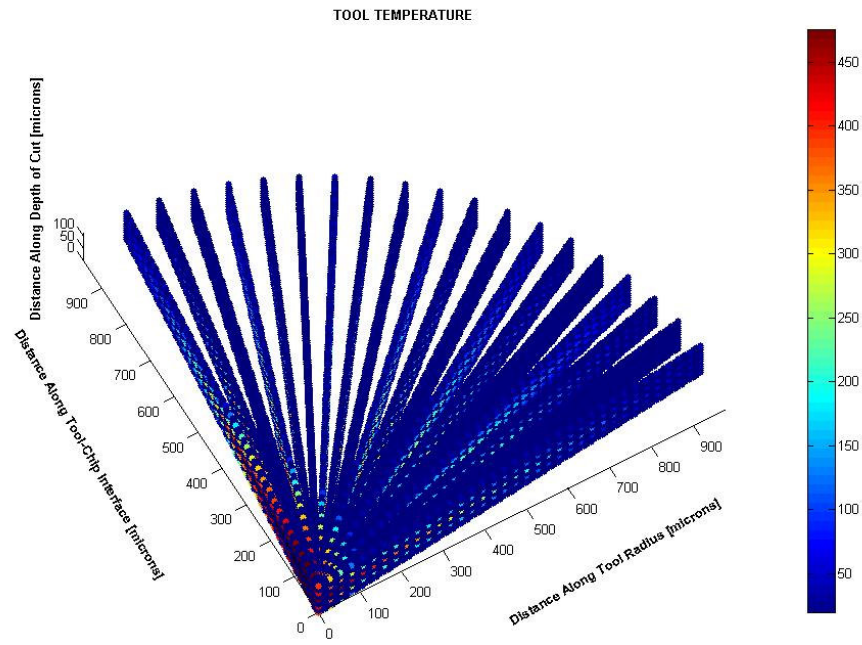
Fig. A.27: Validation for case 27. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



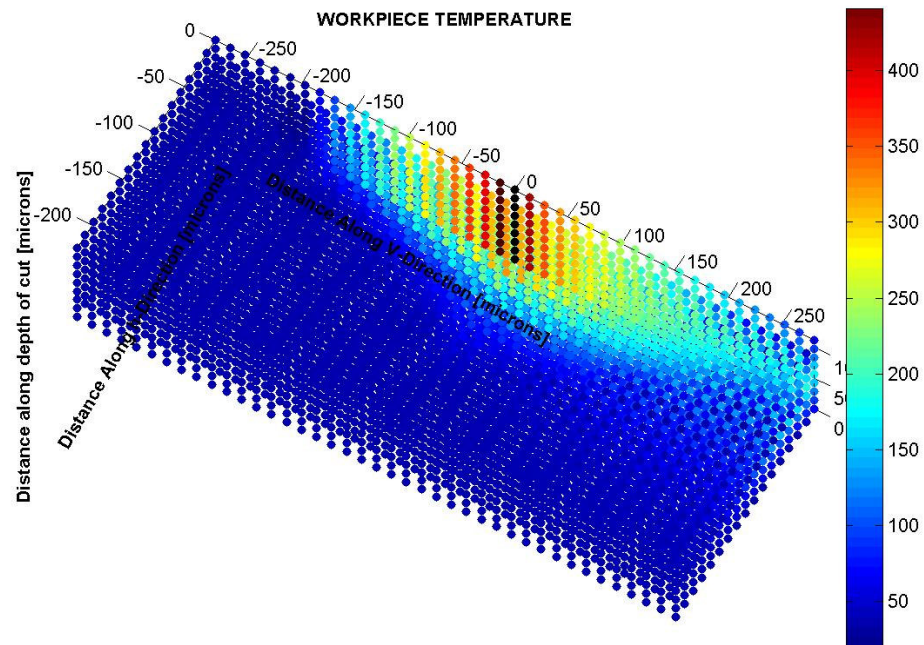
(a)



(b)

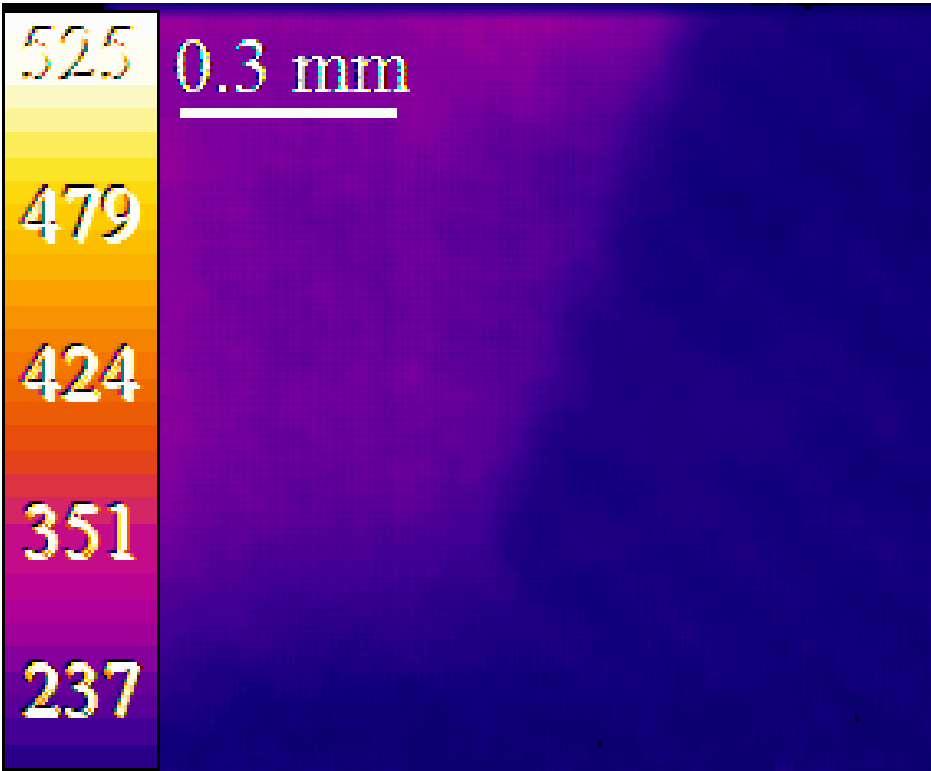


(c)

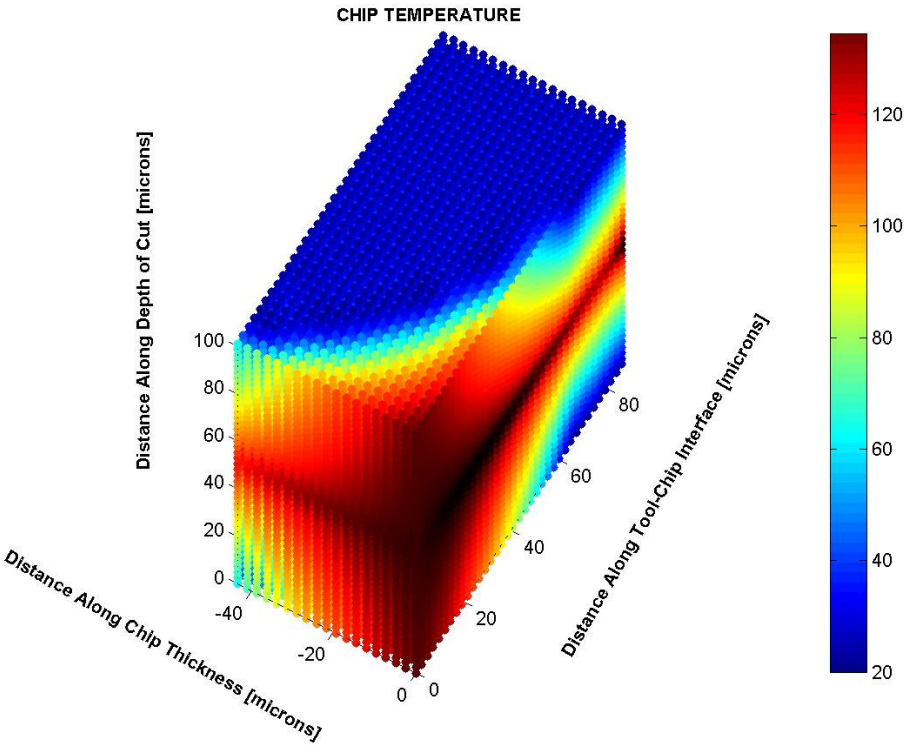


(d)

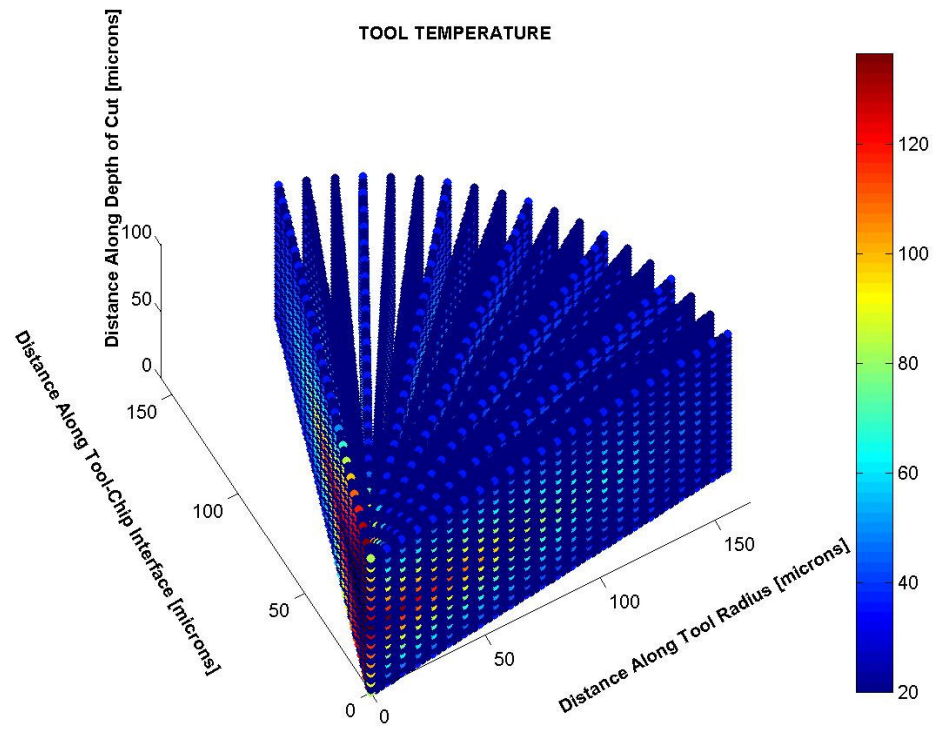
Fig. A.28: Validation for case 28. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



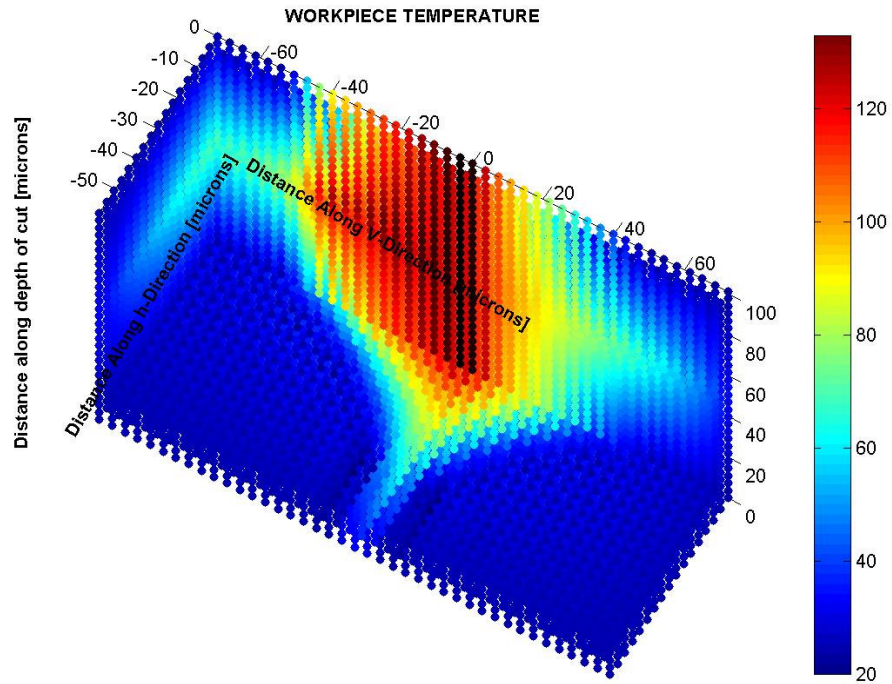
(a)



(b)

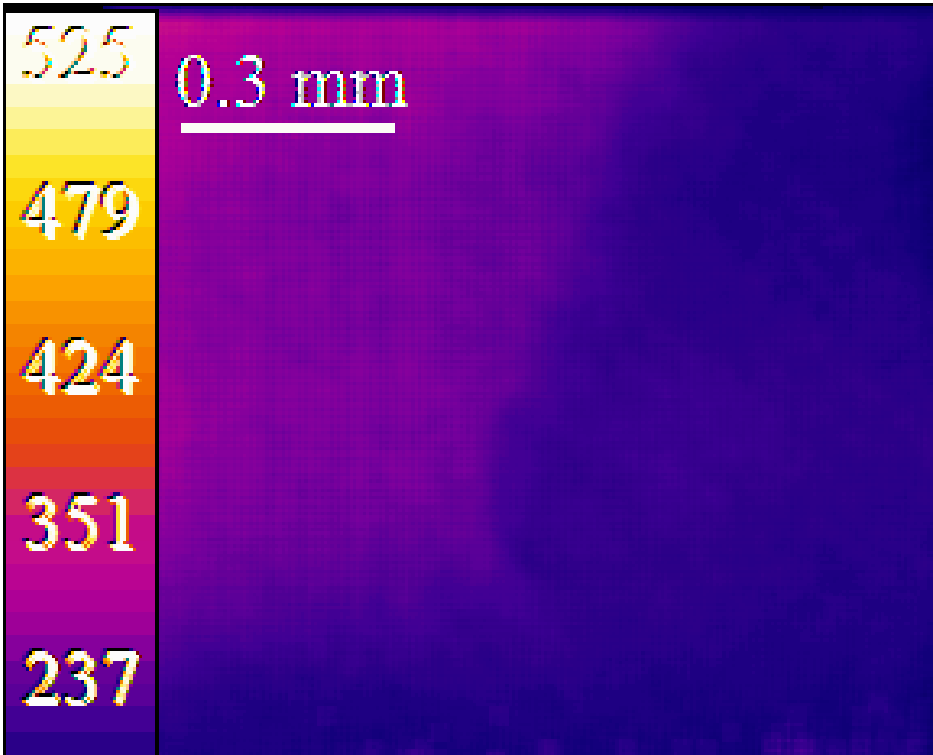


(c)

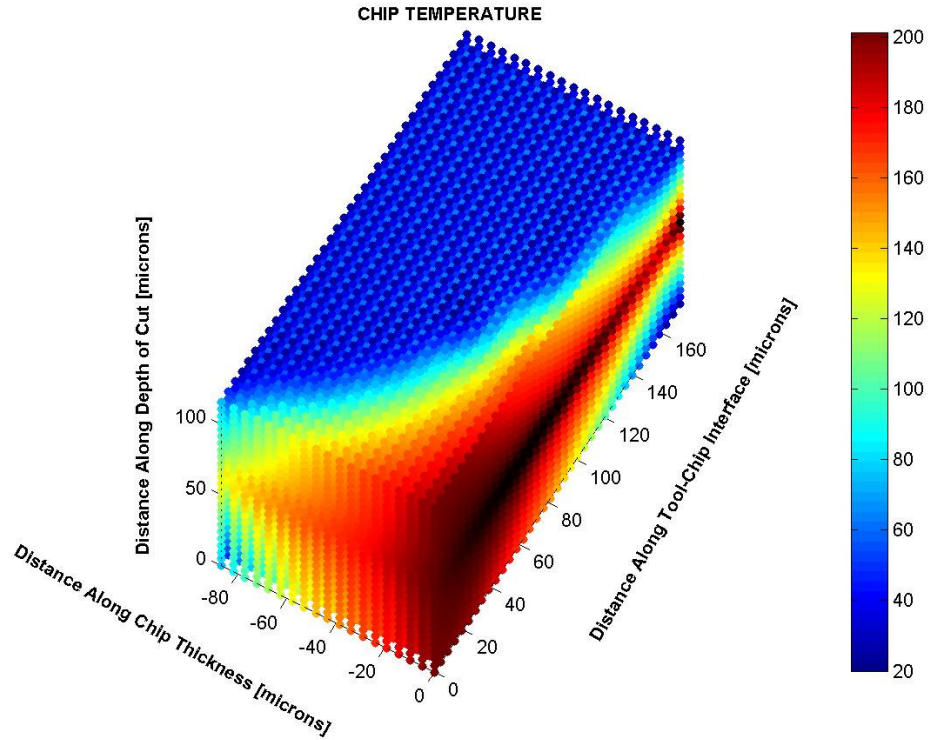


(d)

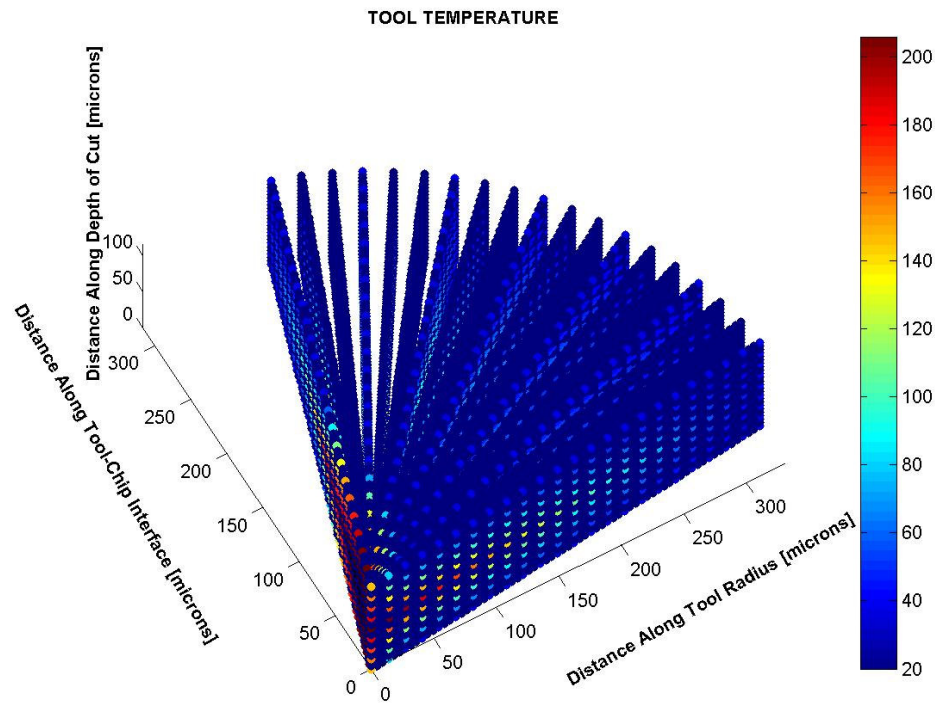
Fig. A.29: Validation for case 29. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



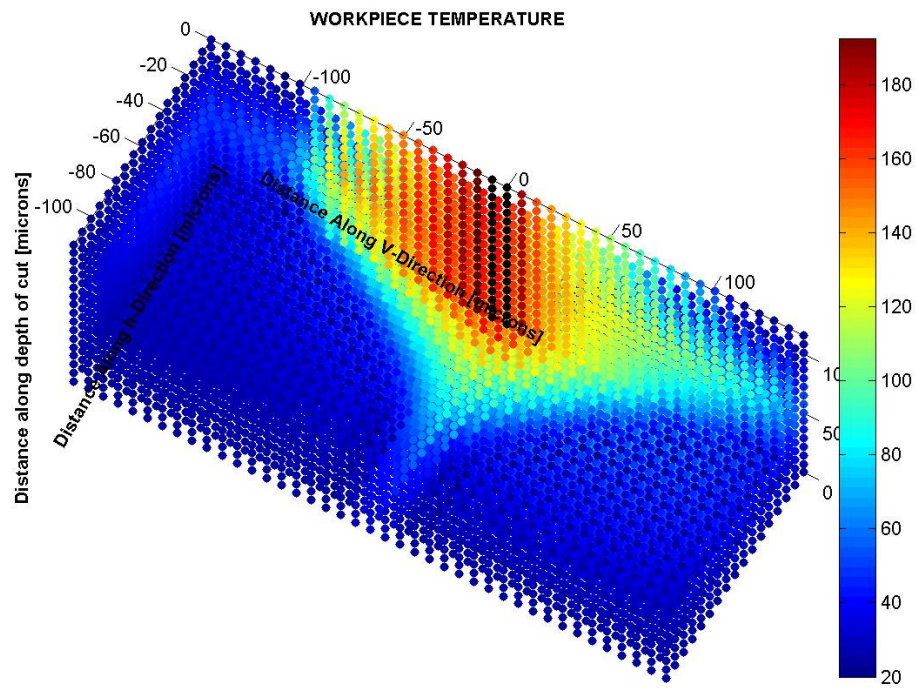
(a)



(b)

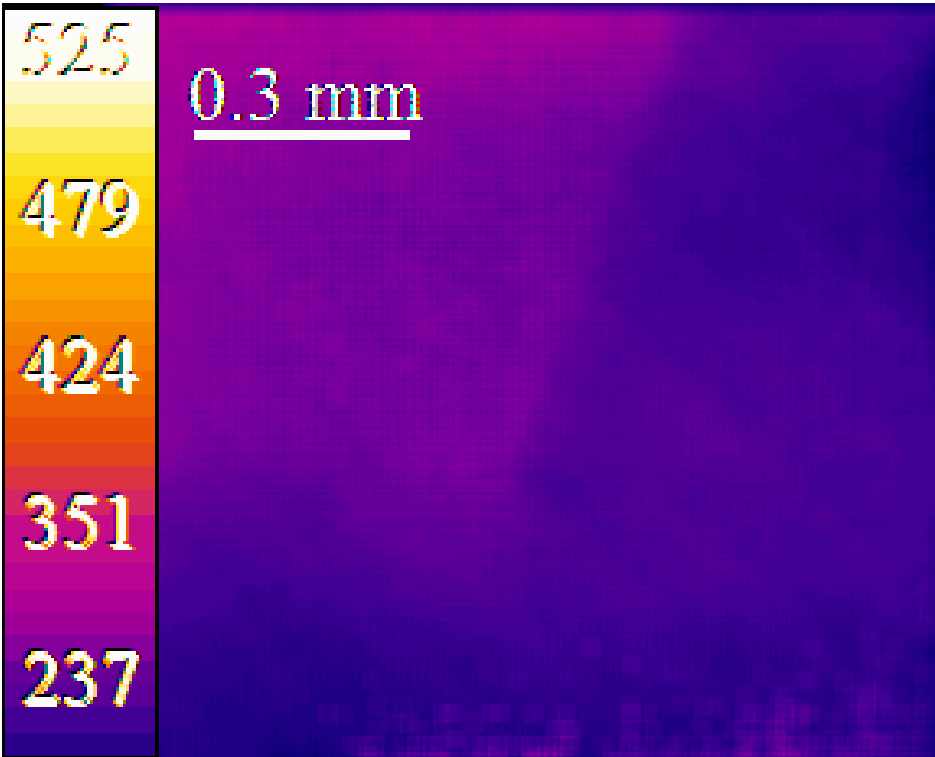


(c)

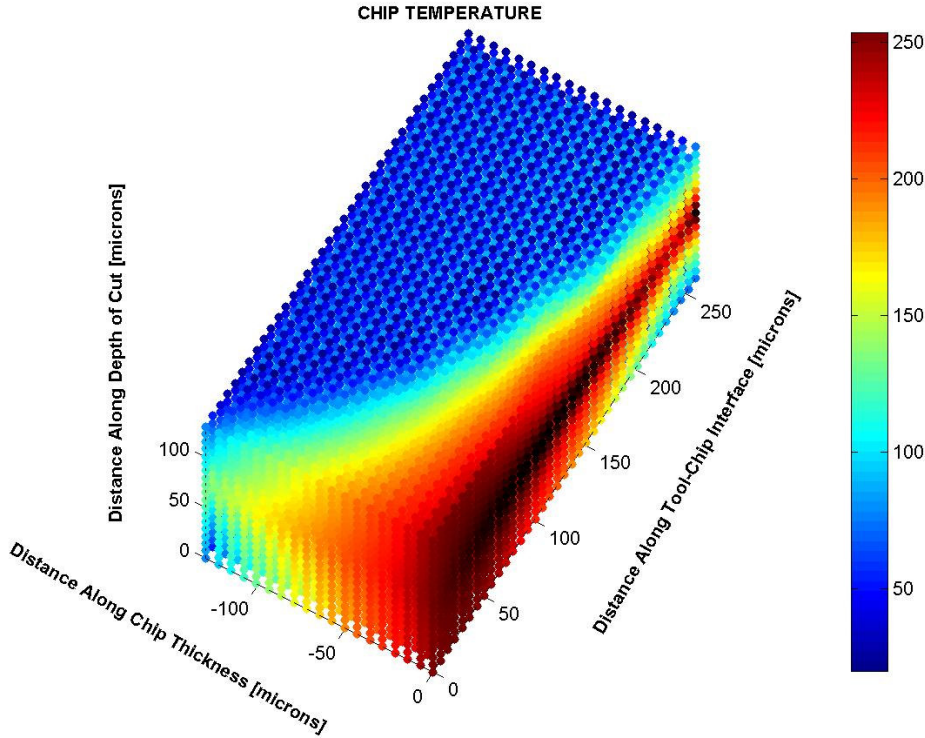


(d)

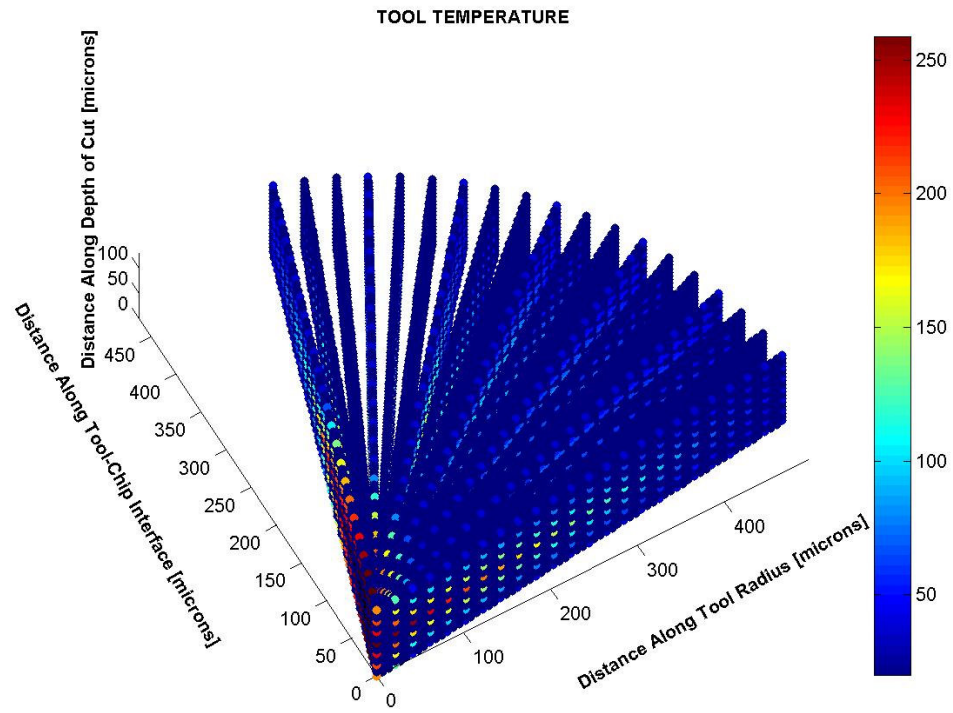
Fig. A.30: Validation for case 30. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



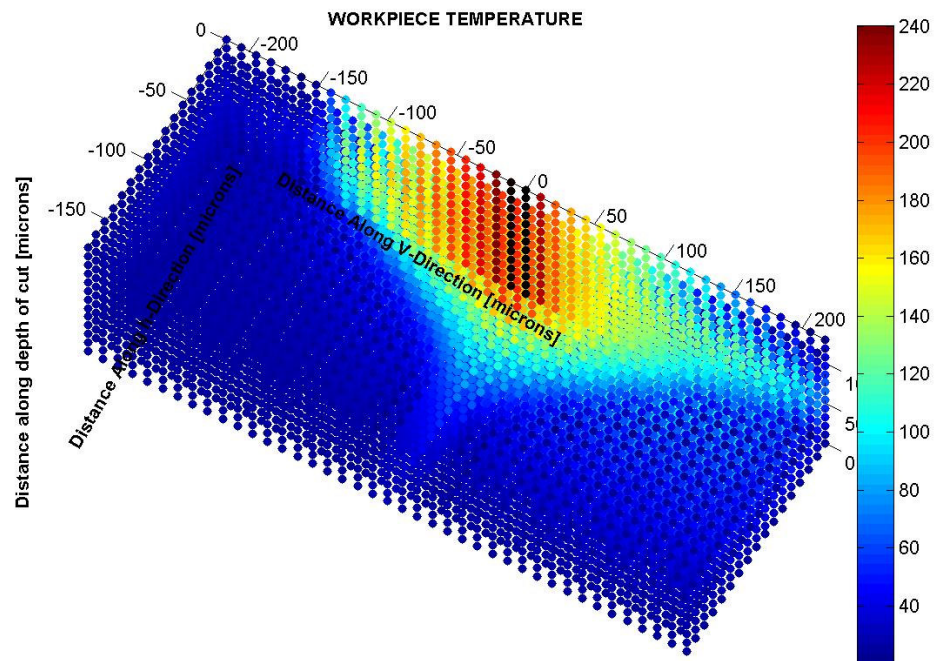
(a)



(b)

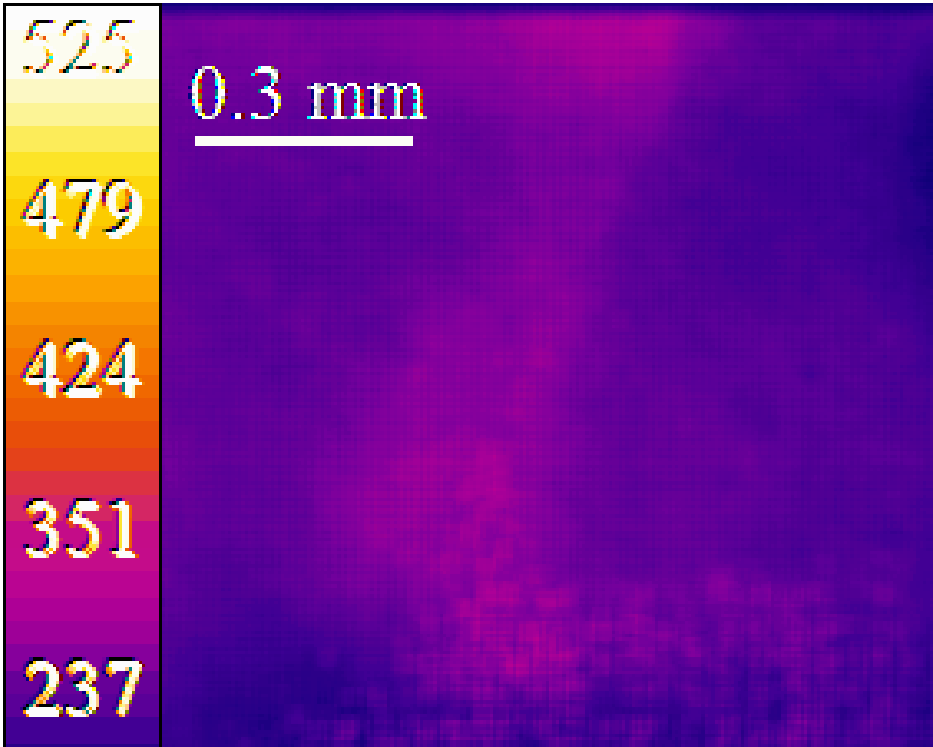


(c)

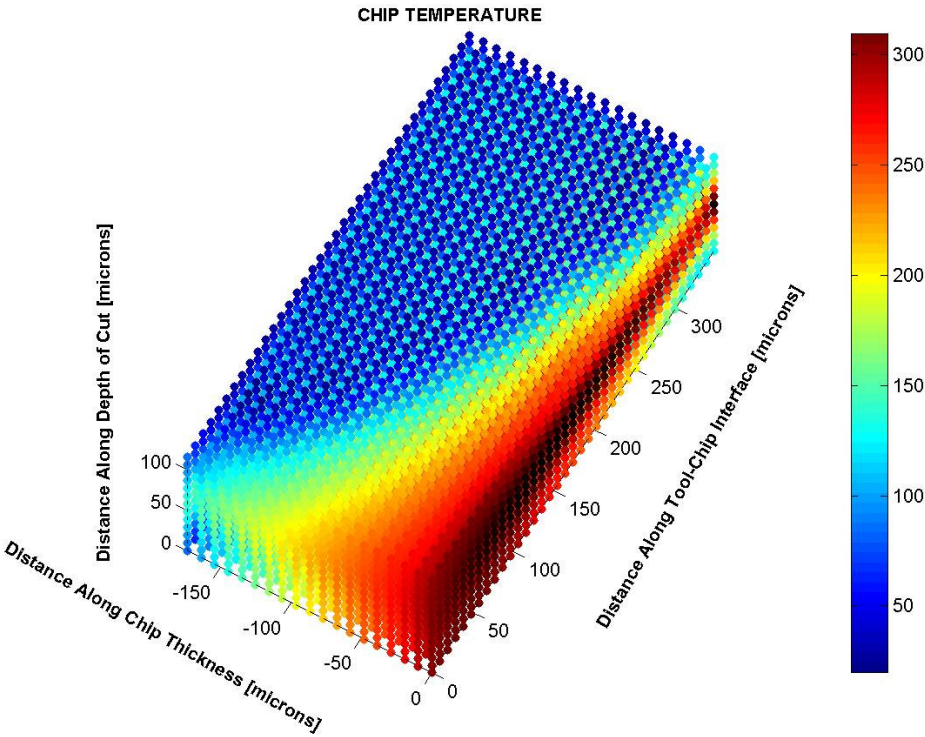


(d)

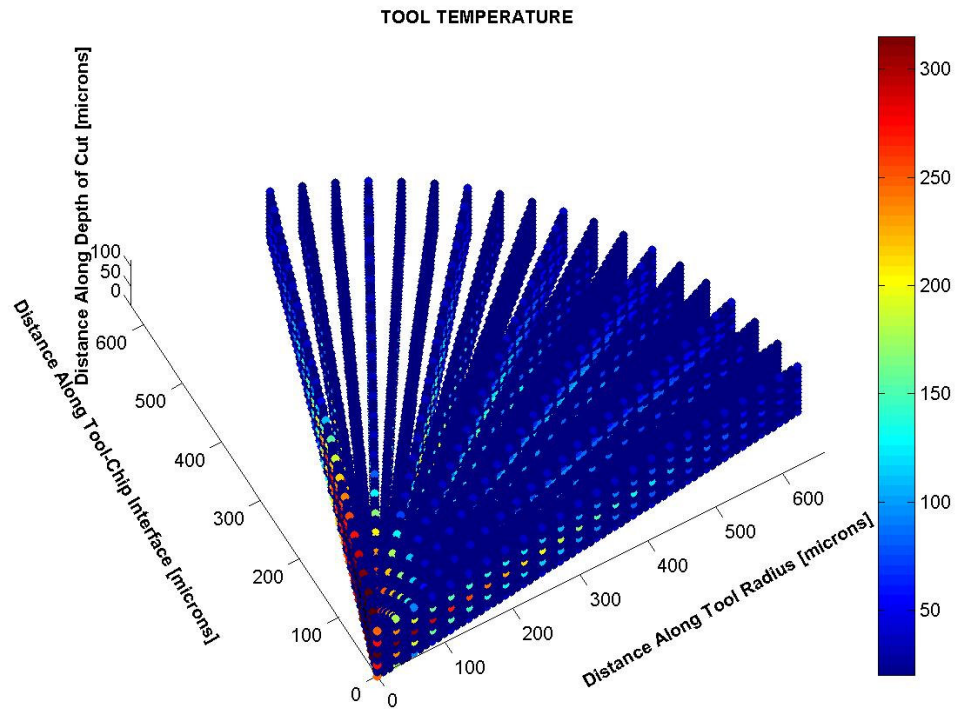
Fig. A.31: Validation for case 31. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



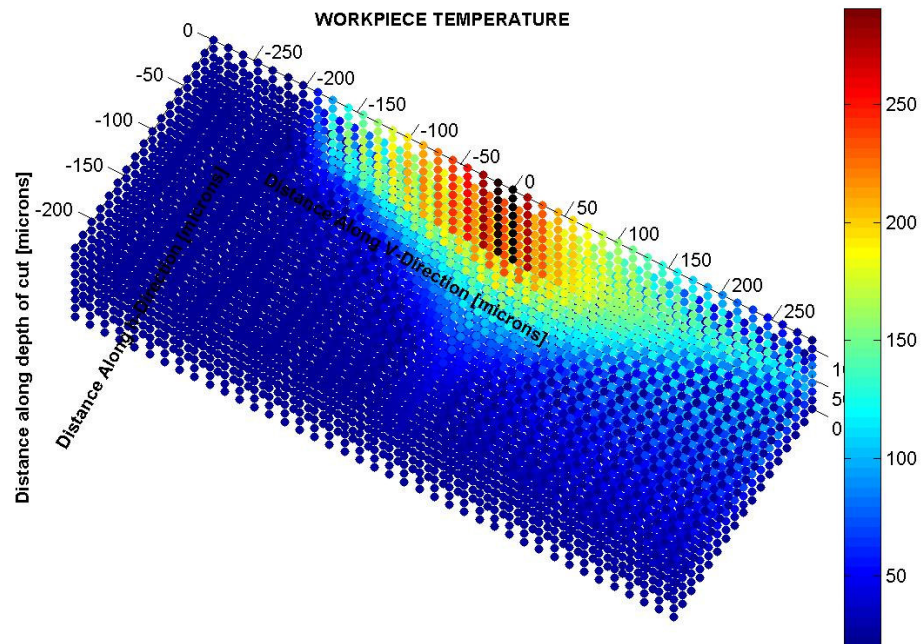
(a)



(b)

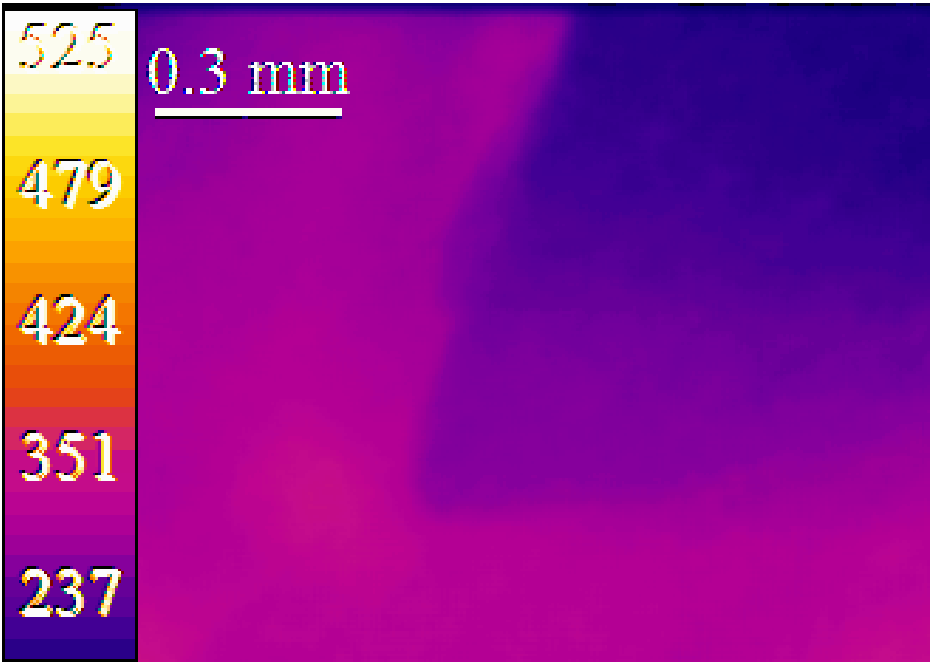


(c)

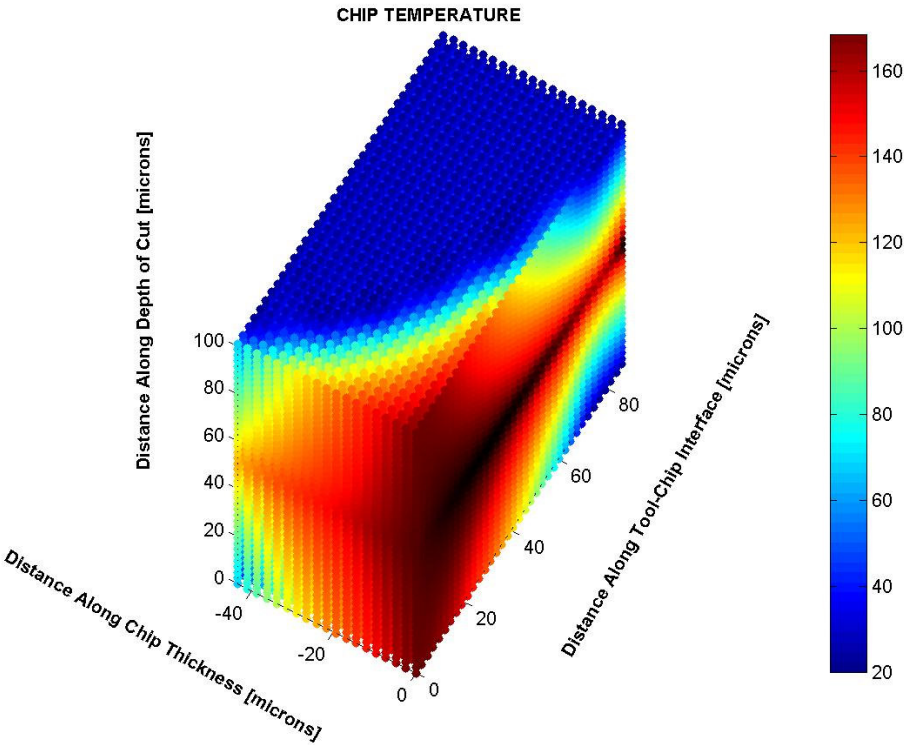


(d)

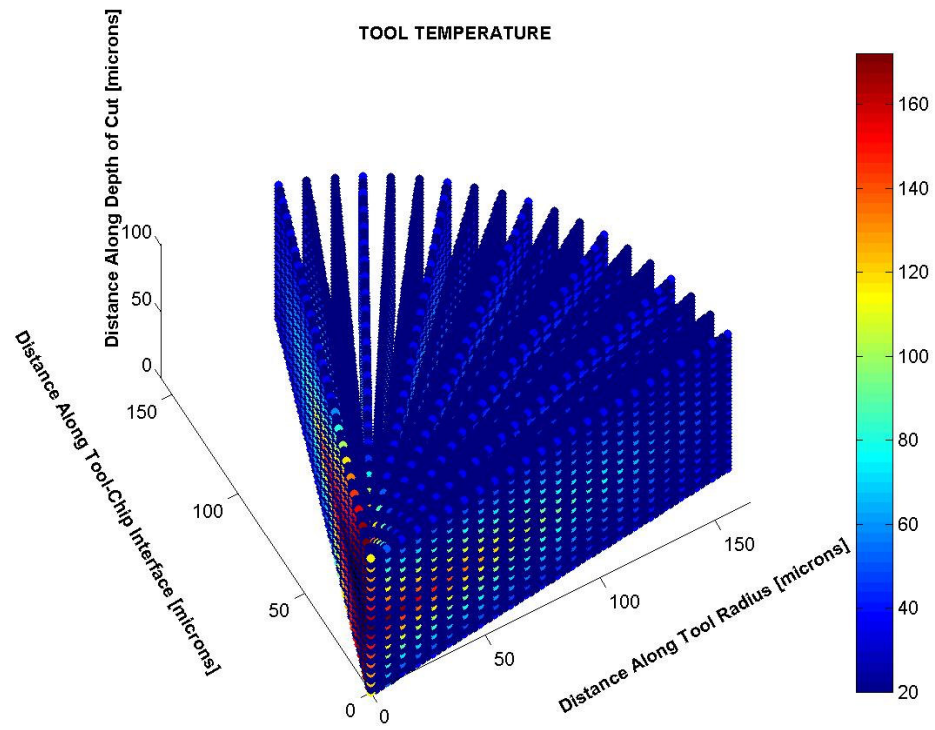
Fig. A.32: Validation for case 32. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



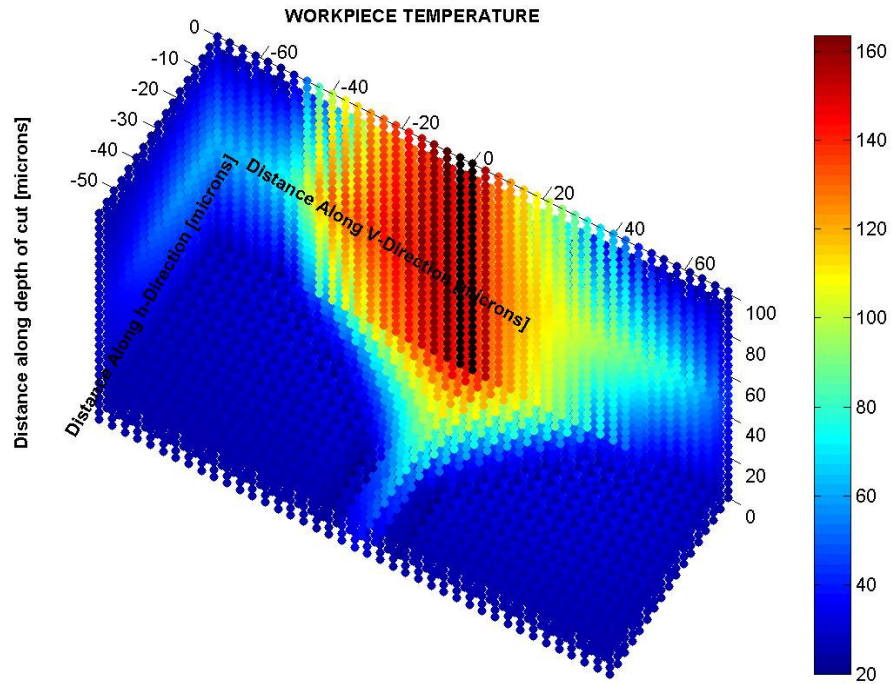
(a)



(b)

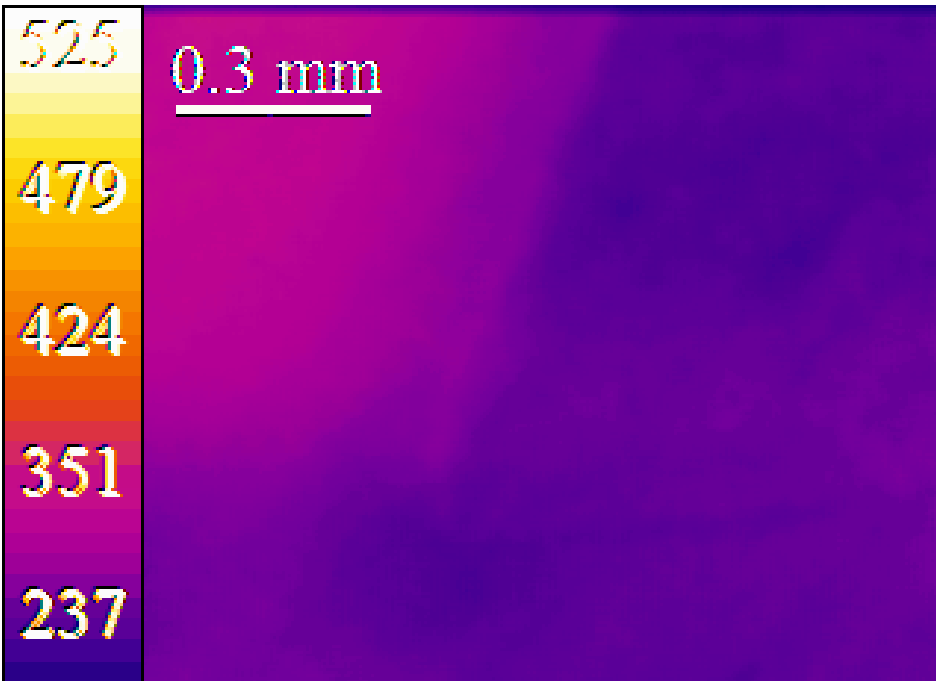


(c)

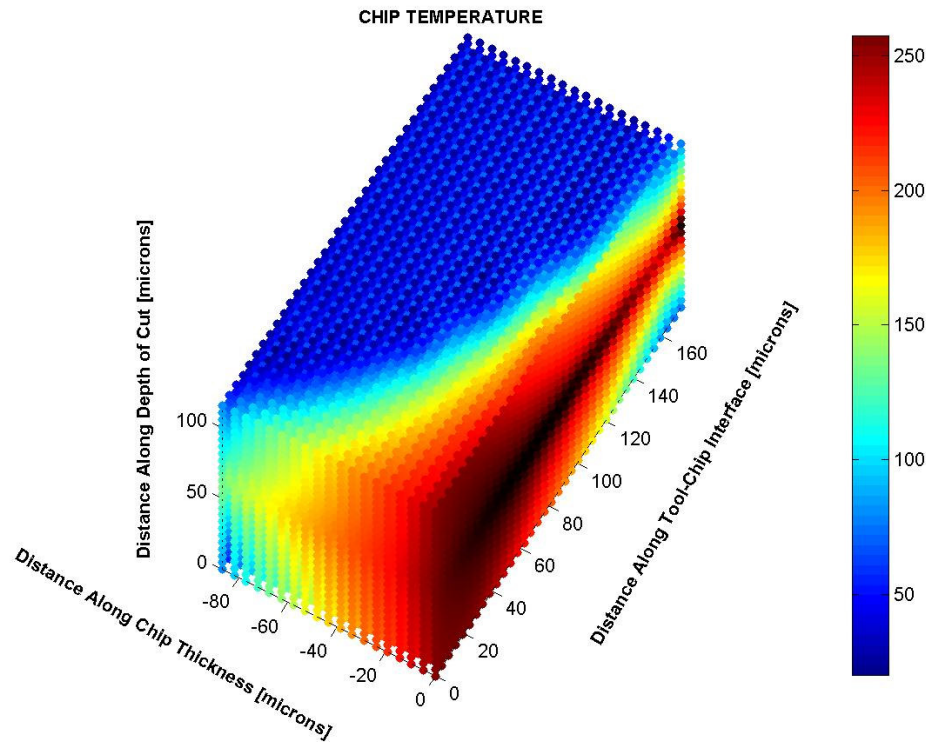


(d)

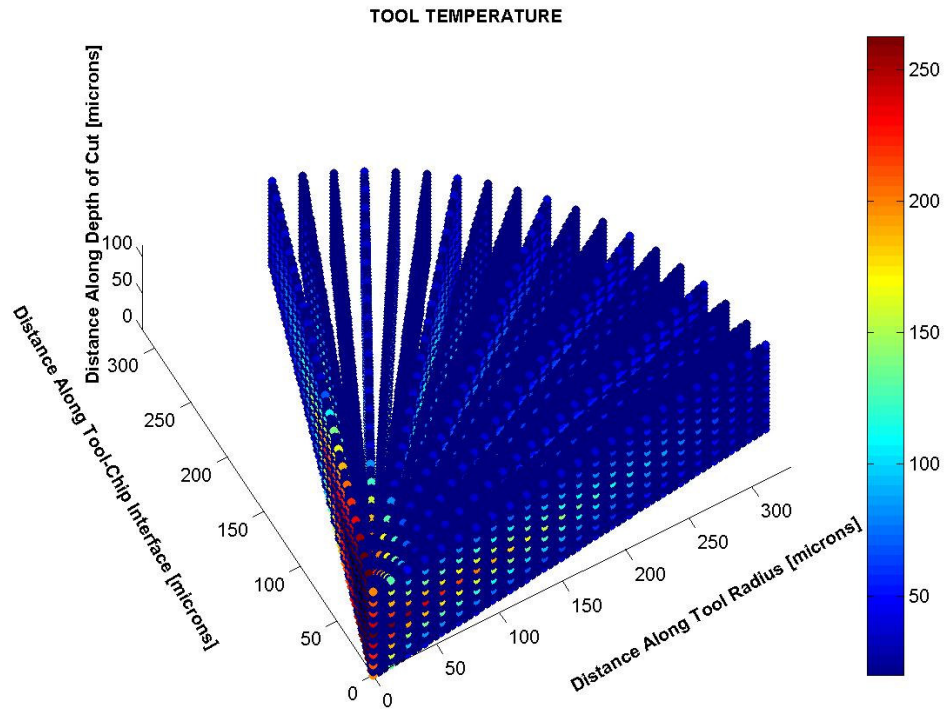
Fig. A.33: Validation for case 33. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



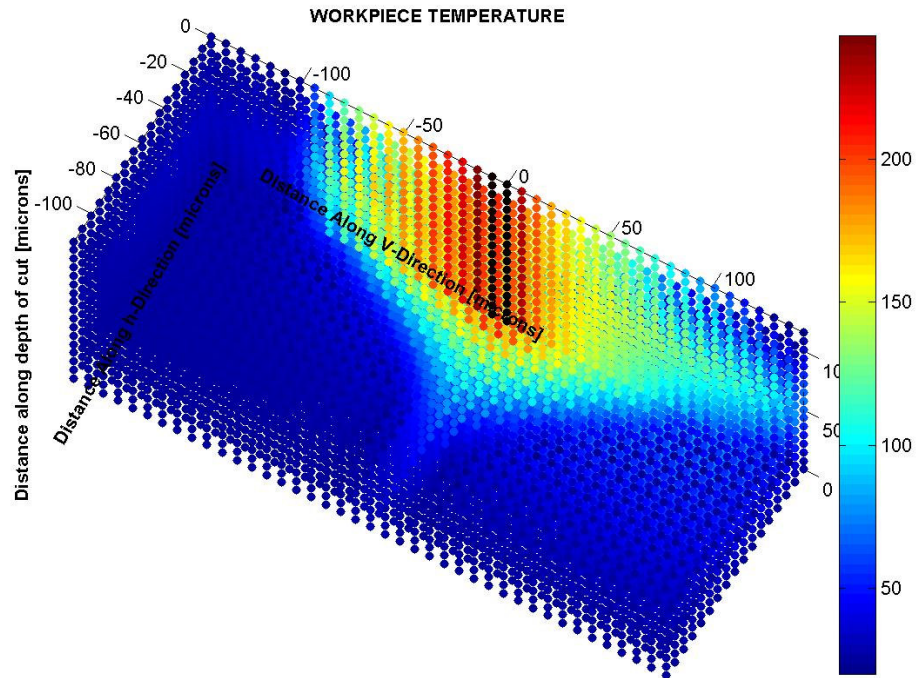
(a)



(b)

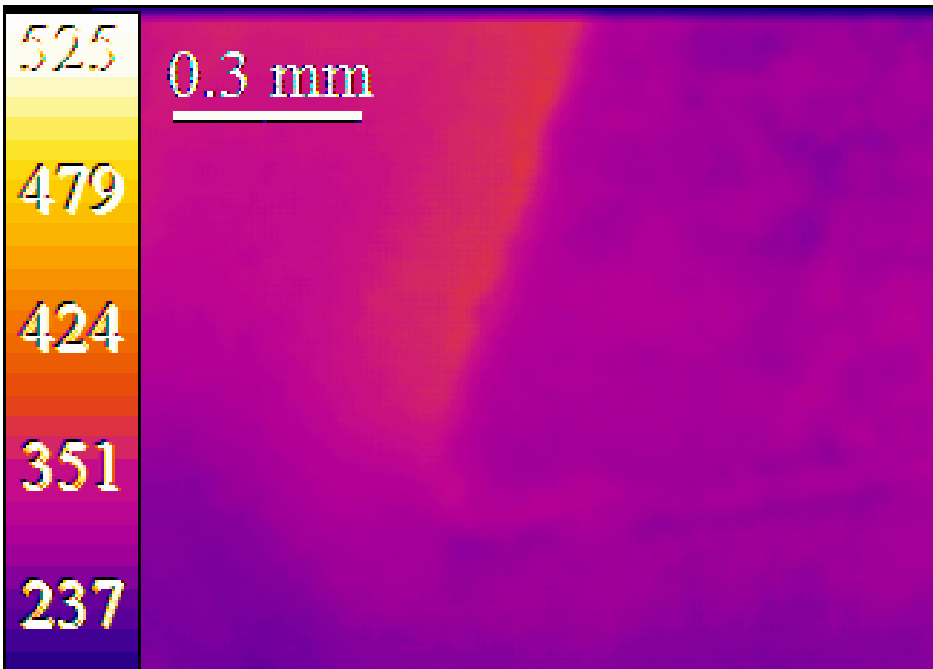


(c)

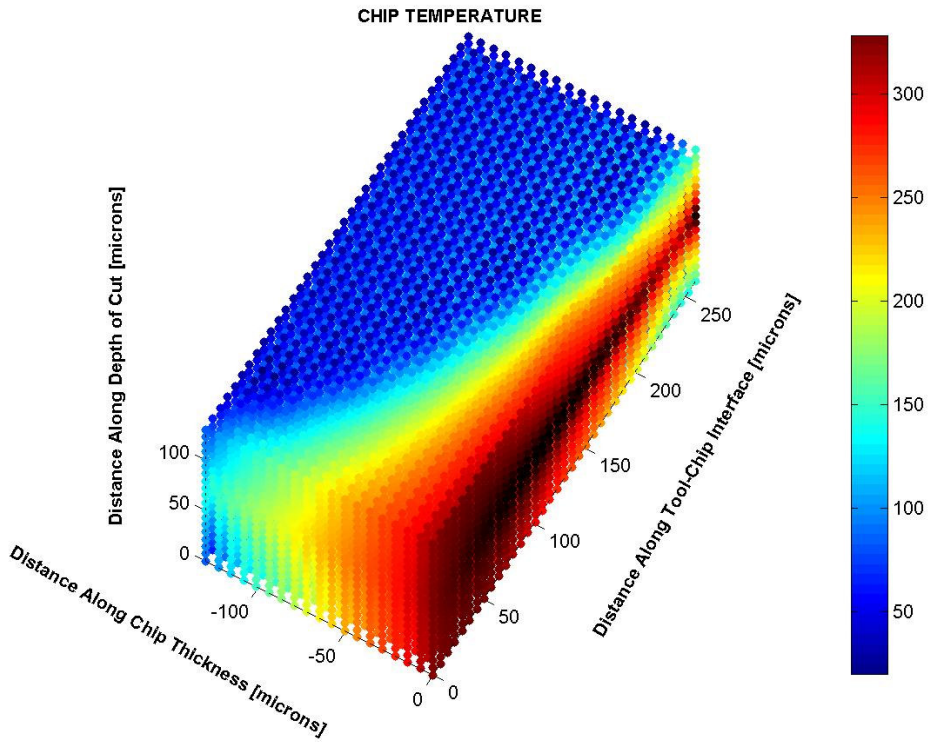


(d)

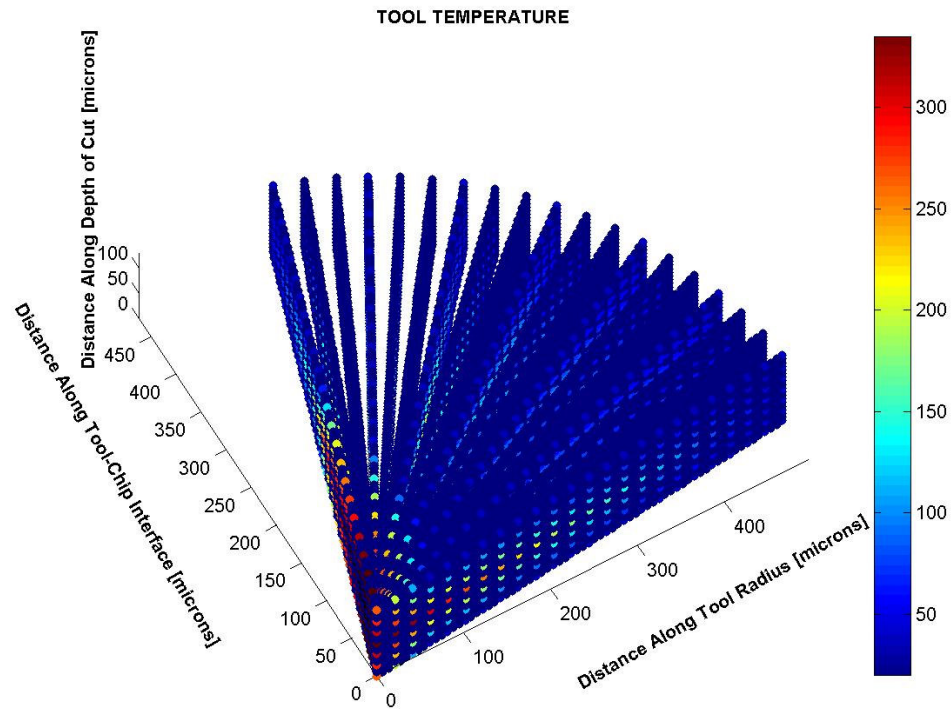
Fig. A.34: Validation for case 34. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



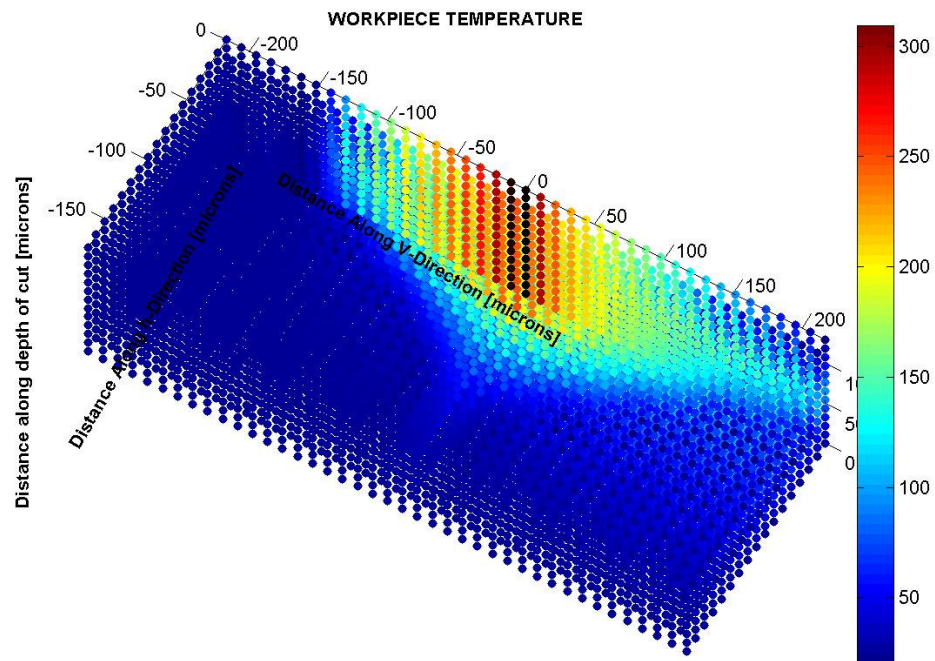
(a)



(b)

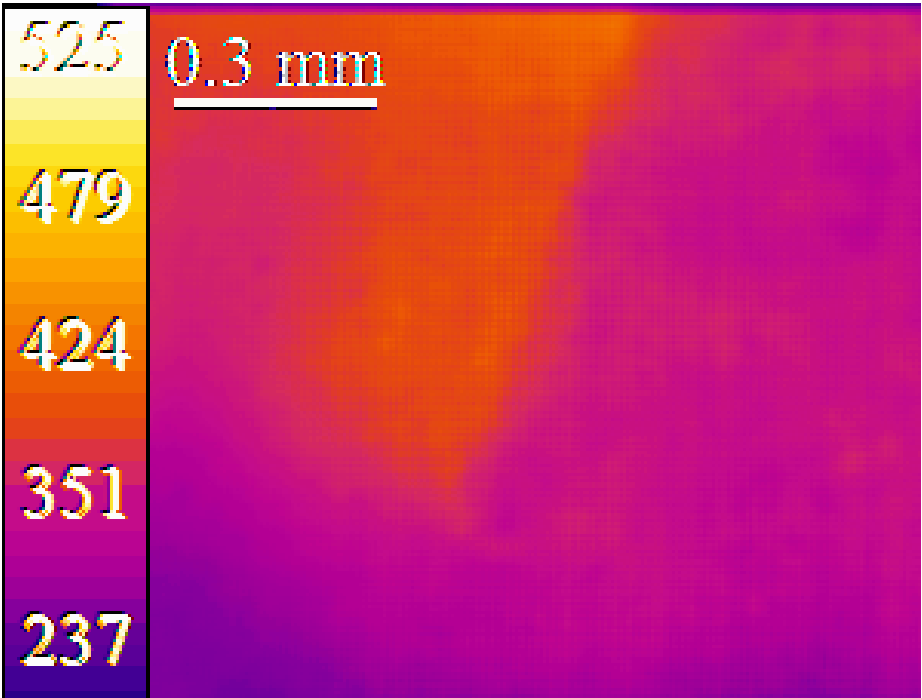


(c)

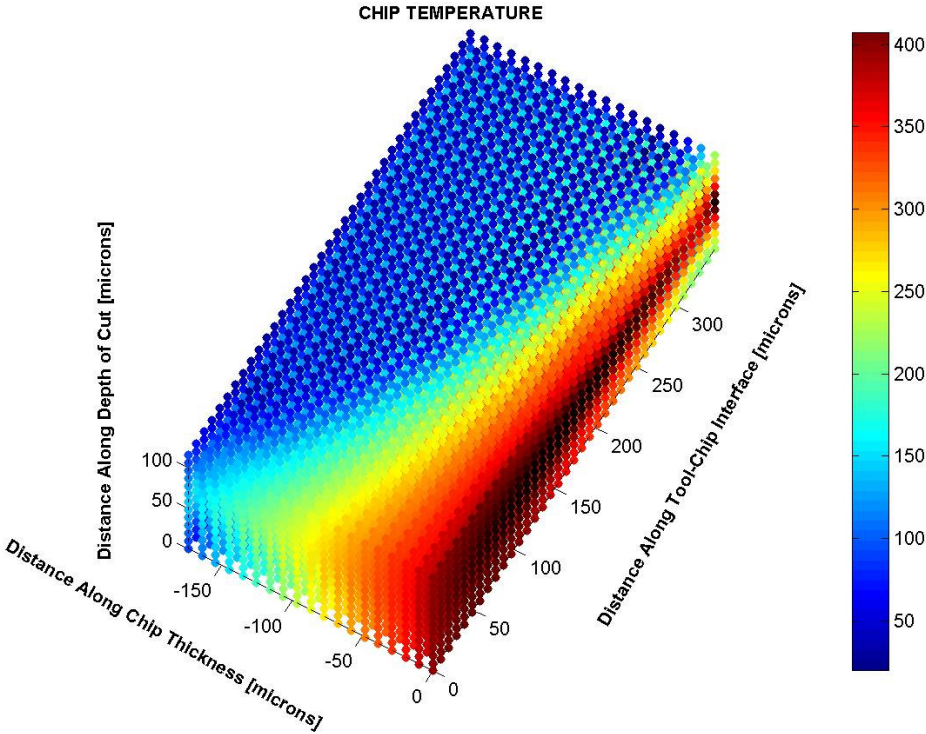


(d)

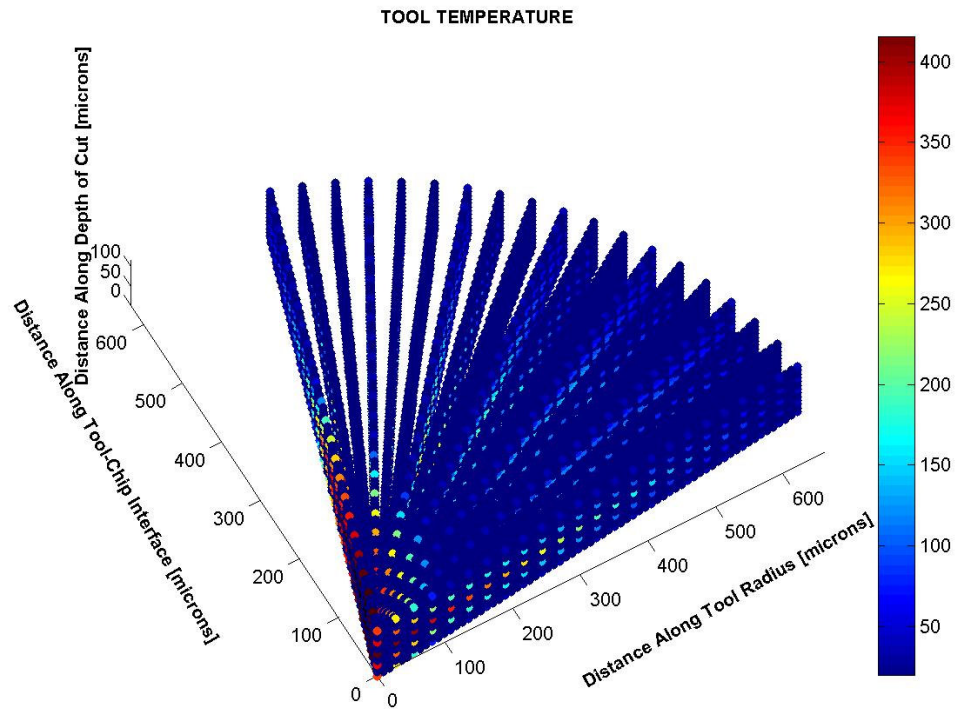
Fig. A.35: Validation for case 35. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



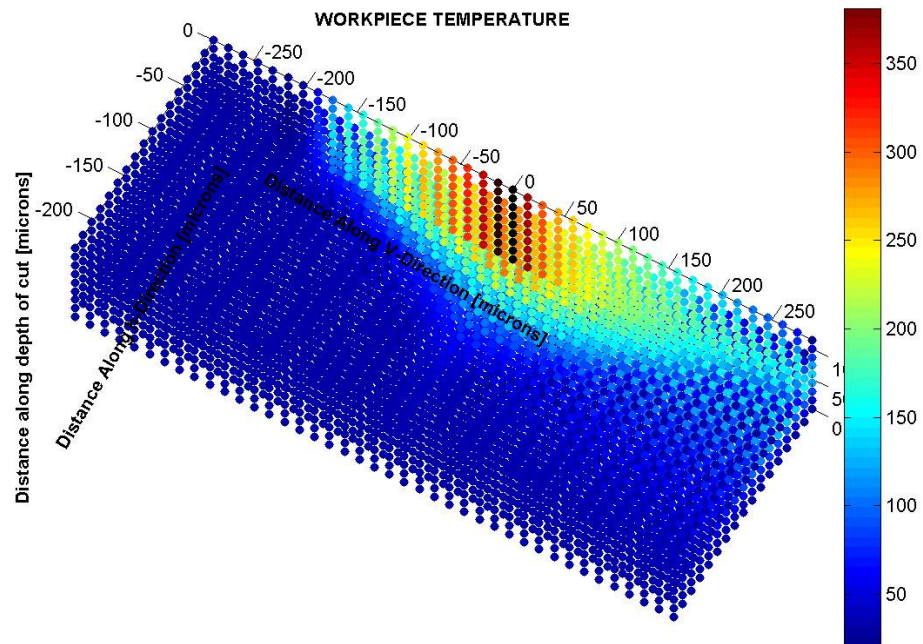
(a)



(b)

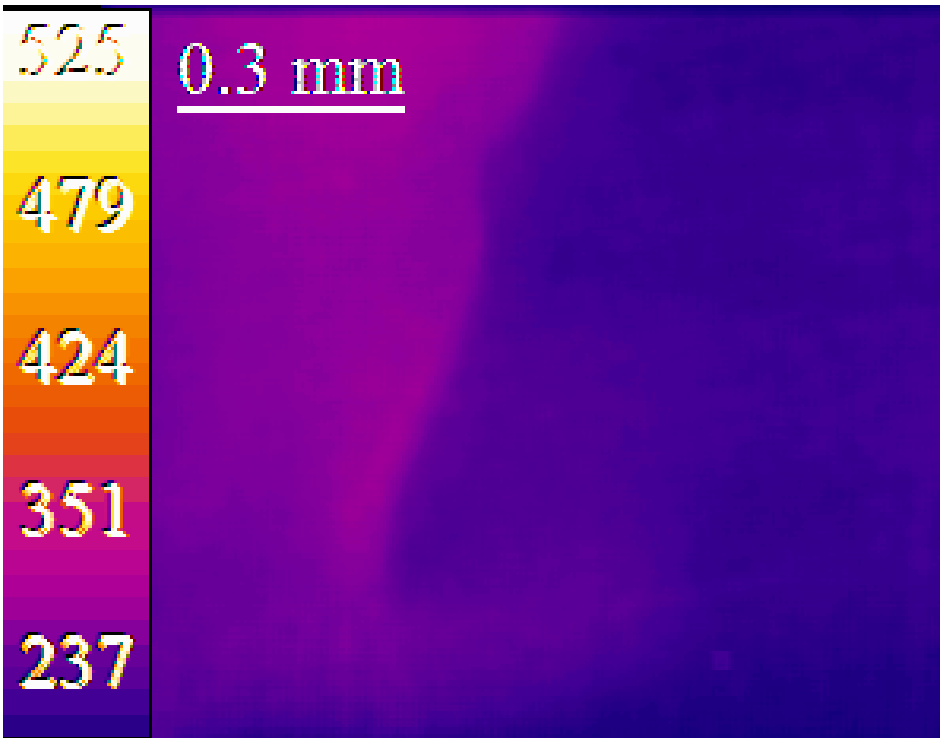


(c)

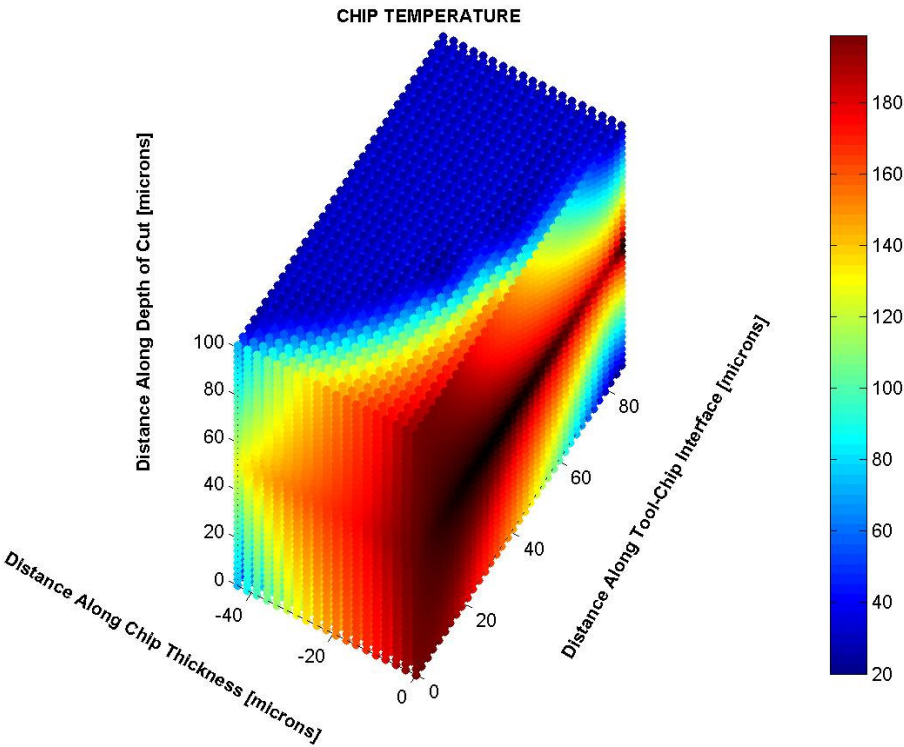


(d)

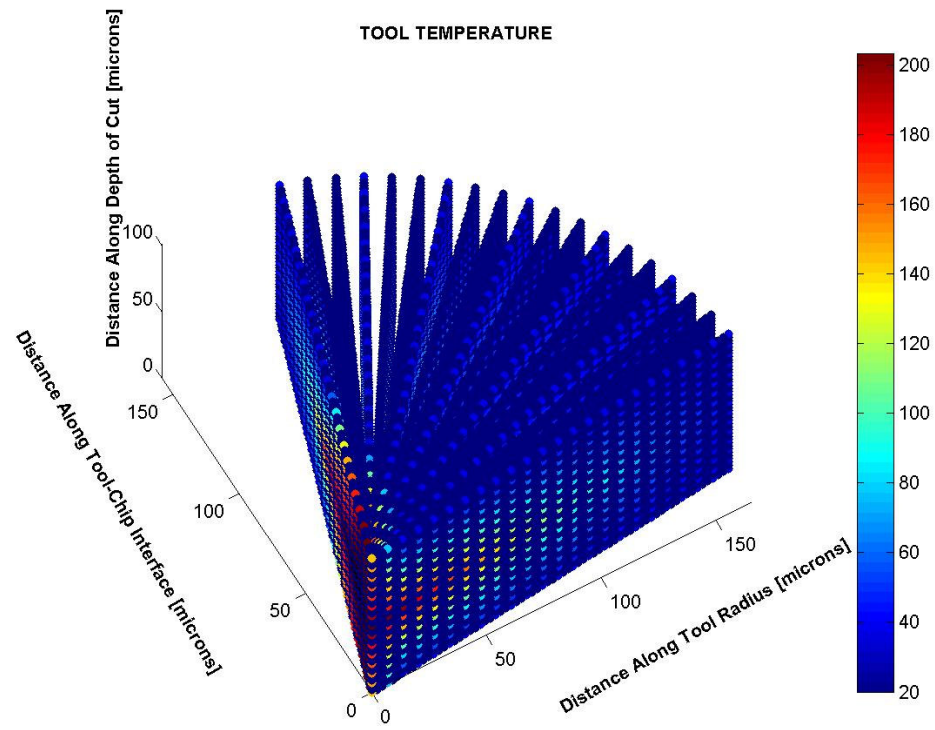
Fig. A.36: Validation for case 36. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



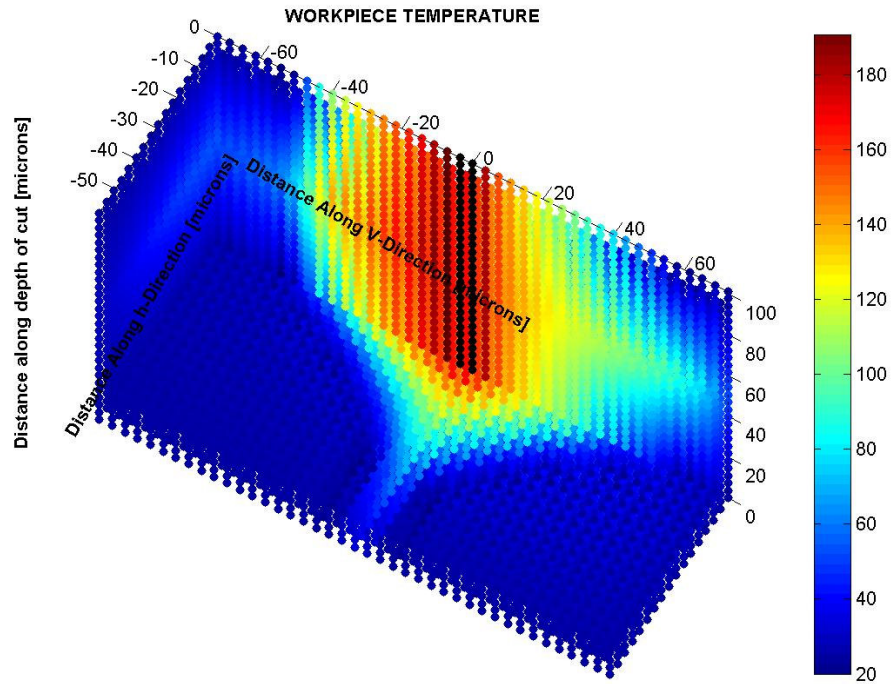
(a)



(b)

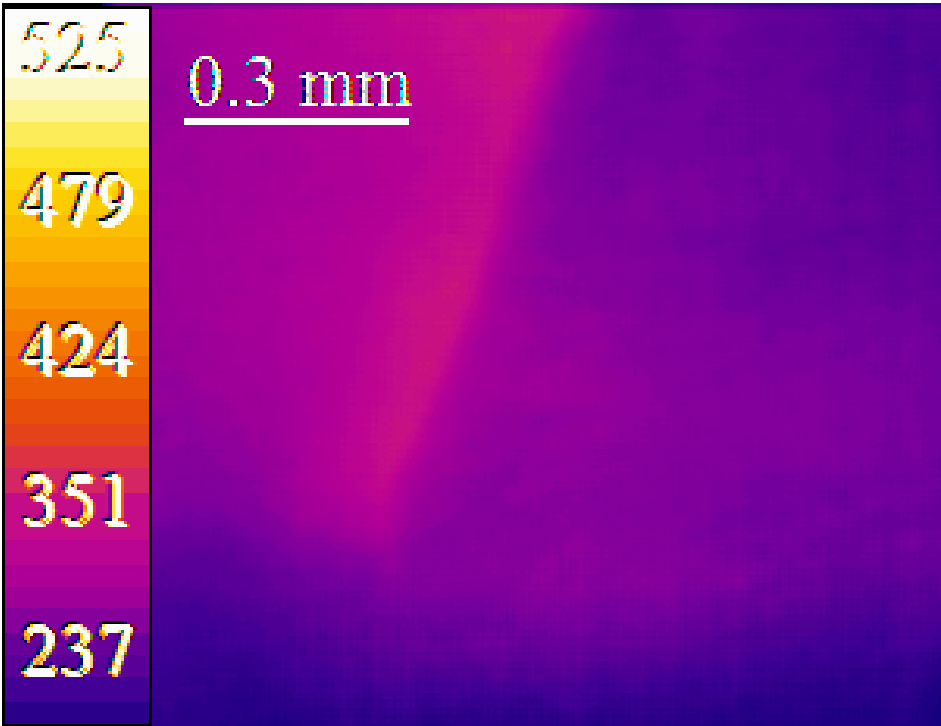


(c)

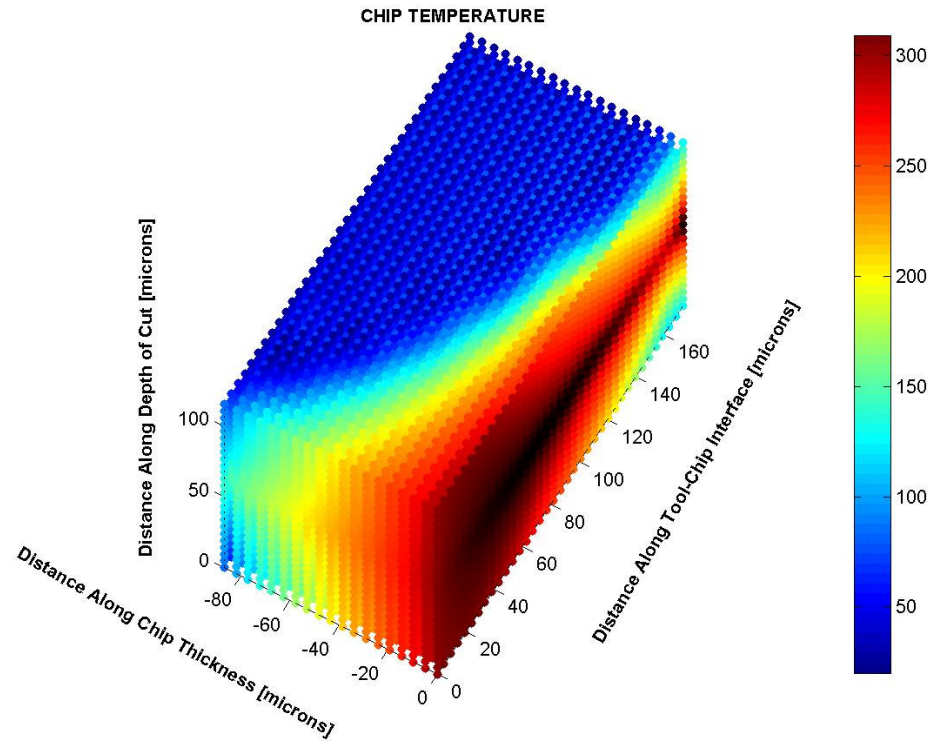


(d)

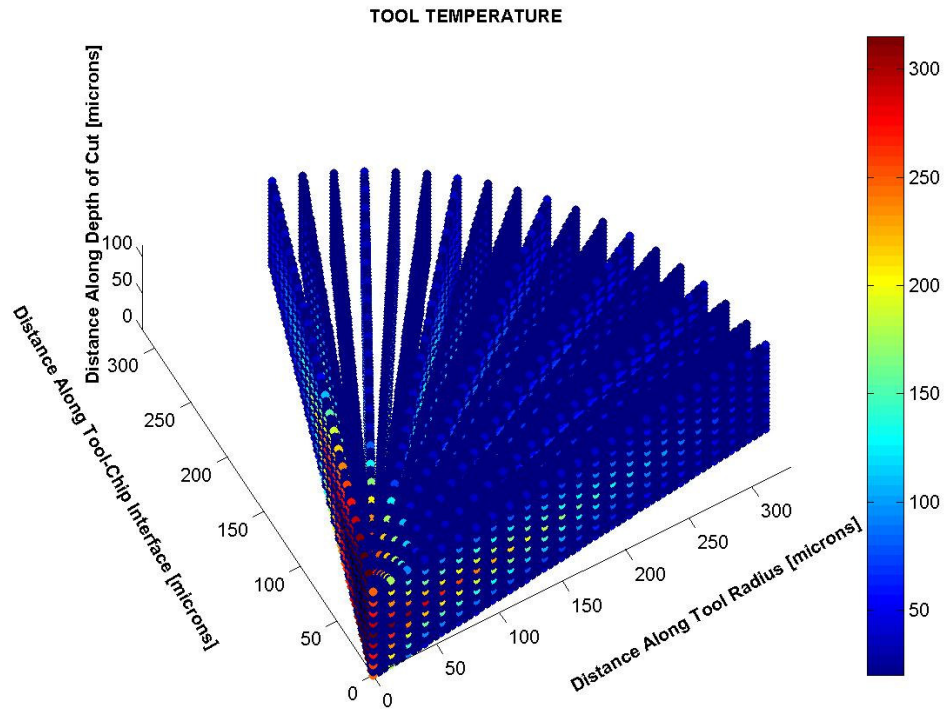
Fig. A.37: Validation for case 37. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



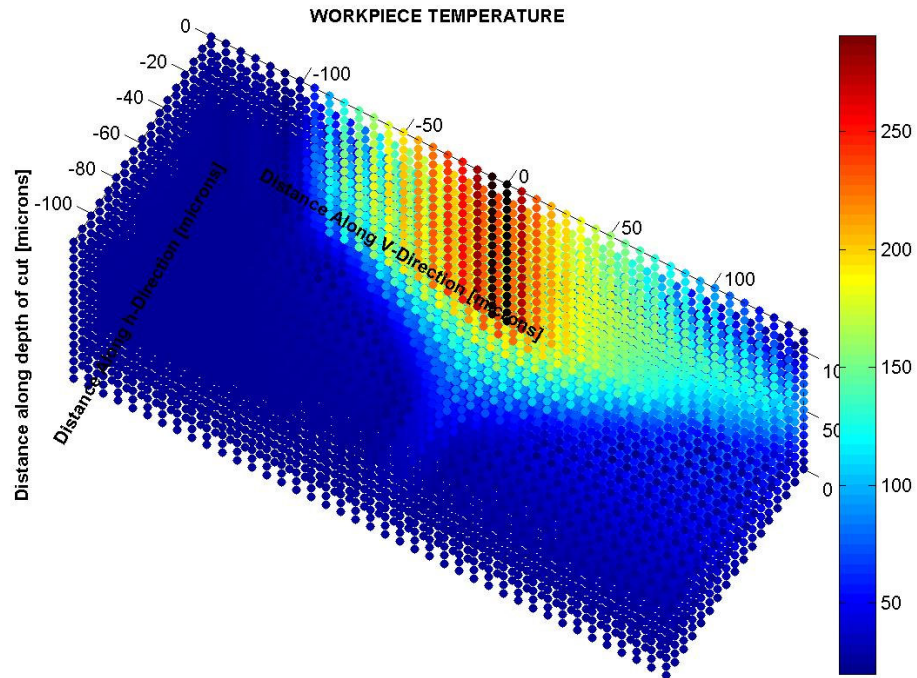
(a)



(b)

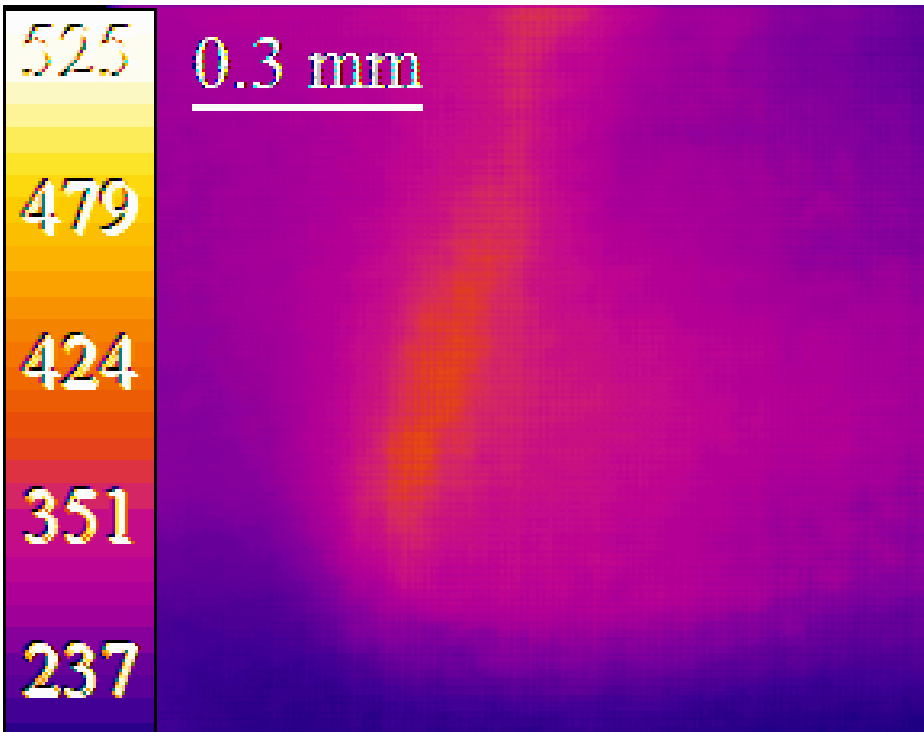


(c)

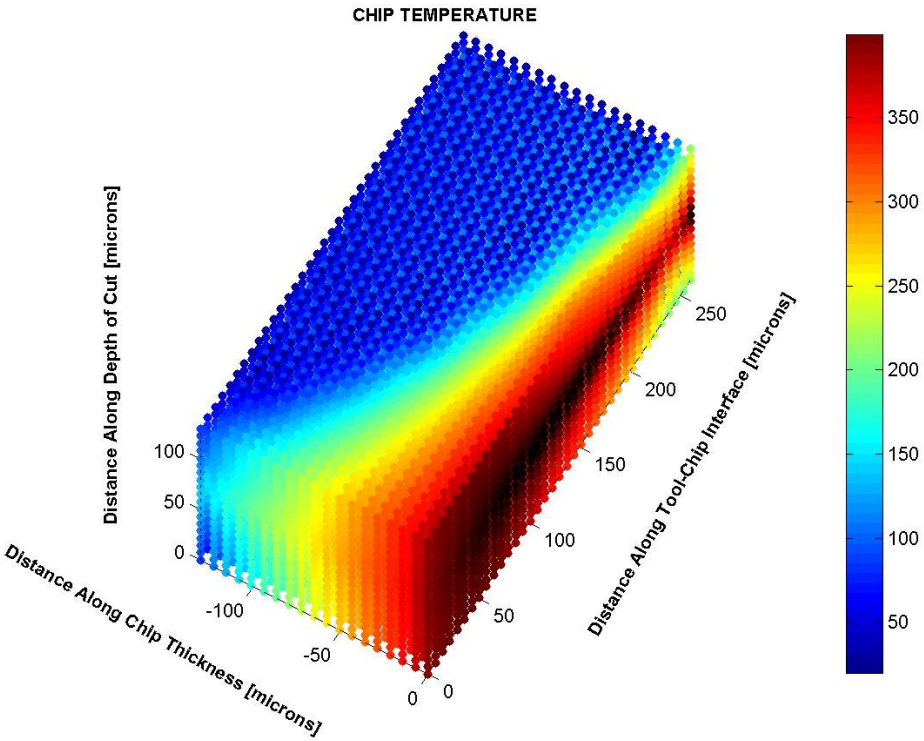


(d)

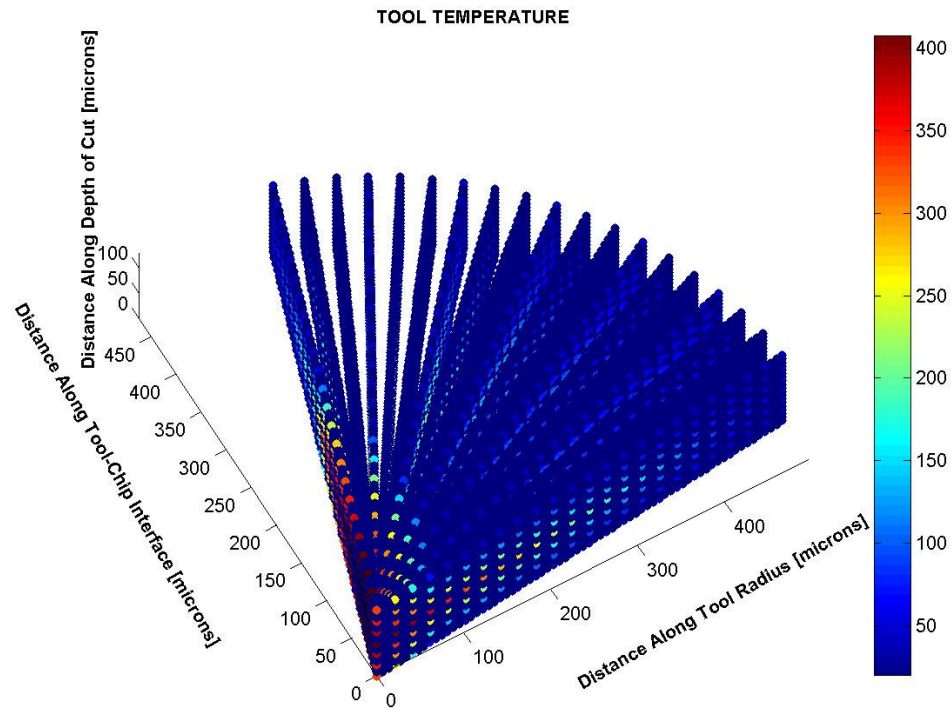
Fig. A.38: Validation for case 38. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



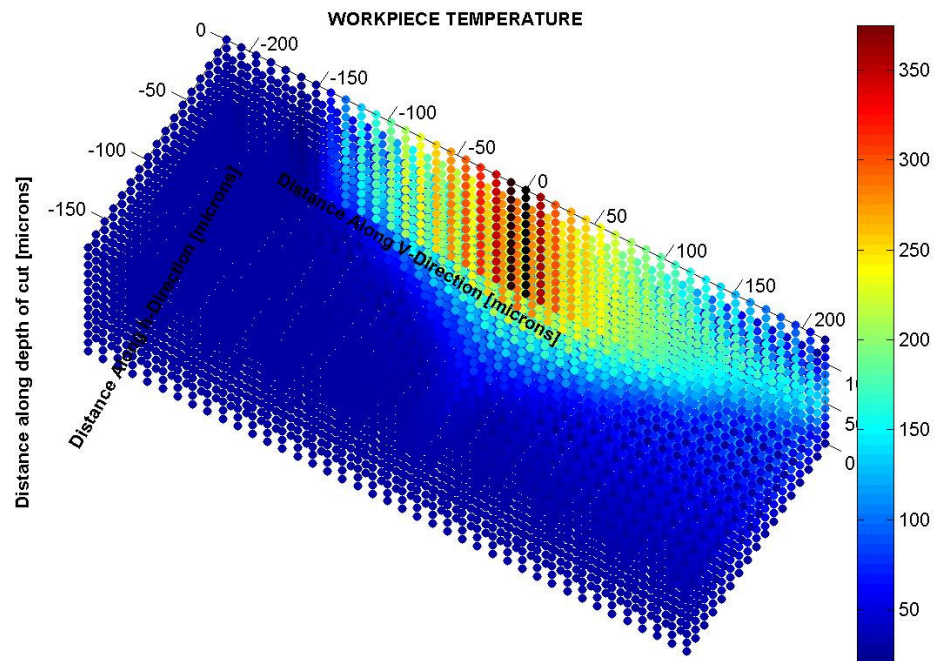
(a)



(b)

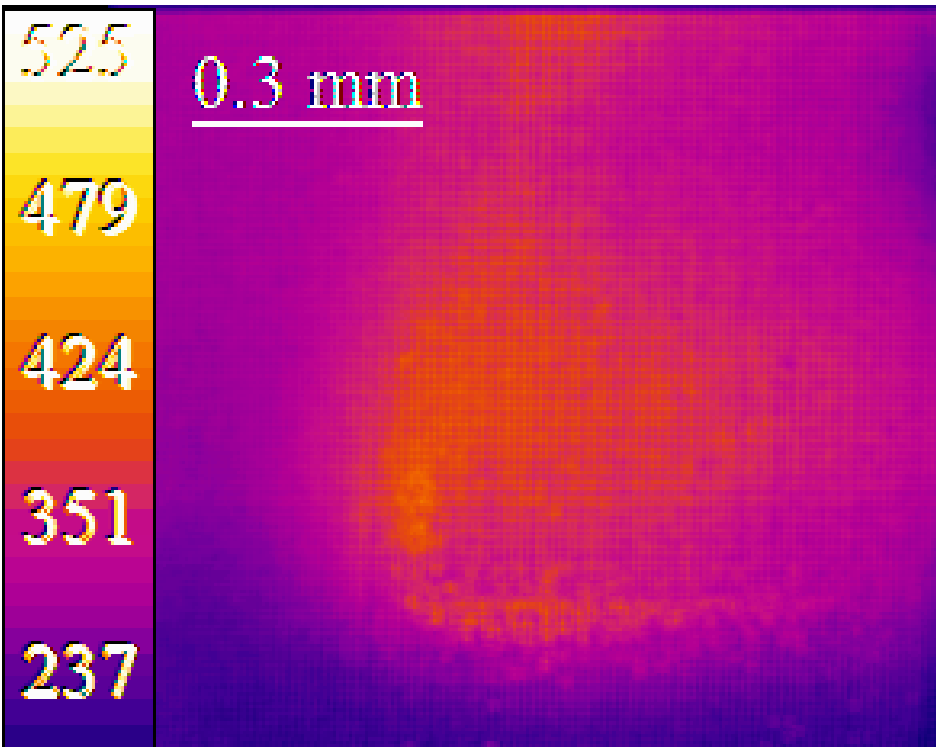


(c)

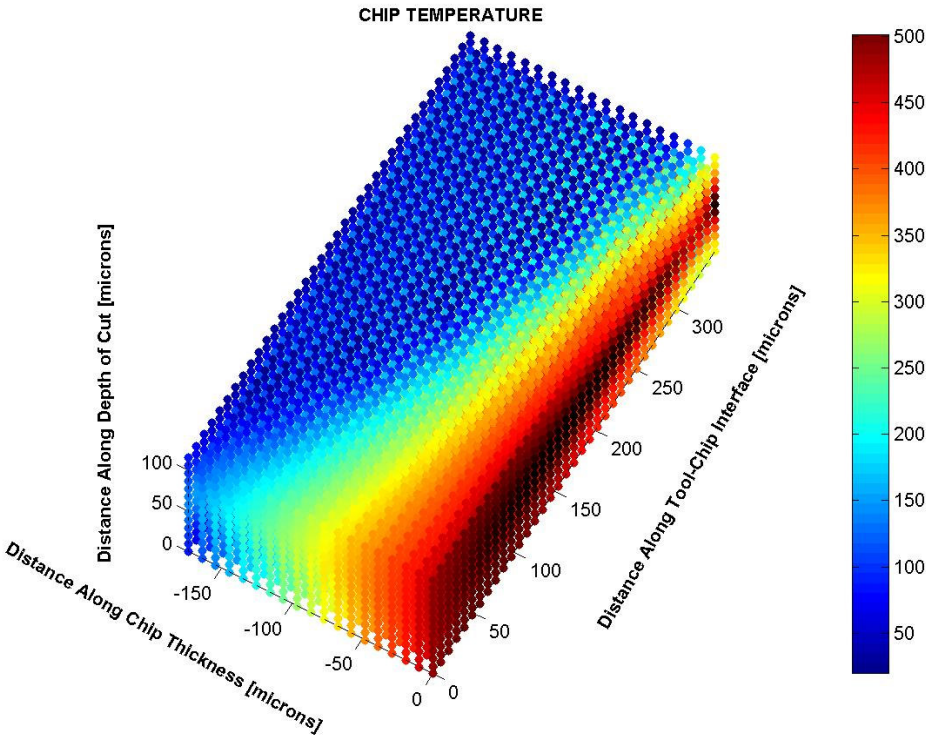


(d)

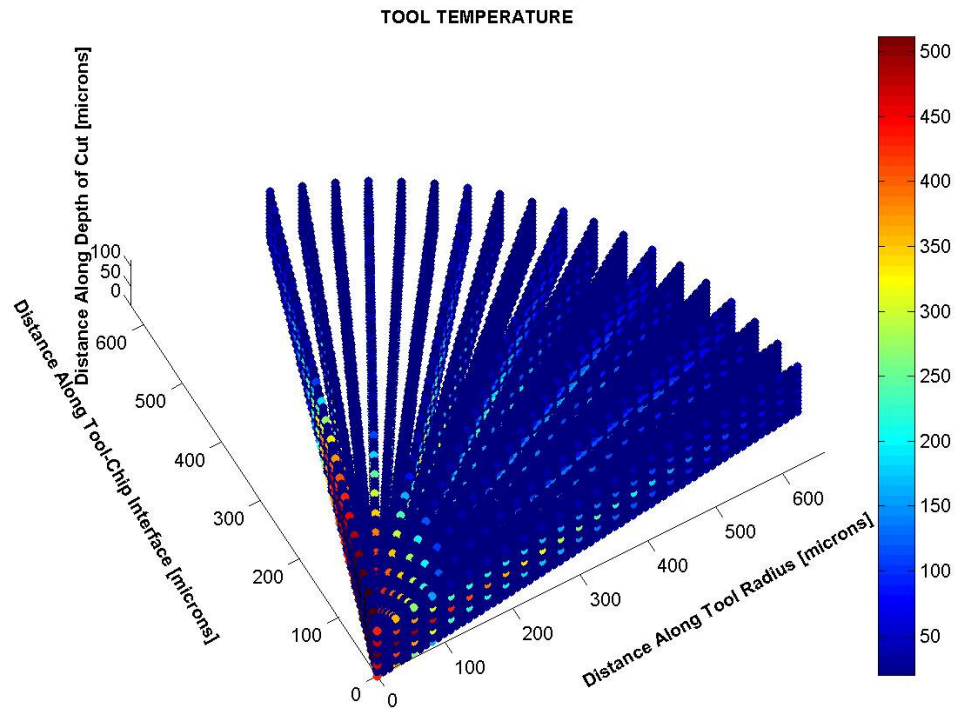
Fig. A.39: Validation for case 39. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



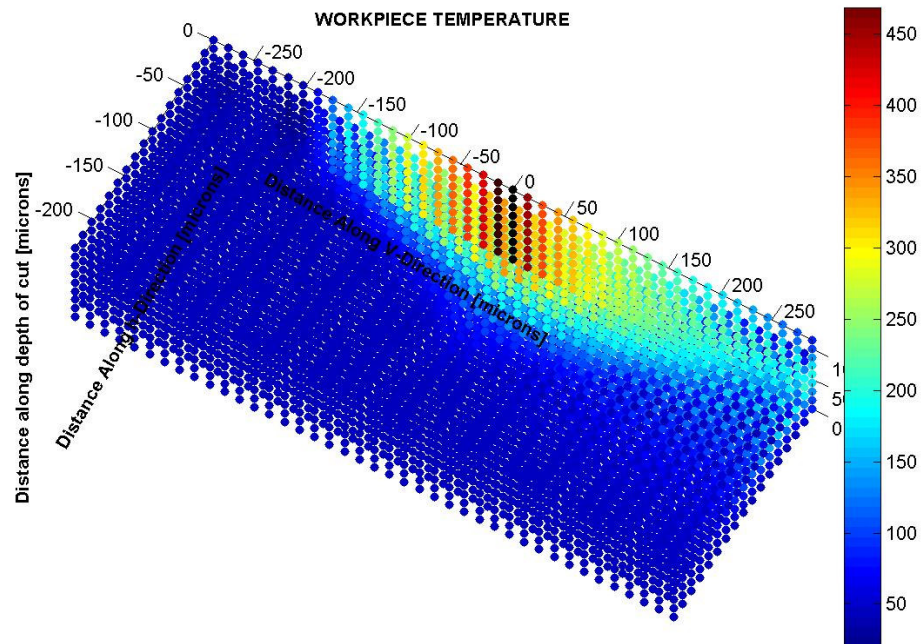
(a)



(b)

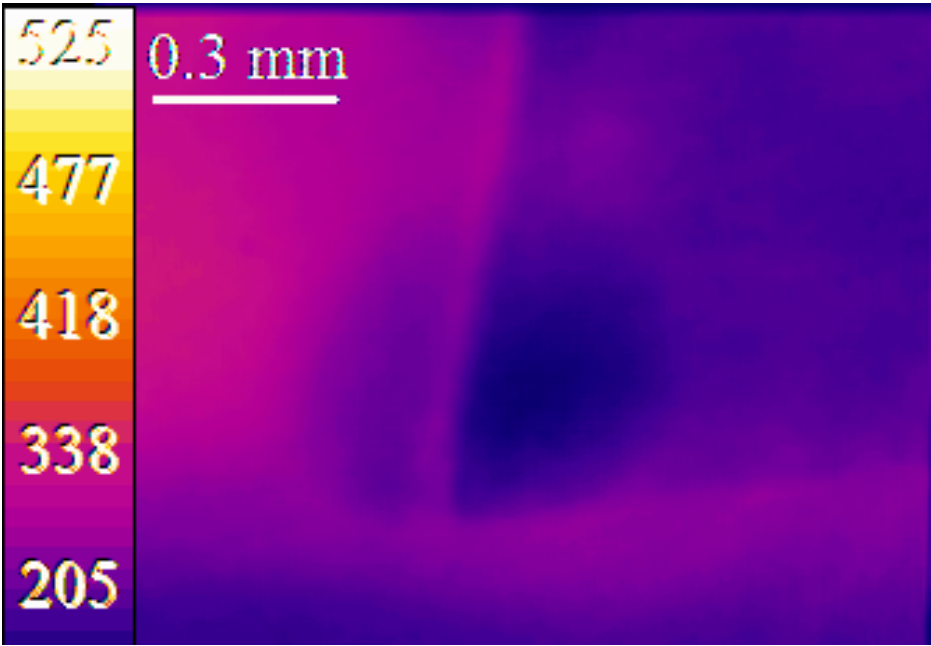


(c)

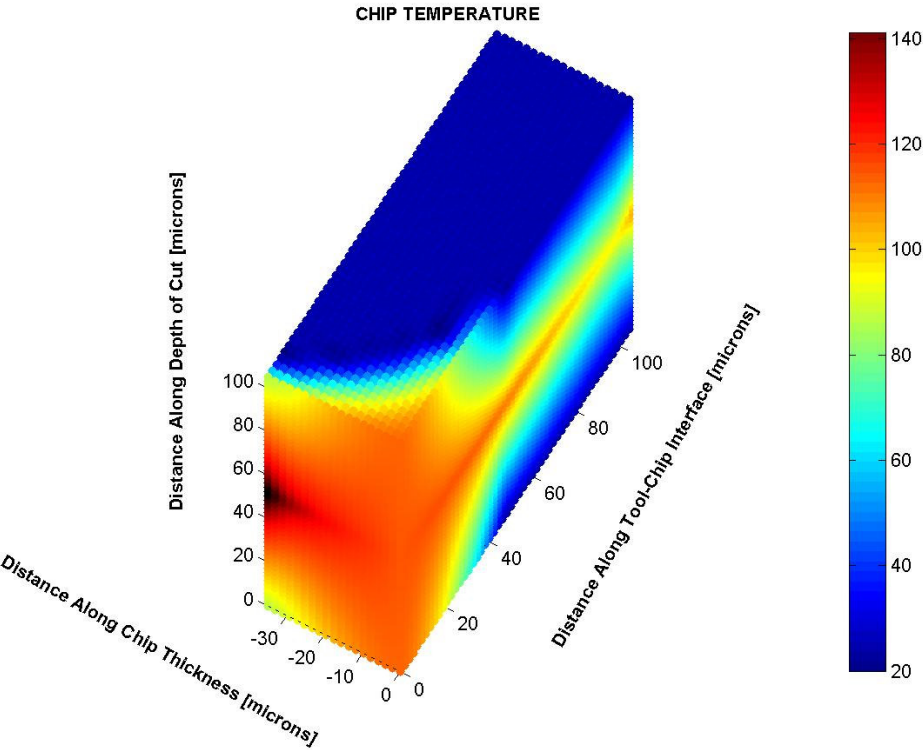


(d)

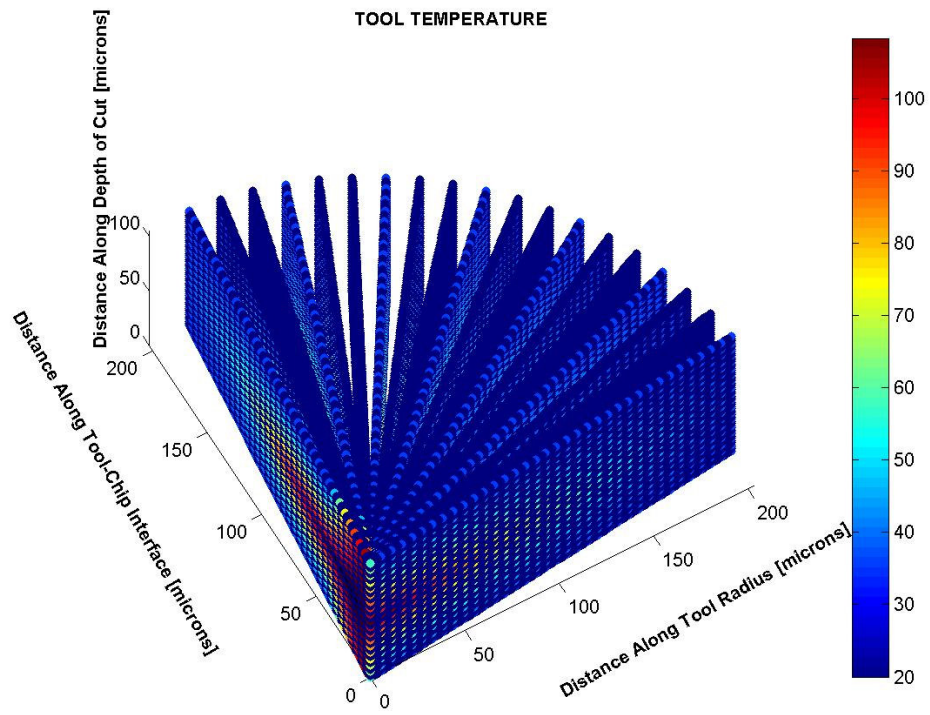
Fig. A.40: Validation for case 40. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



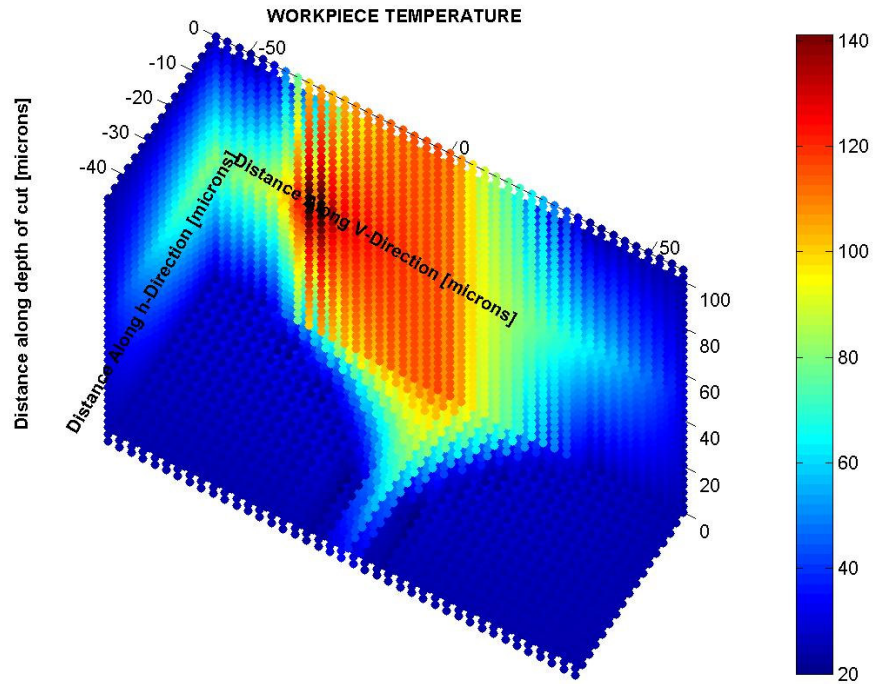
(a)



(b)

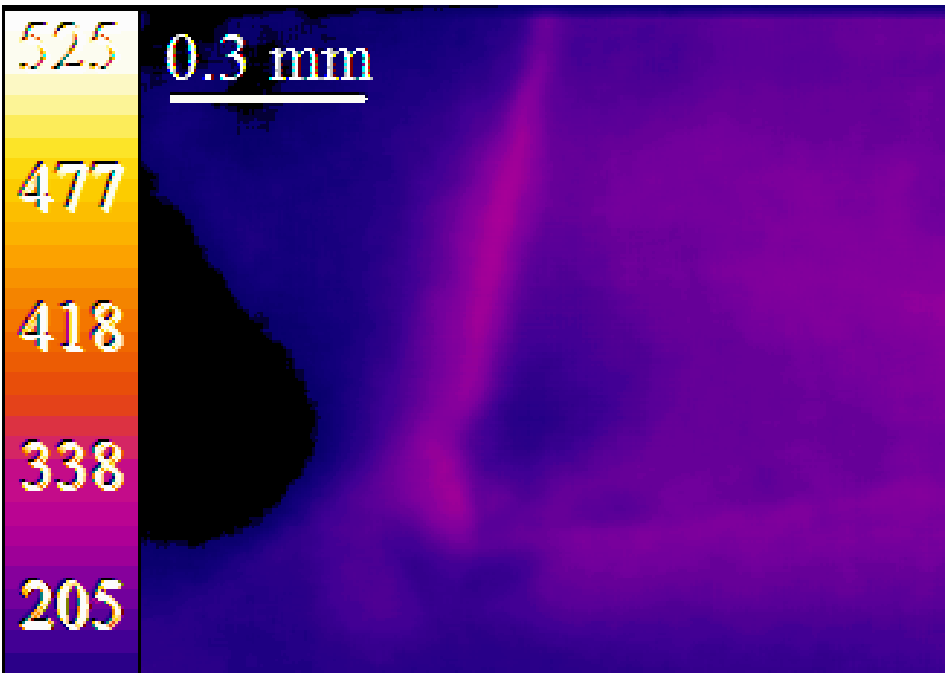


(c)

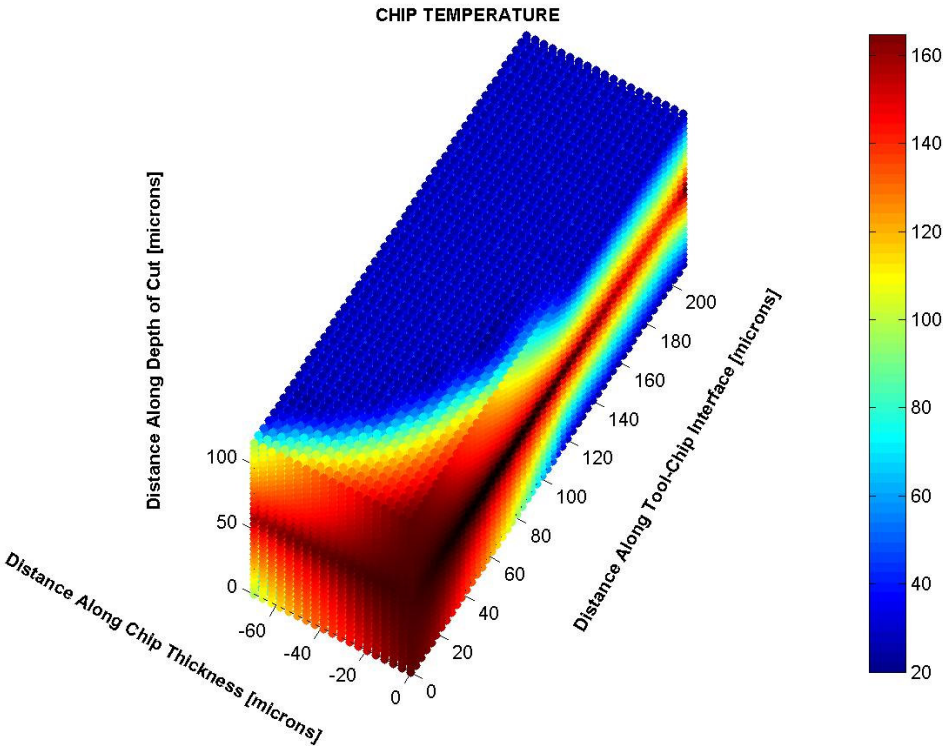


(d)

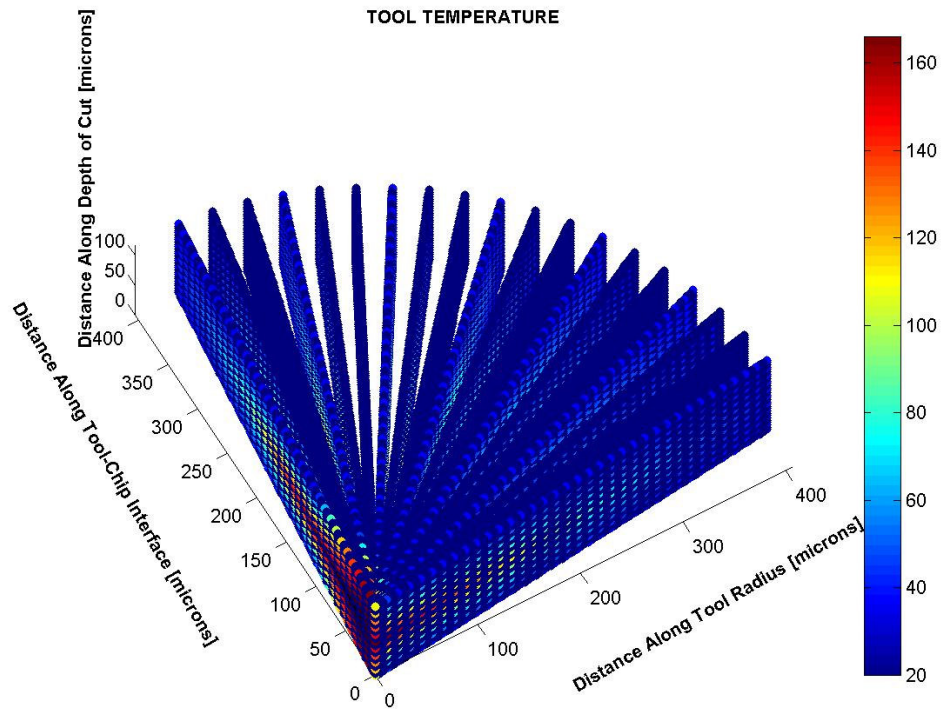
Fig. A.41: Validation for case 41. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



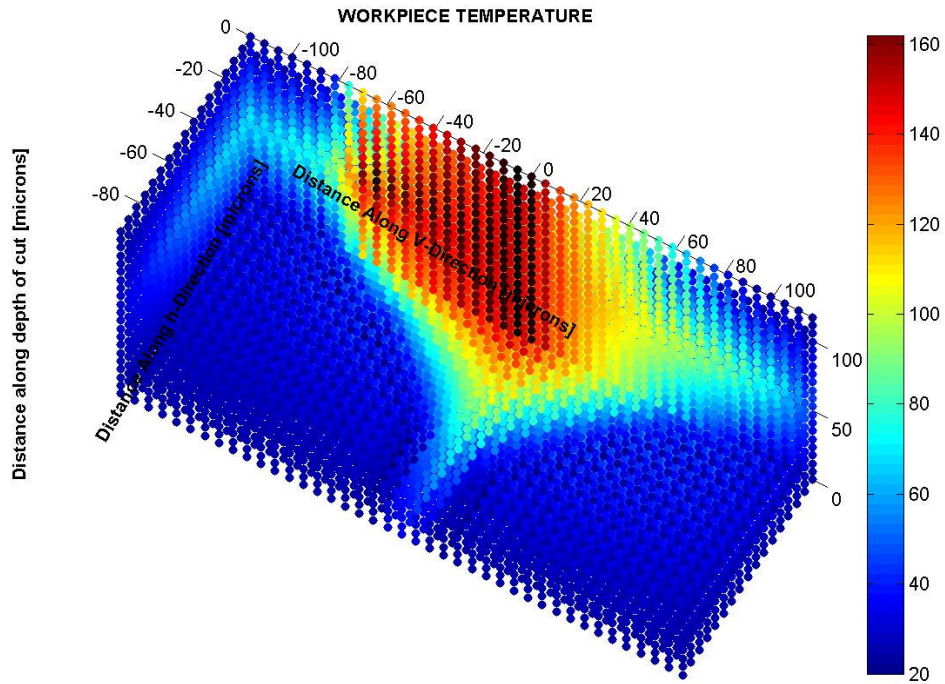
(a)



(b)

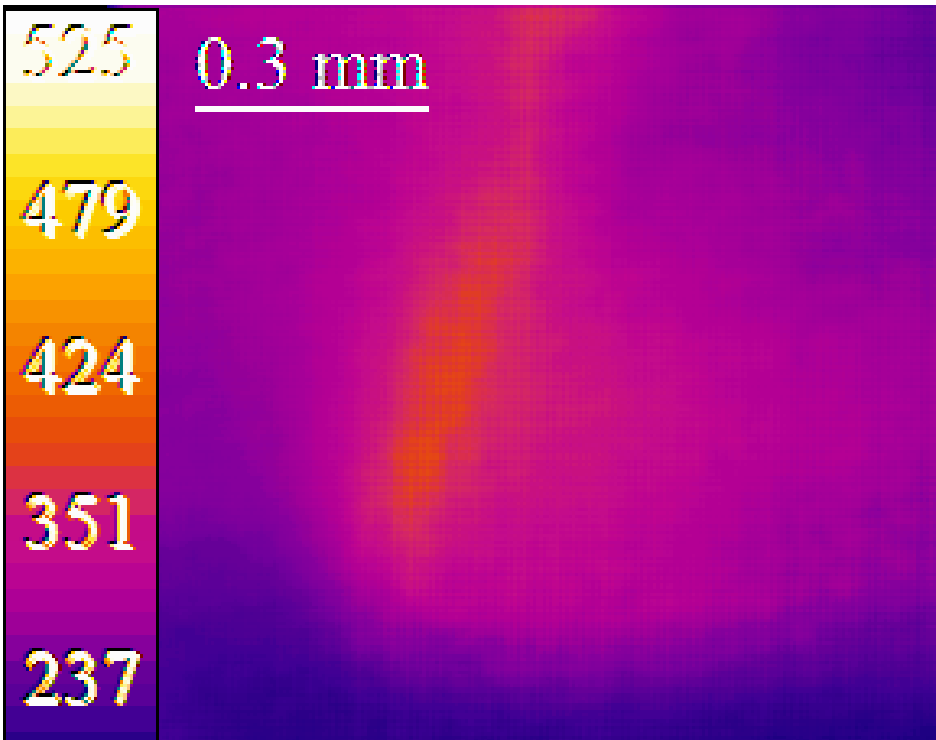


(c)

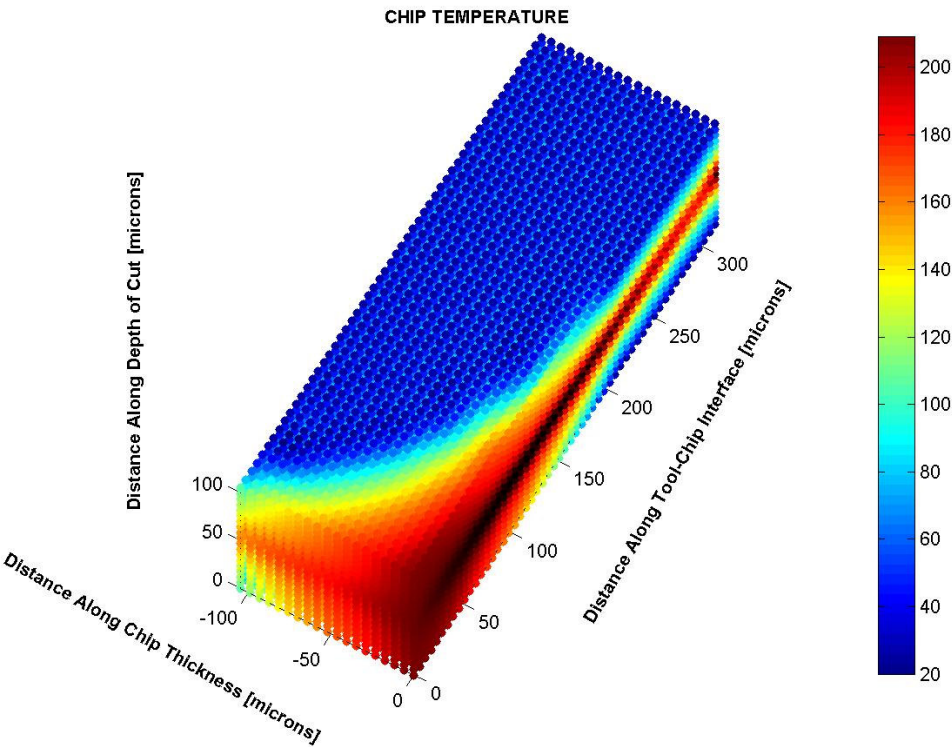


(d)

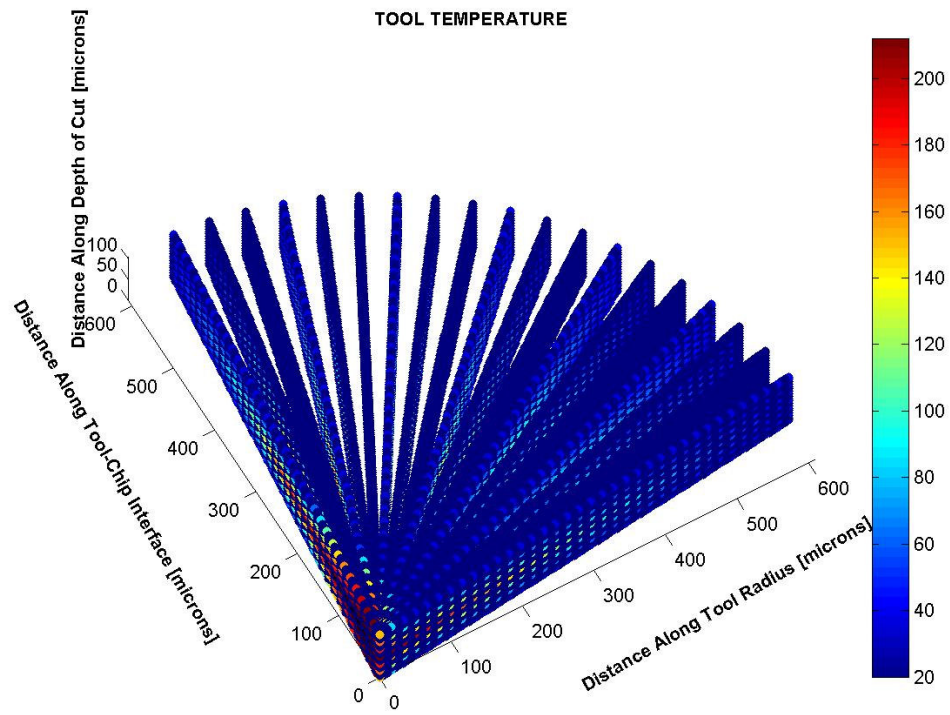
Fig. A.42: Validation for case 42. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



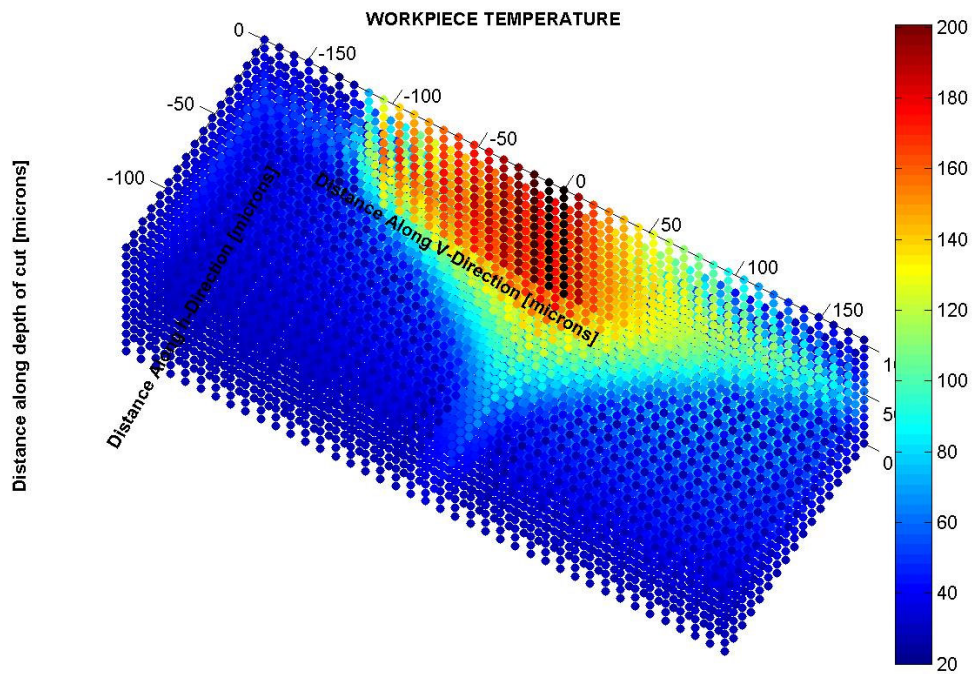
(a)



(b)

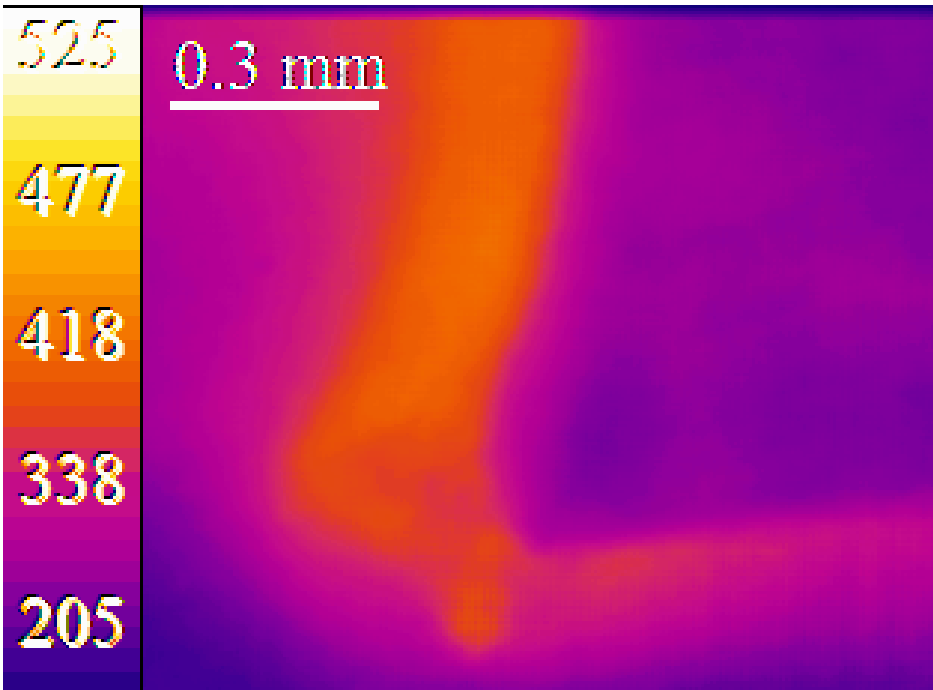


(c)

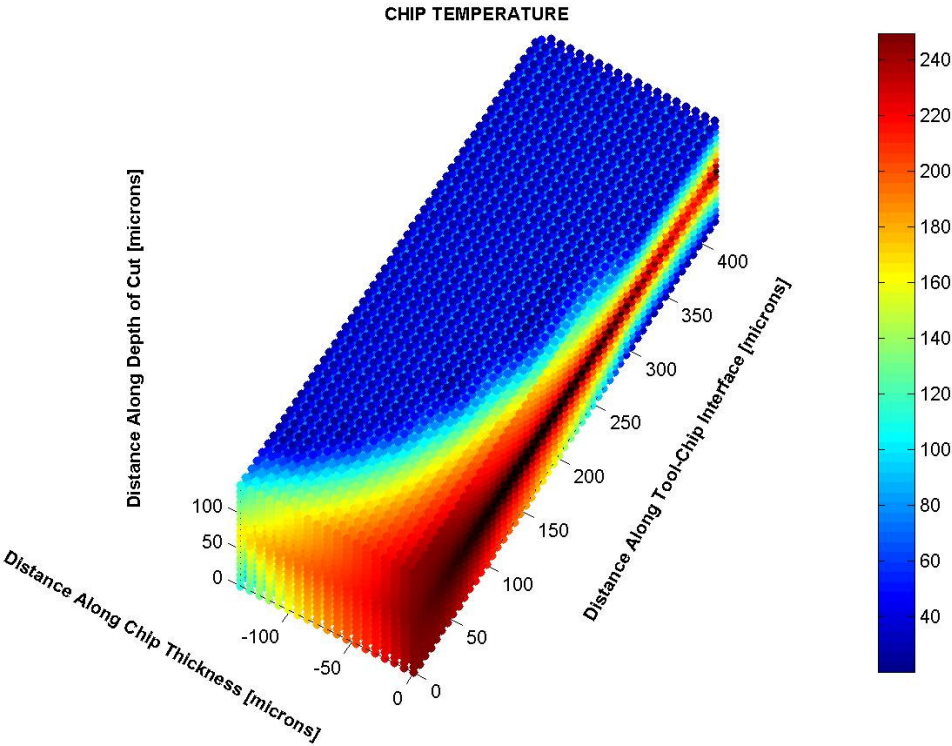


(d)

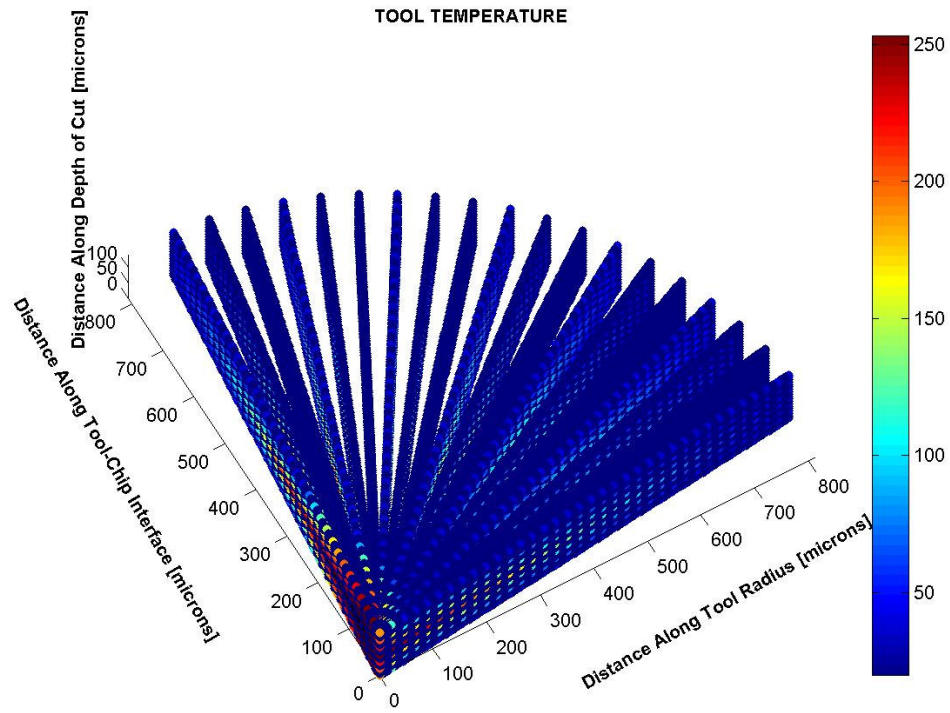
Fig. A.43: Validation for case 43. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



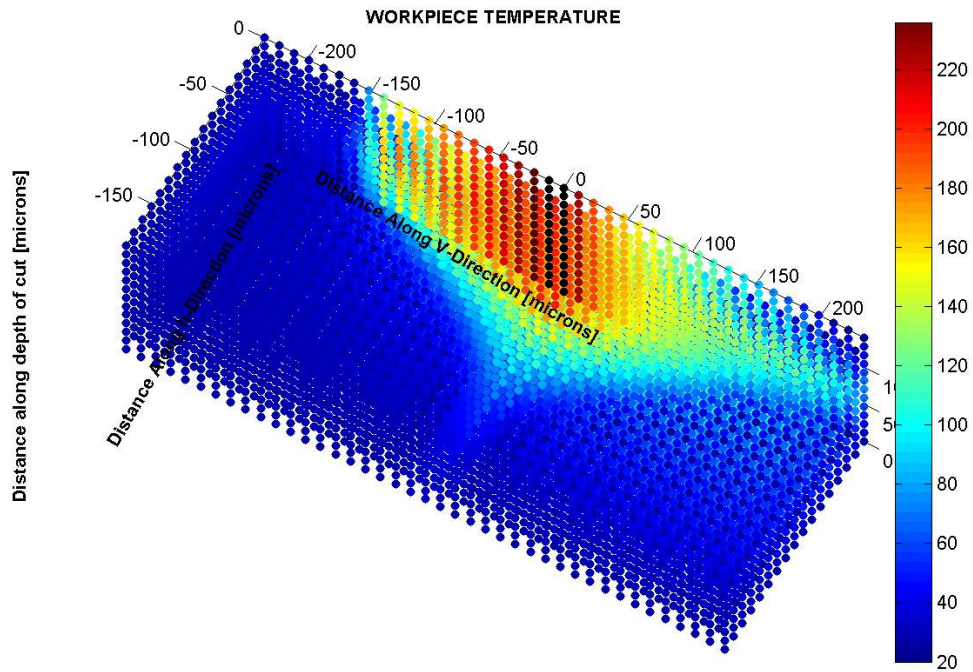
(a)



(b)

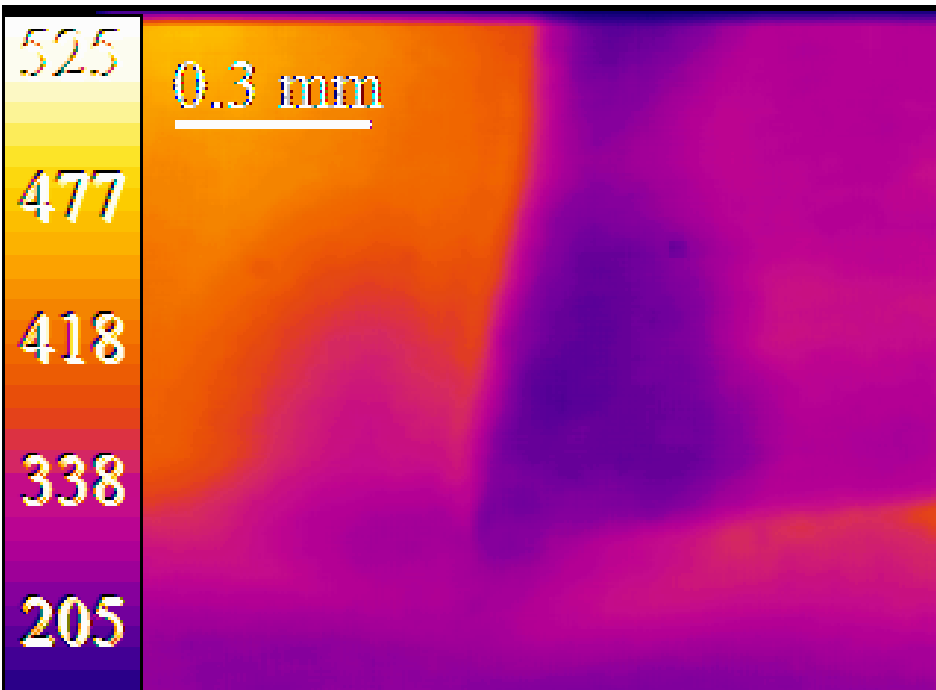


(c)

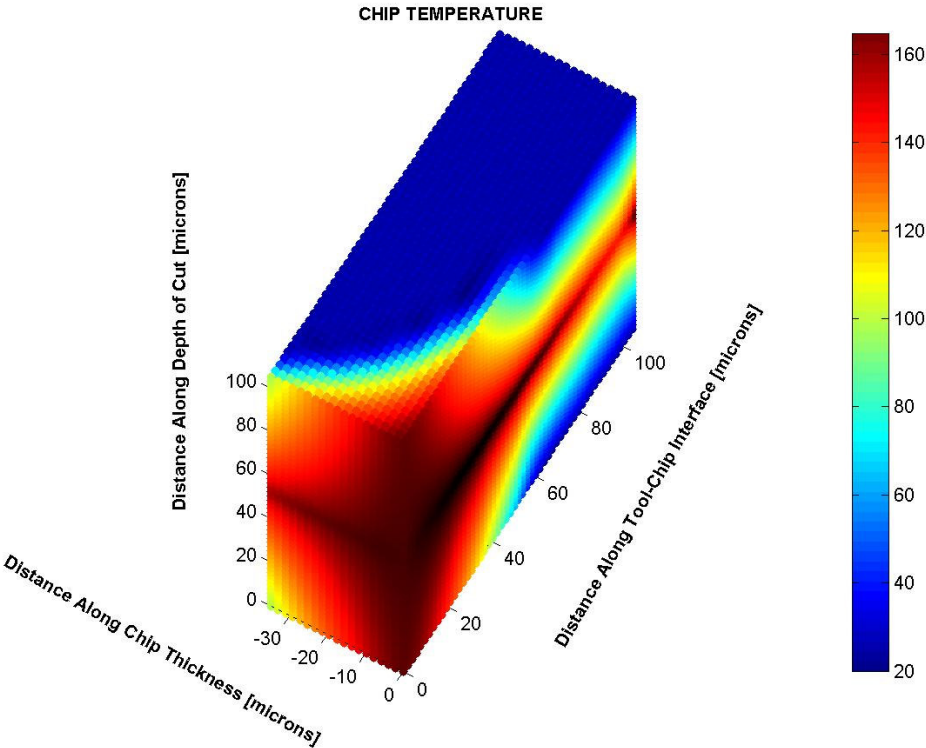


(d)

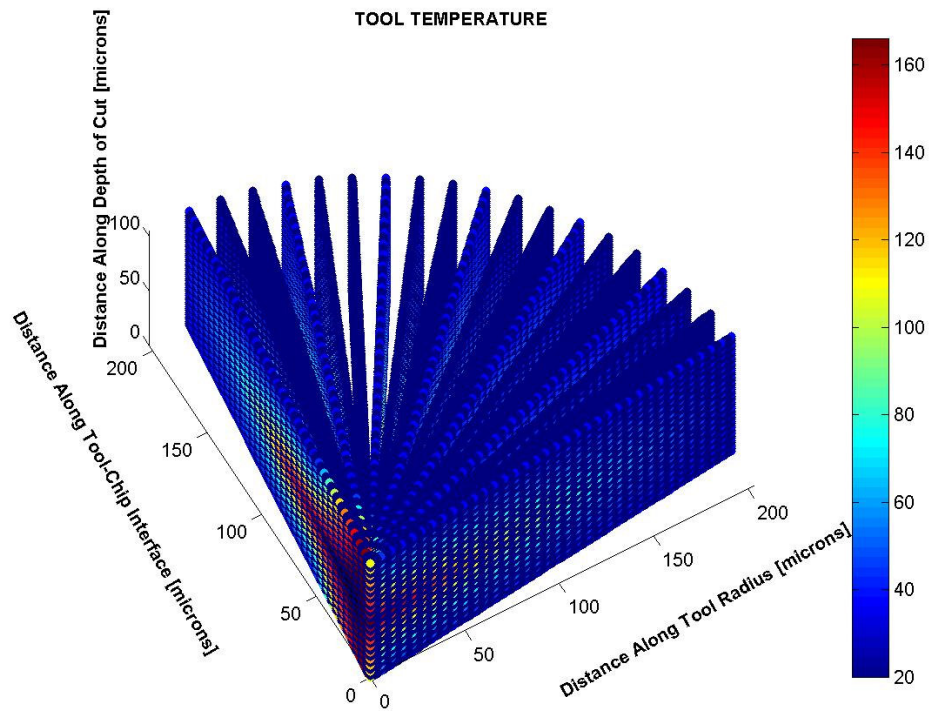
Fig. A.44: Validation for case 44. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



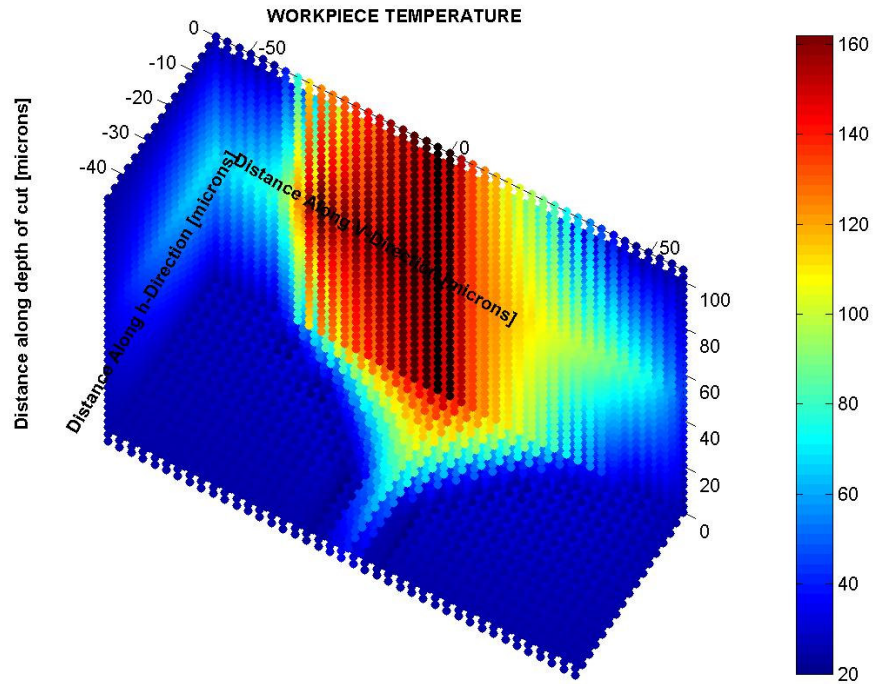
(a)



(b)

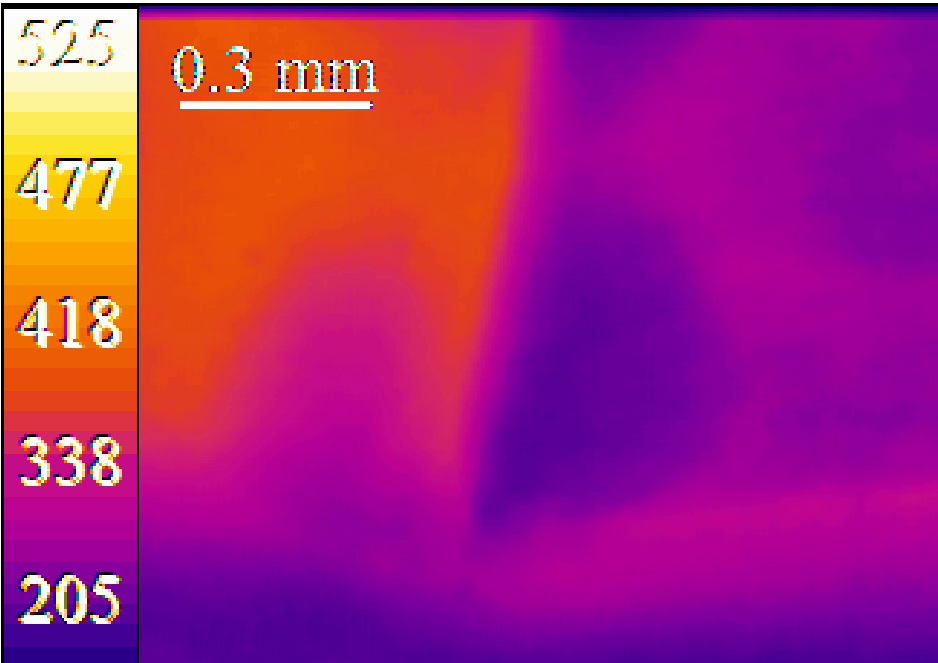


(c)

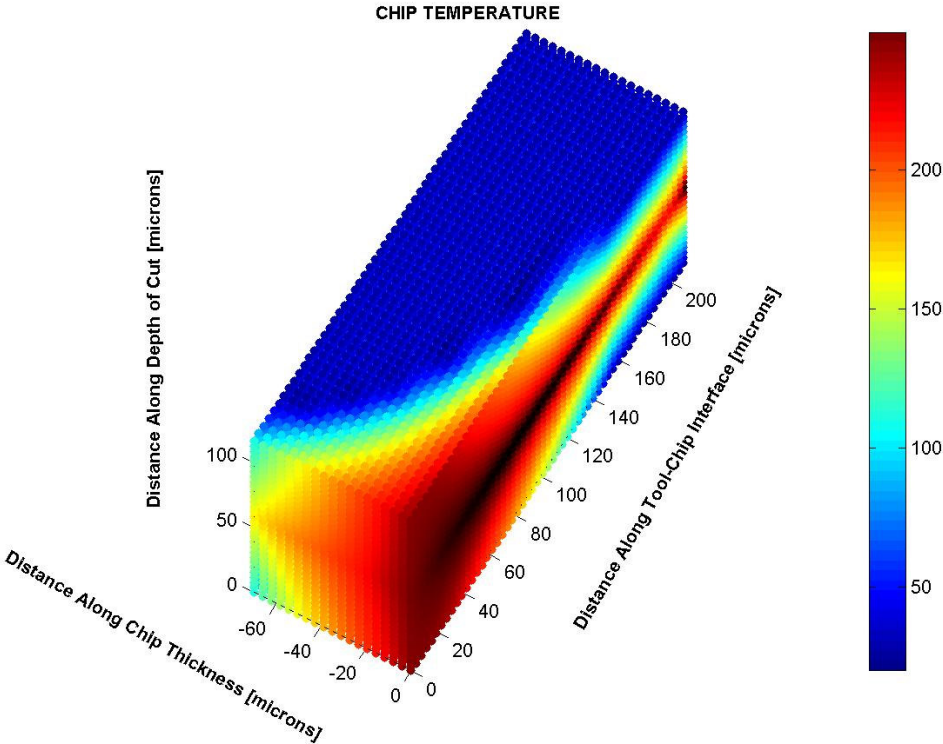


(d)

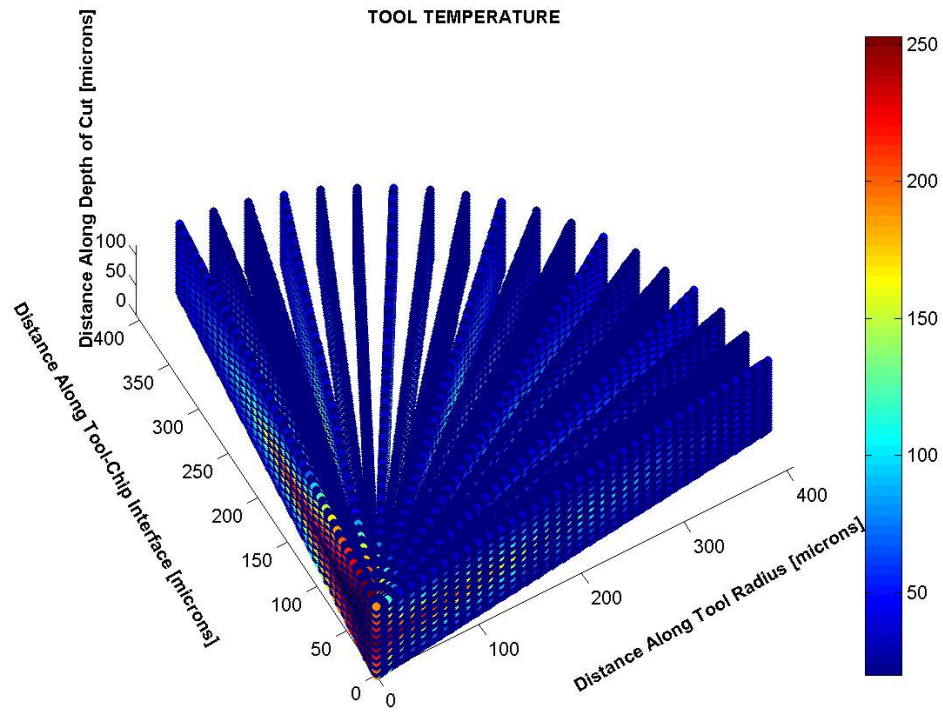
Fig. A.45: Validation for case 45. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



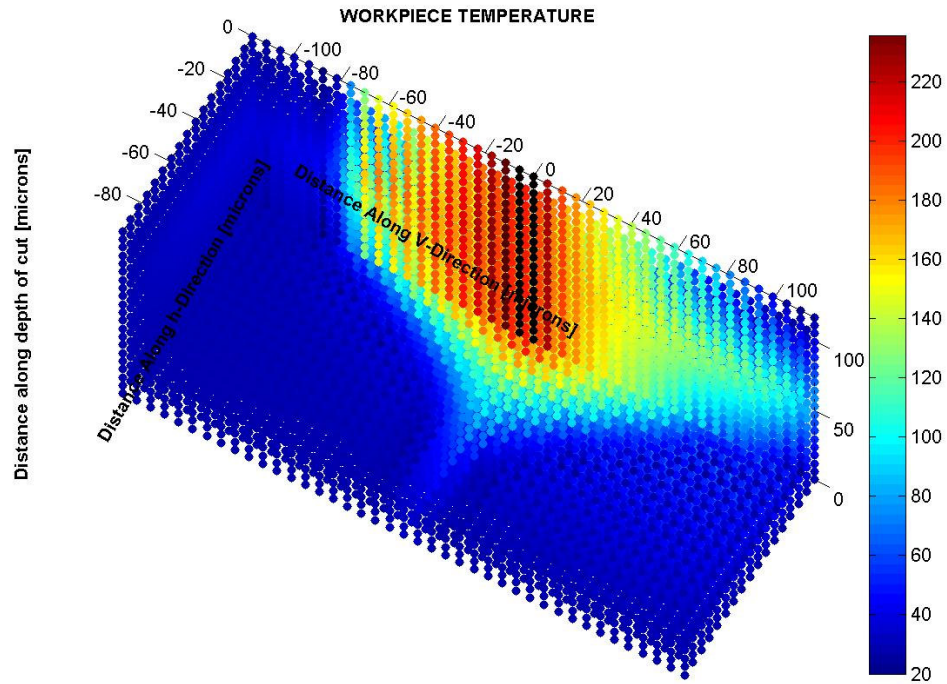
(a)



(b)

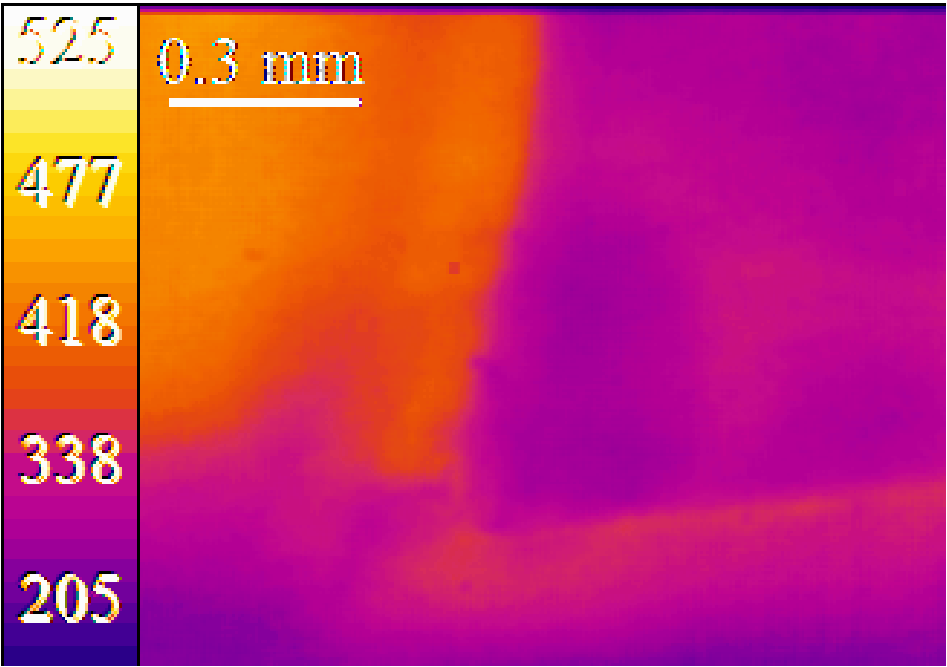


(c)

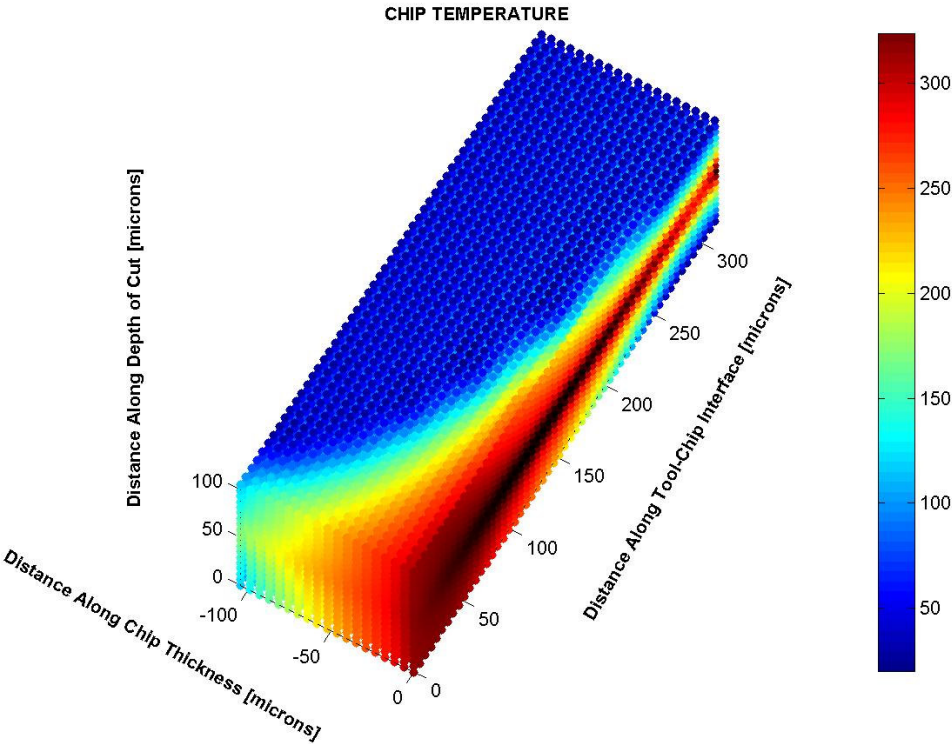


(d)

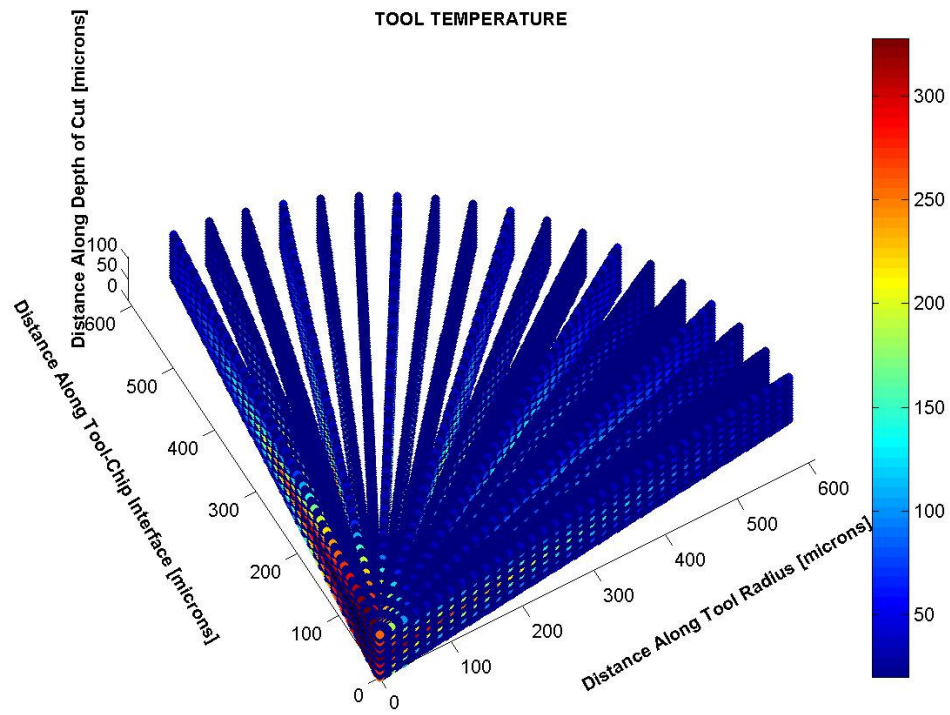
Fig. A.46: Validation for case 46. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



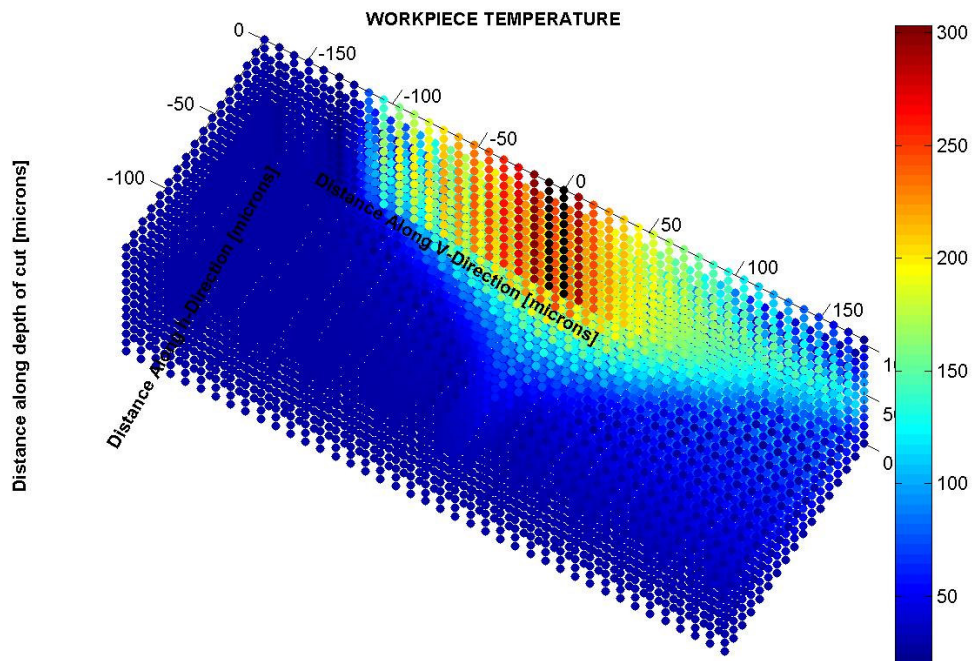
(a)



(b)

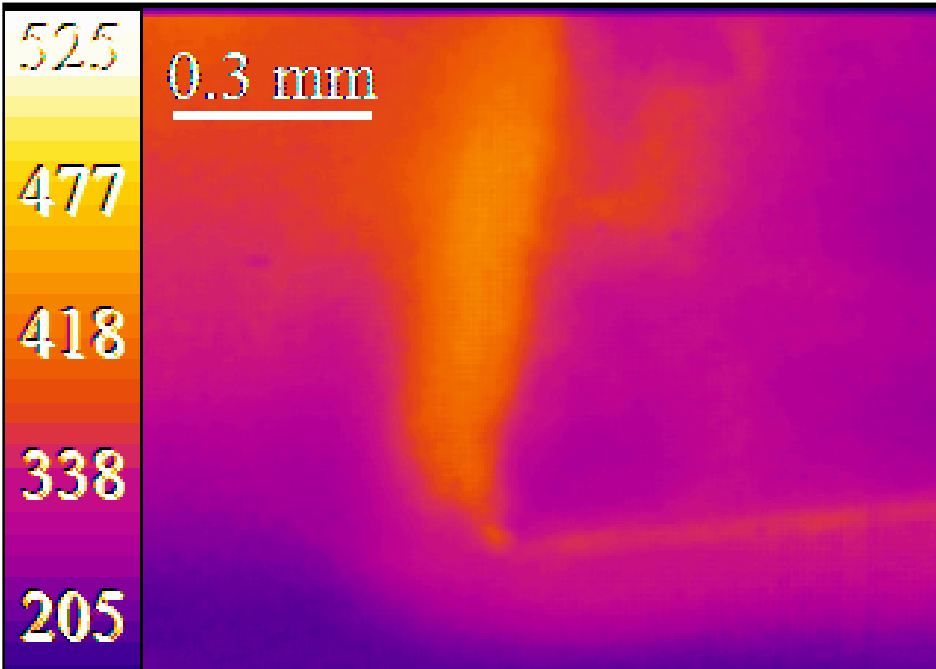


(c)

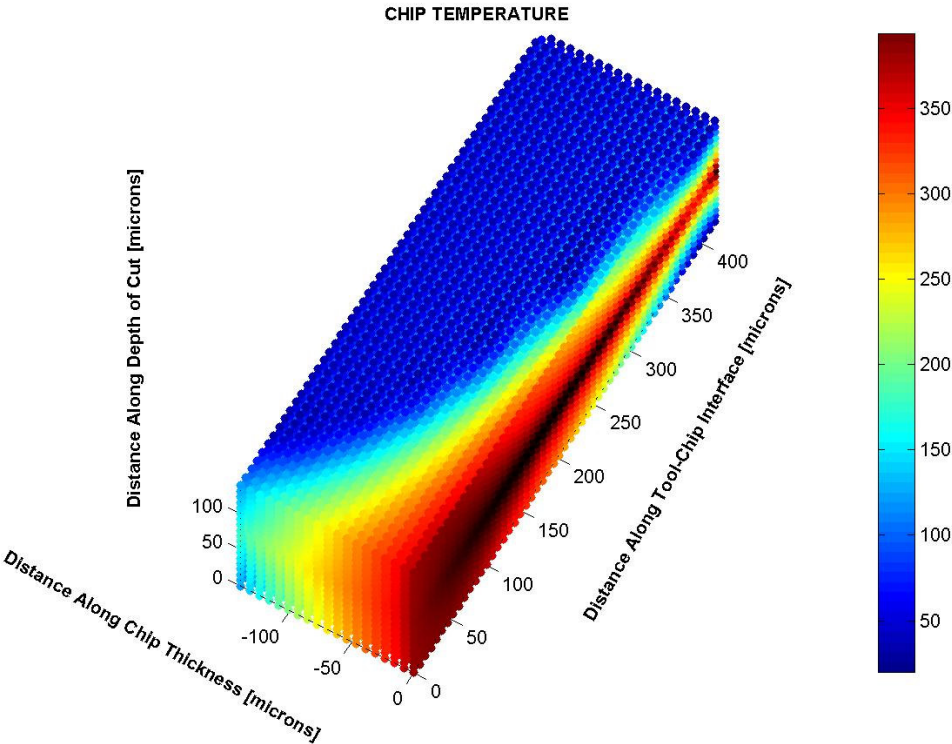


(d)

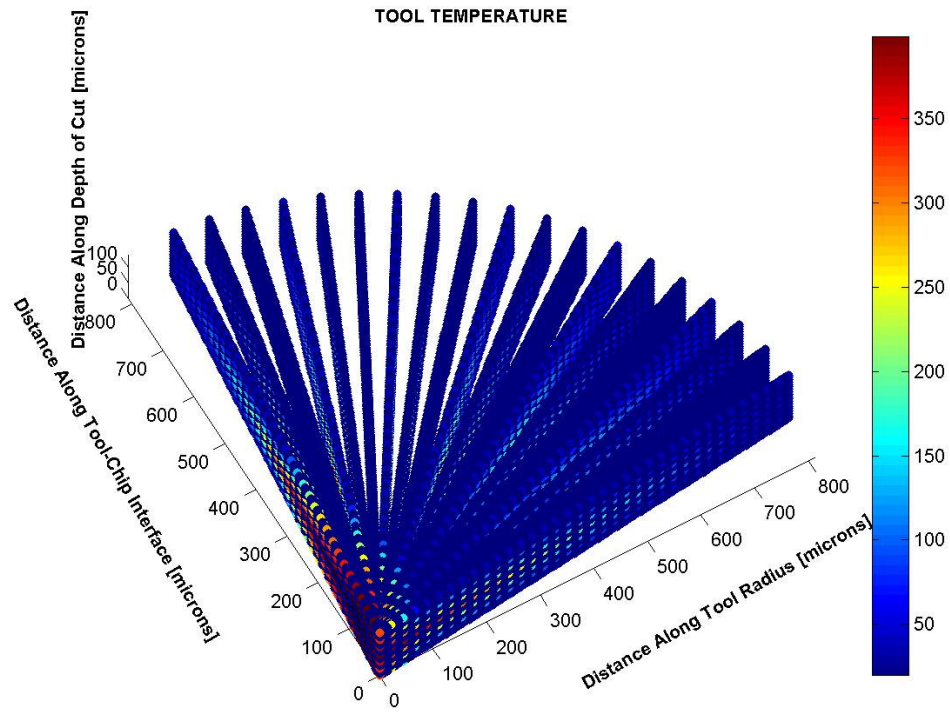
Fig. A.47: Validation for case 47. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



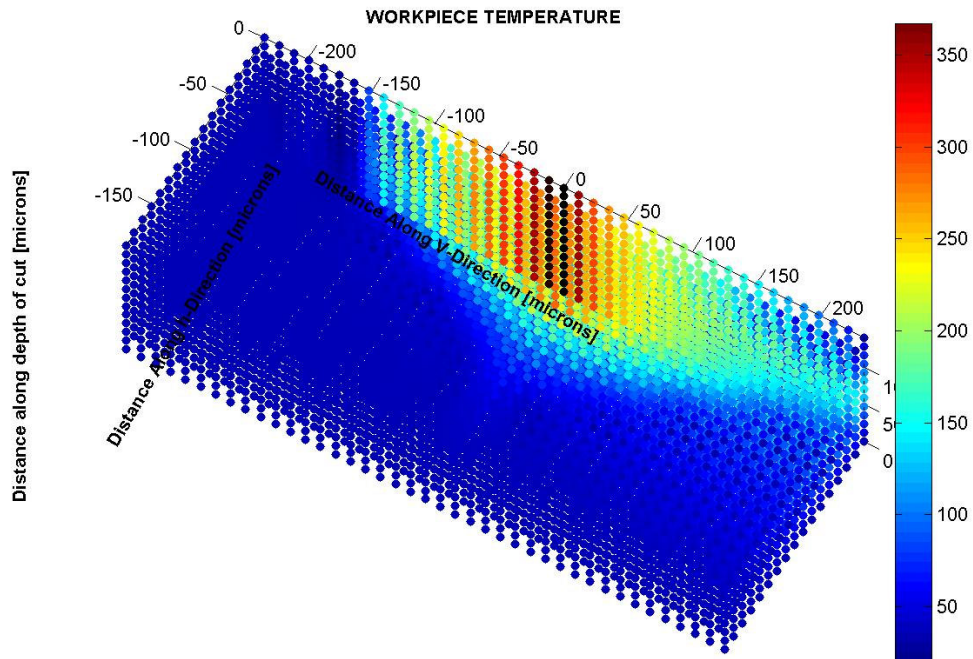
(a)



(b)

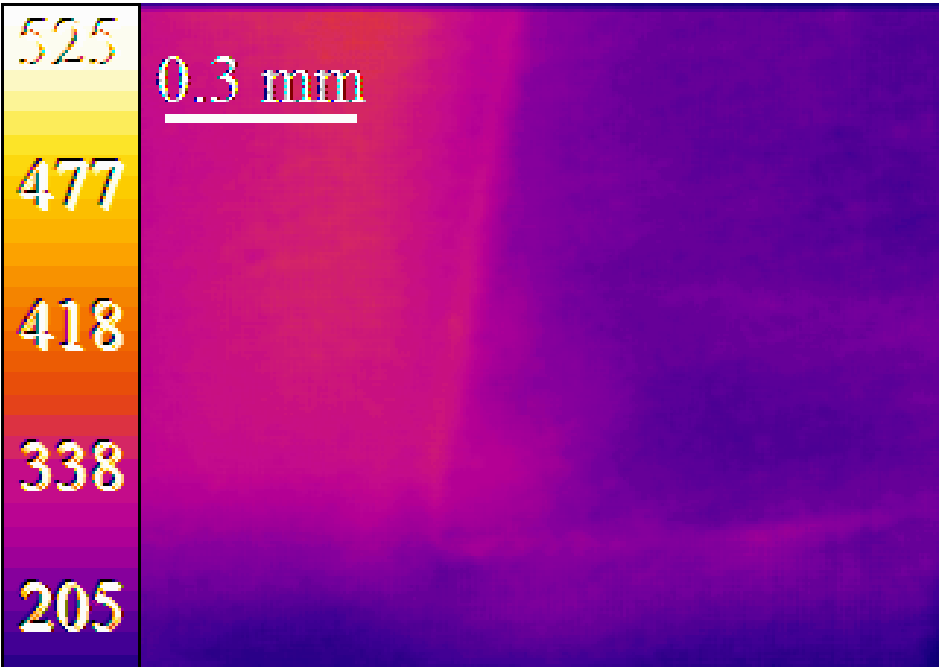


(c)

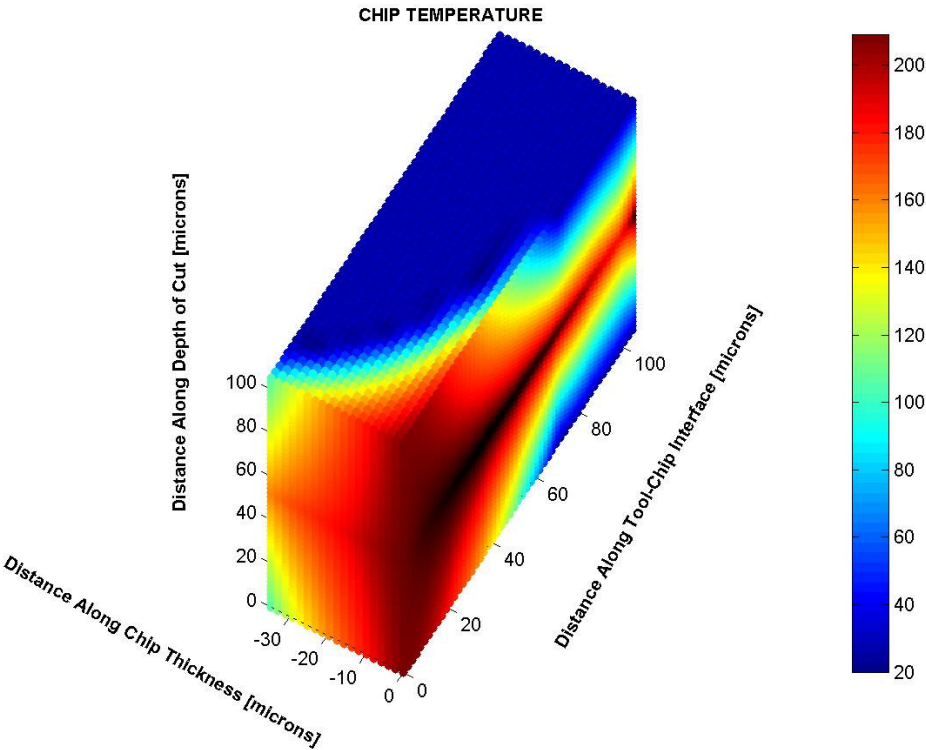


(d)

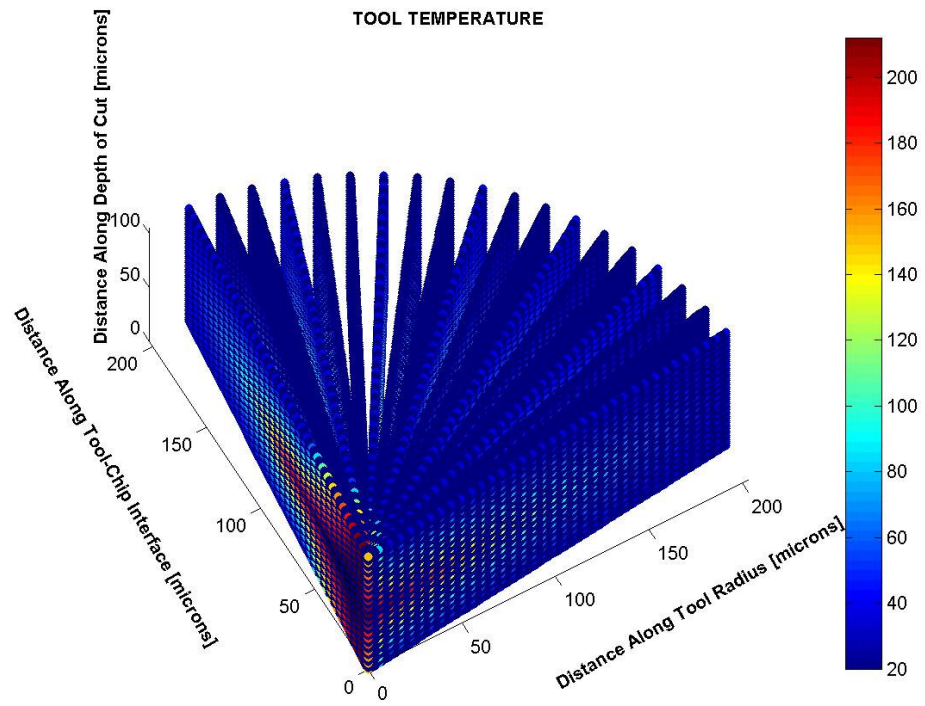
Fig. A.48: Validation for case 48. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



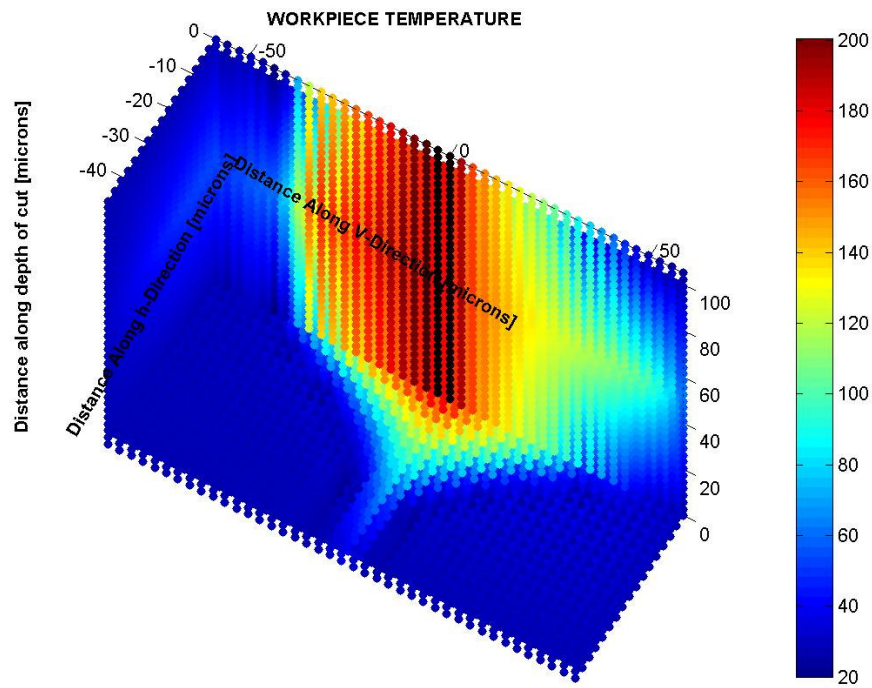
(a)



(b)

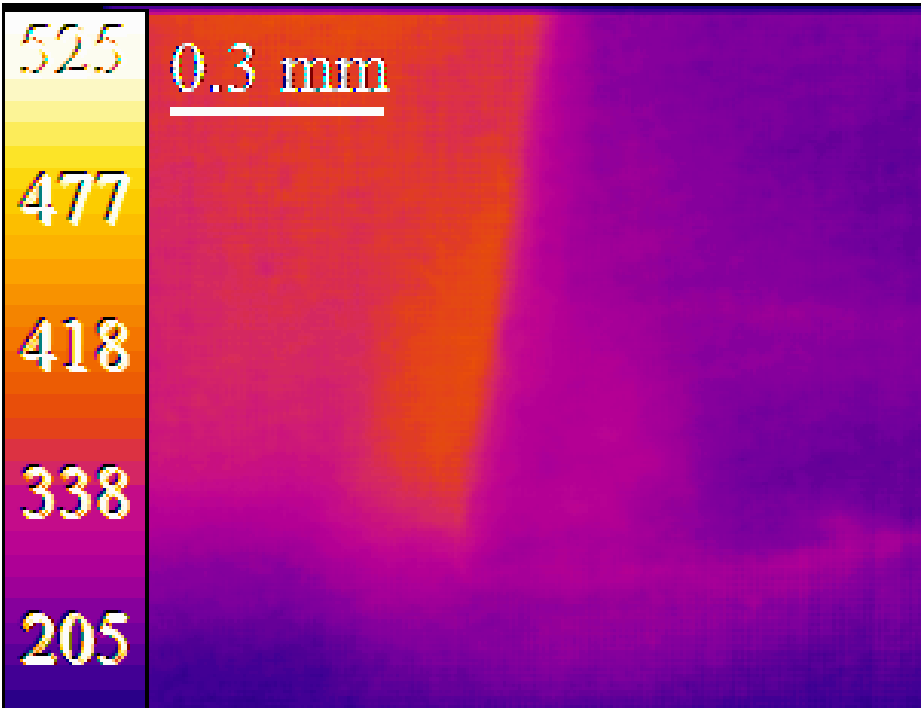


(c)

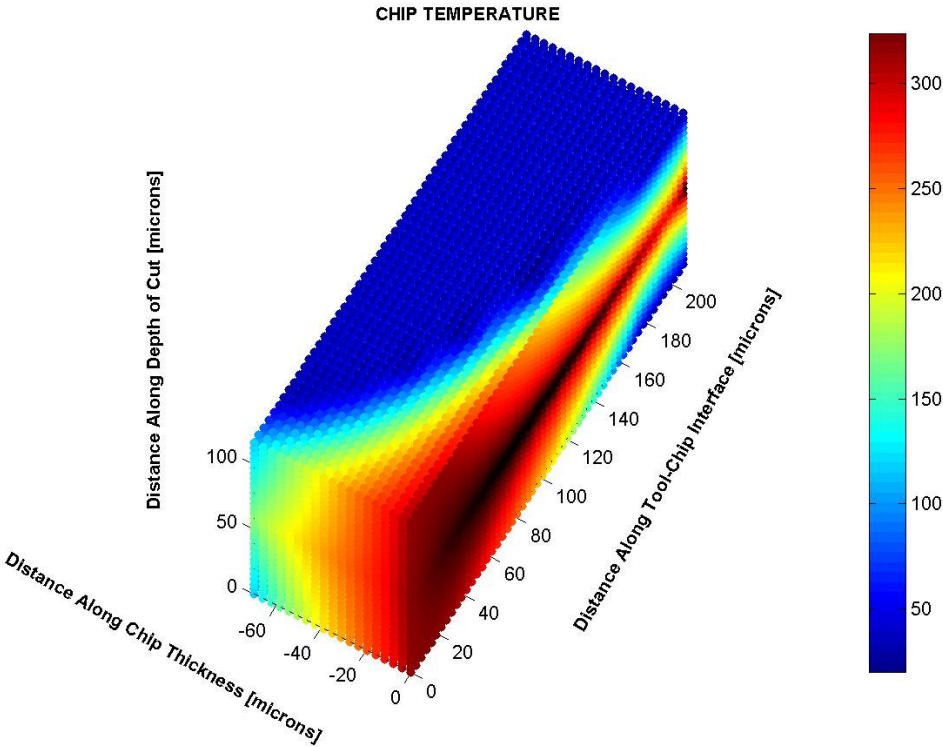


(d)

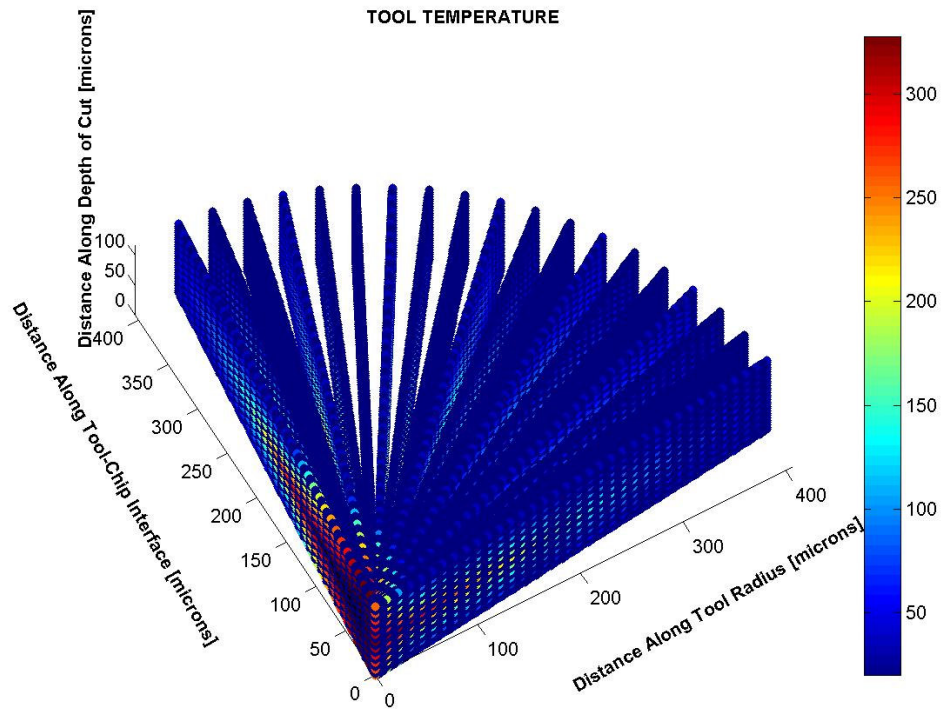
Fig. A.49: Validation for case 49. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



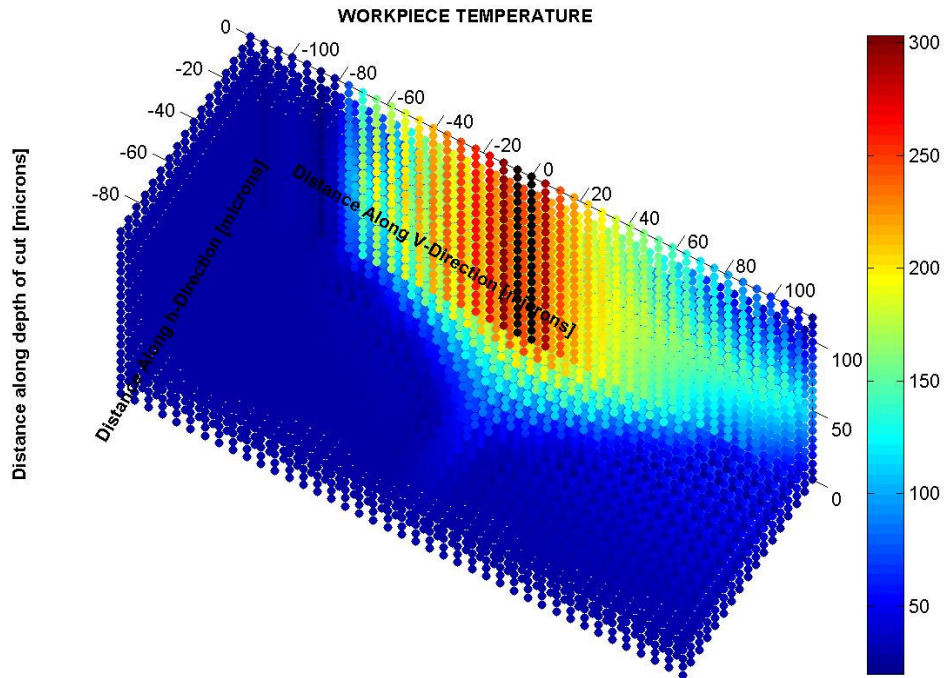
(a)



(b)

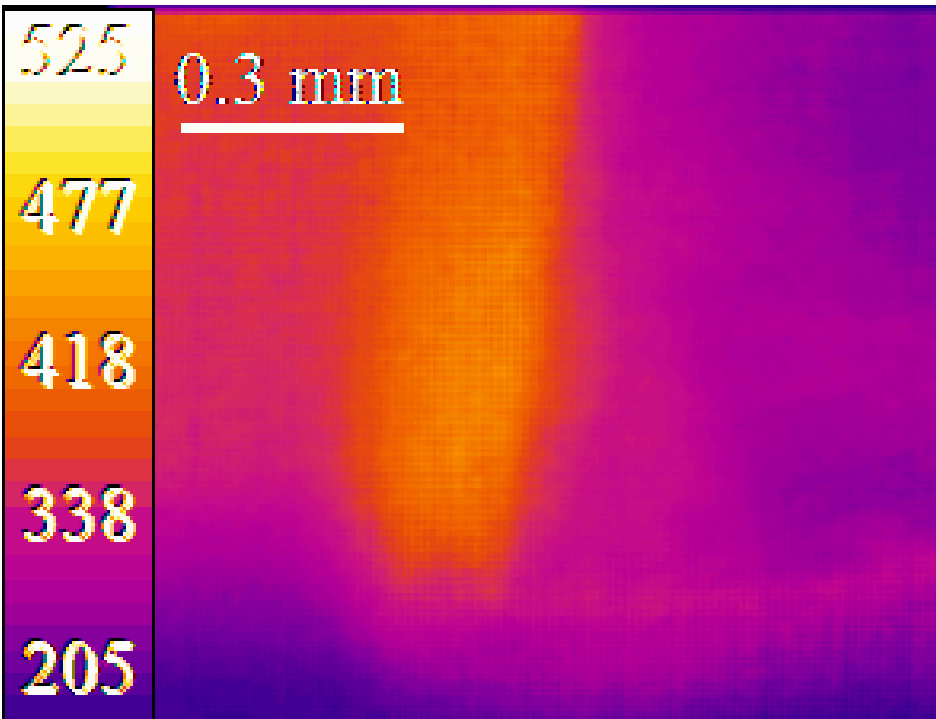


(c)

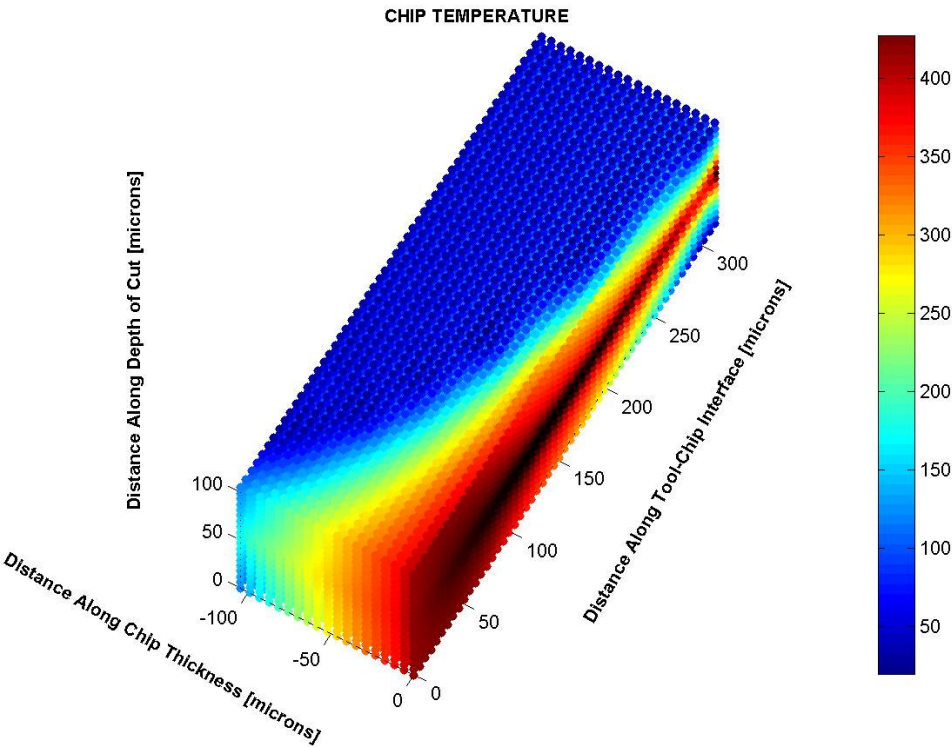


(d)

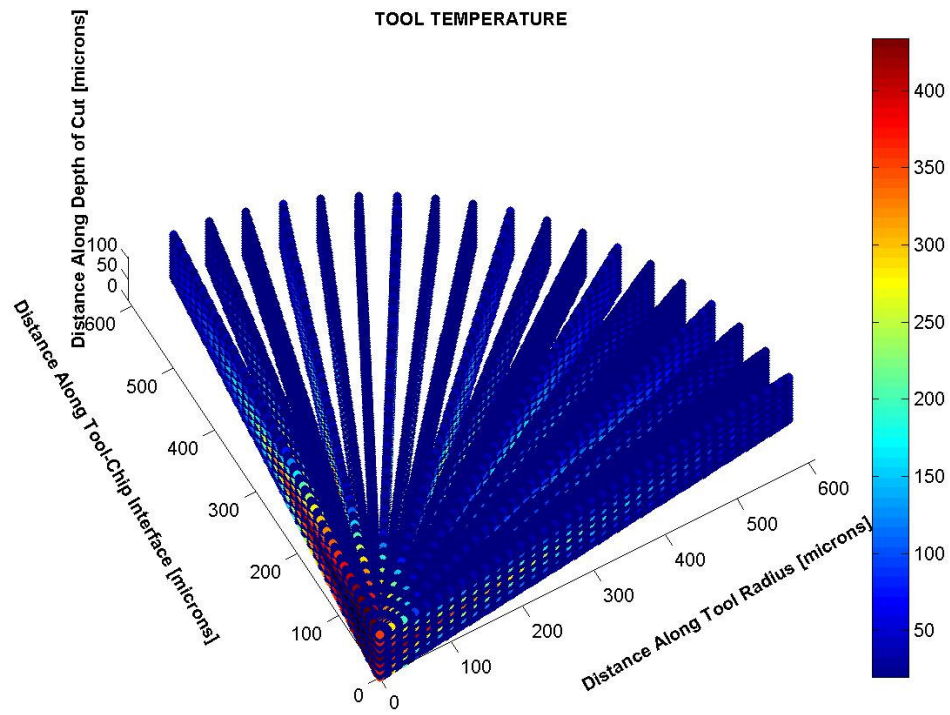
Fig. A.50: Validation for case 50. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



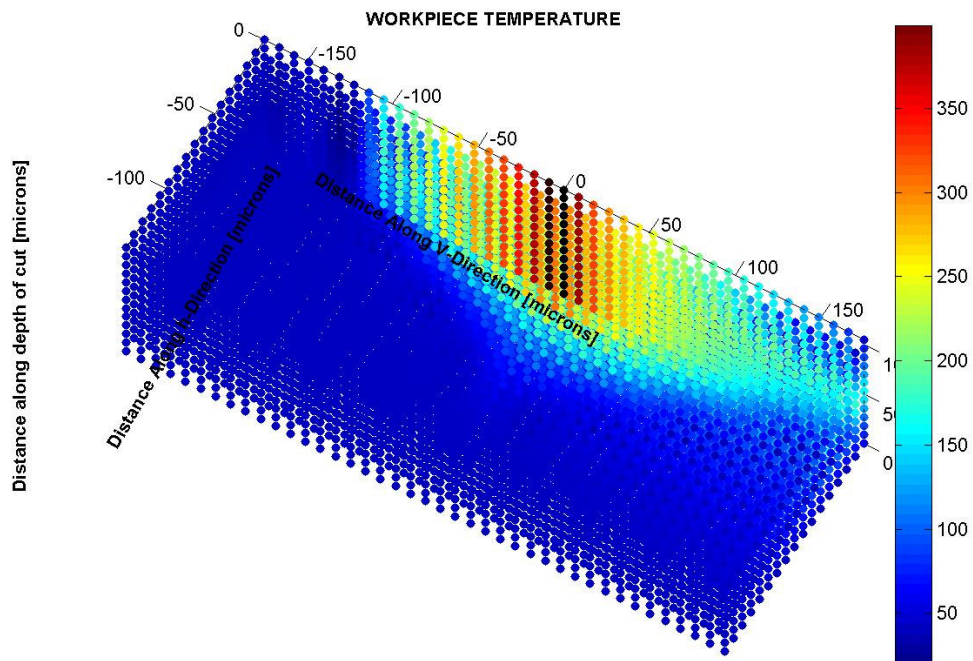
(a)



(b)

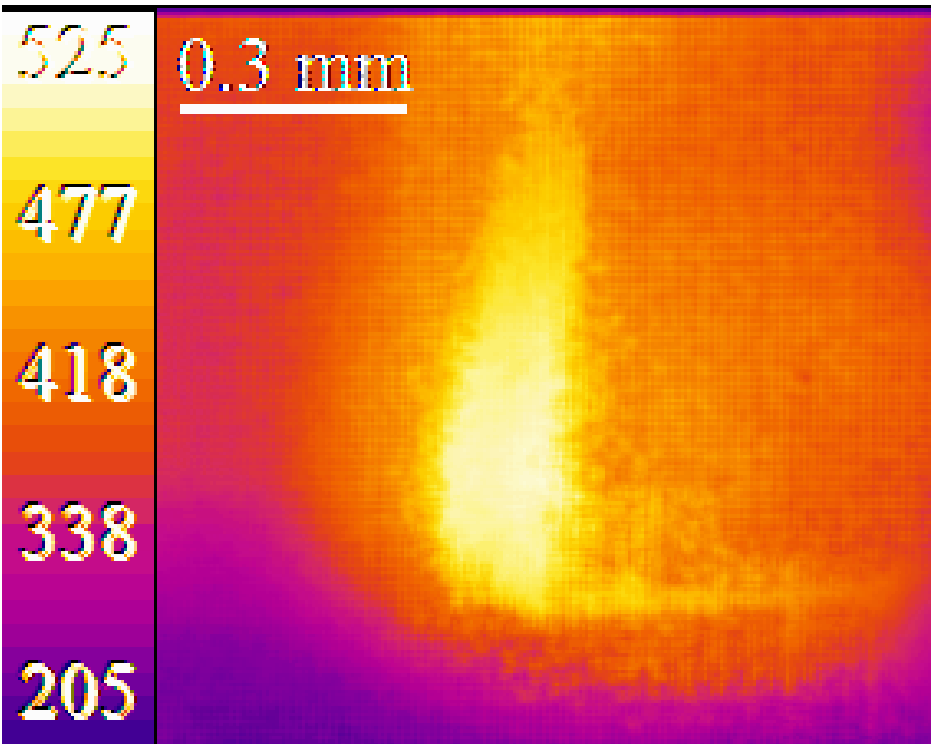


(c)

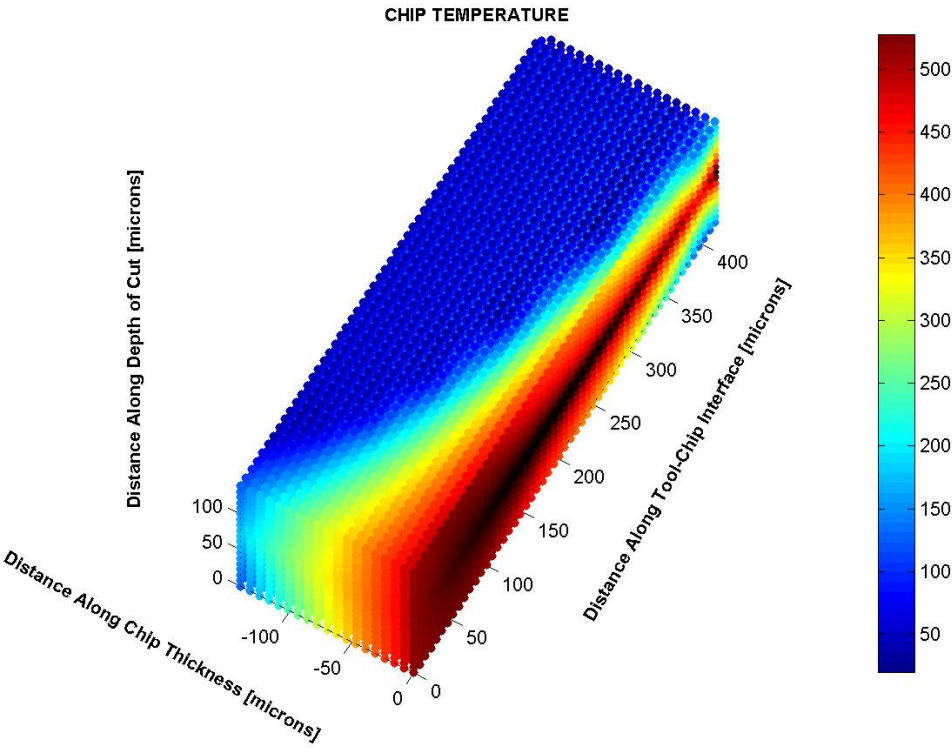


(d)

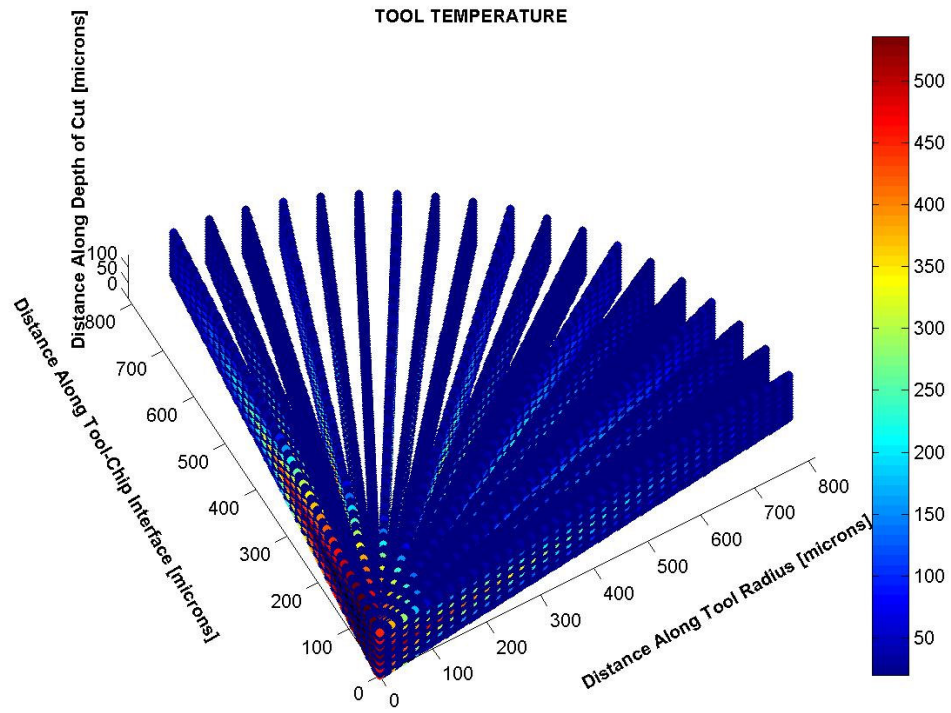
Fig. A.51: Validation for case 51. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



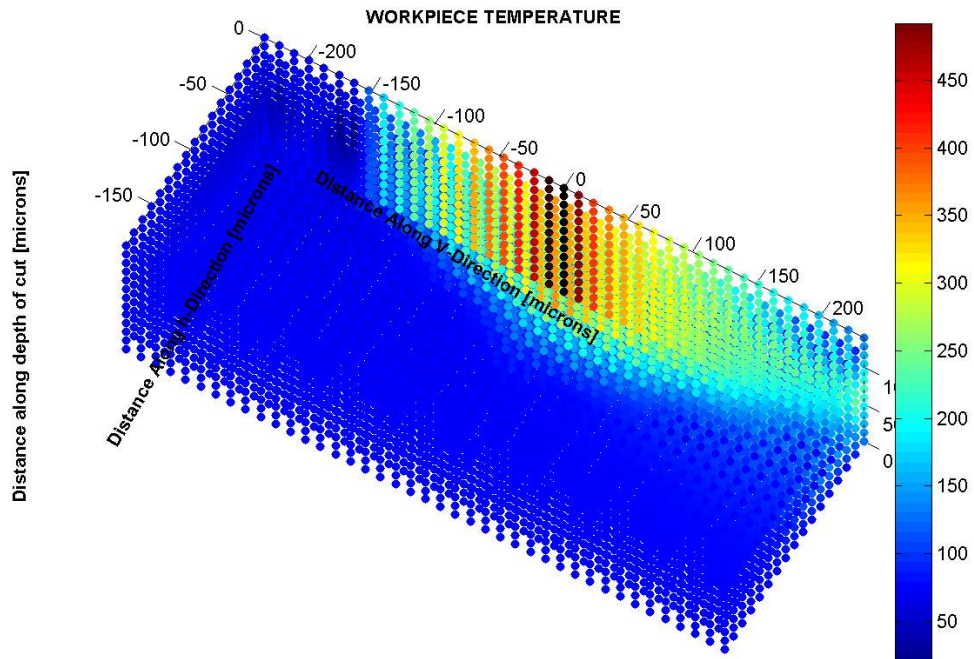
(a)



(b)

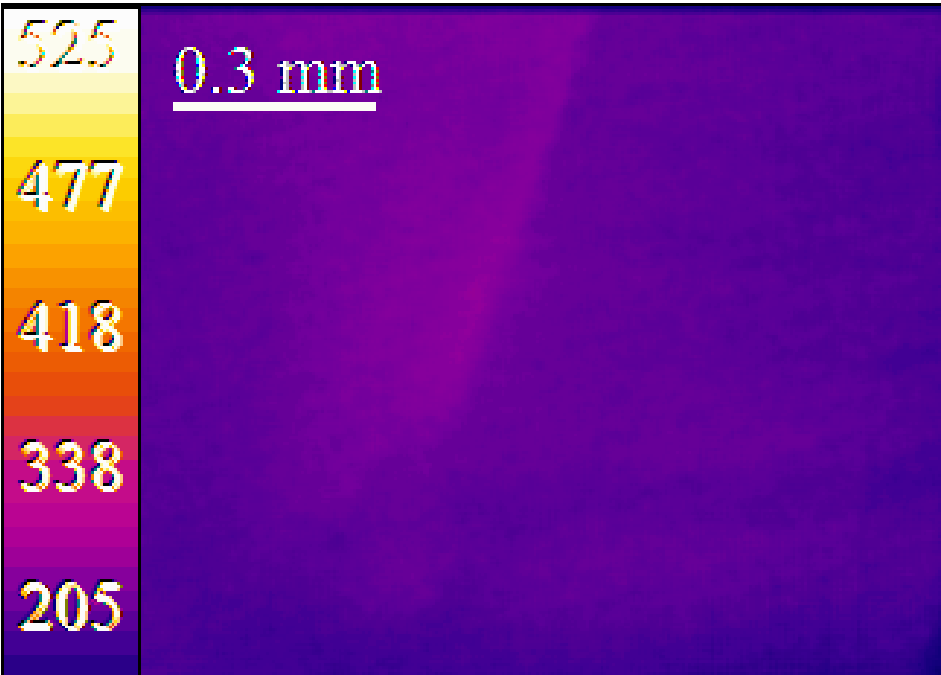


(c)

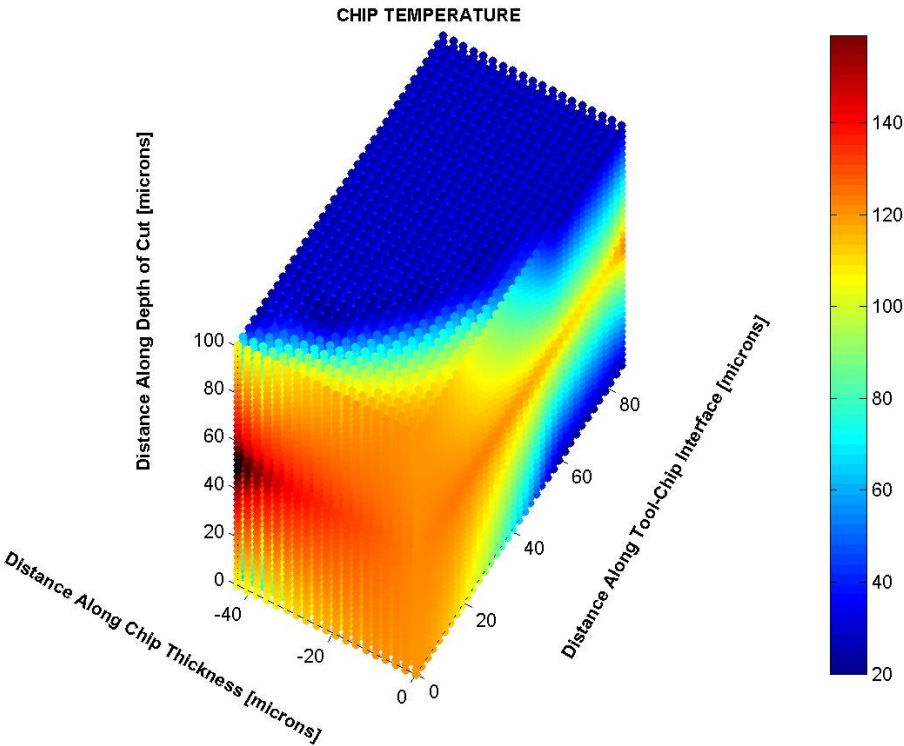


(d)

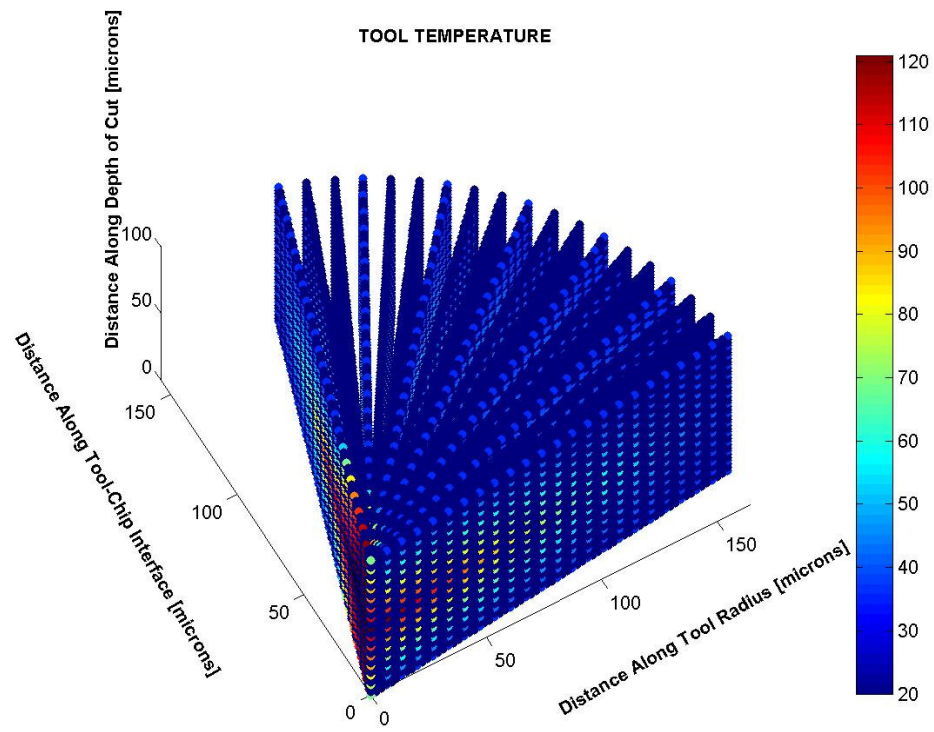
Fig. A.52: Validation for case 52. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



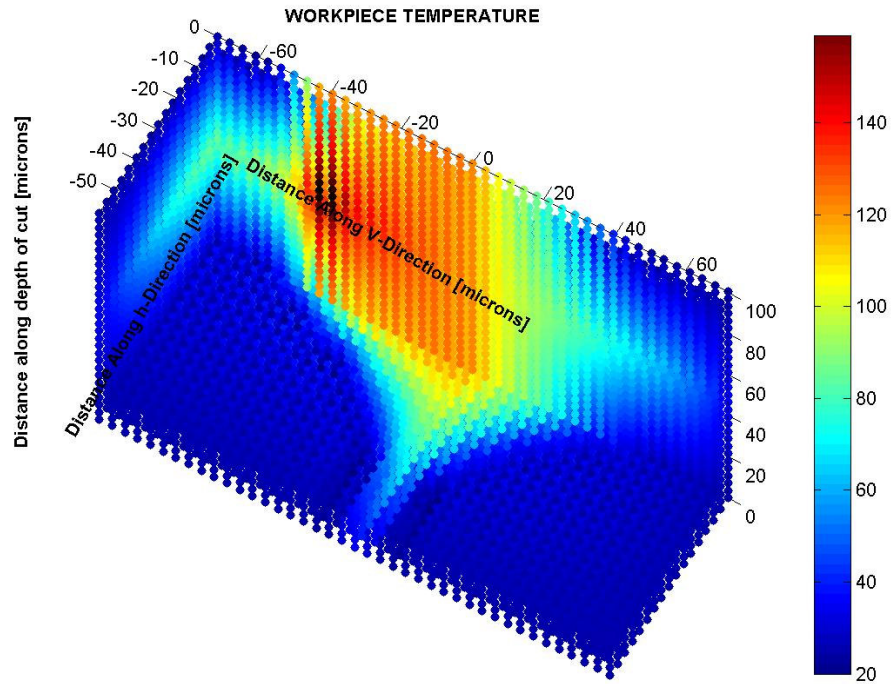
(a)



(b)

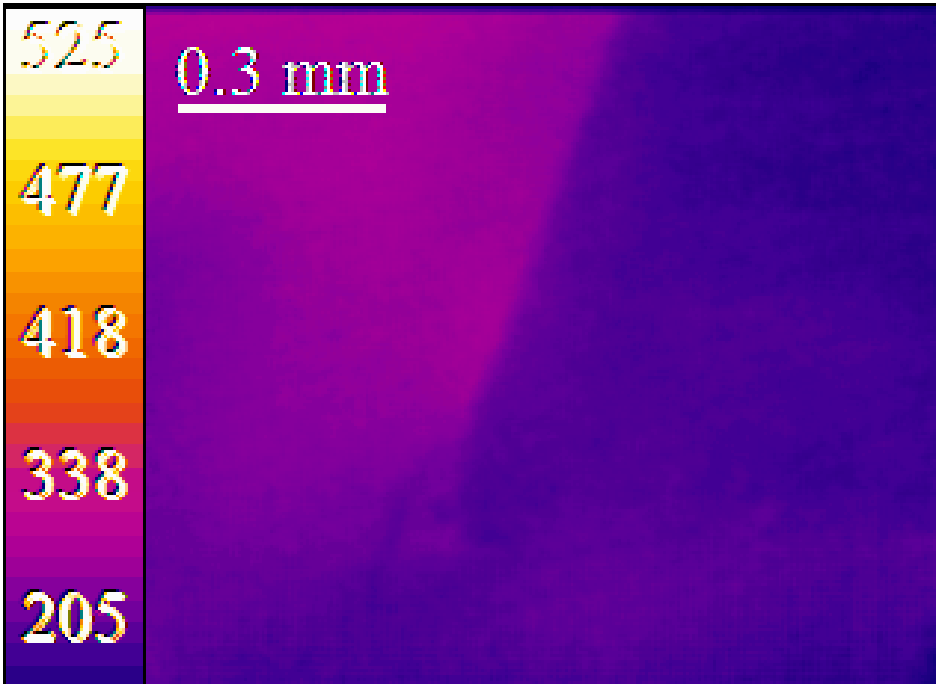


(c)

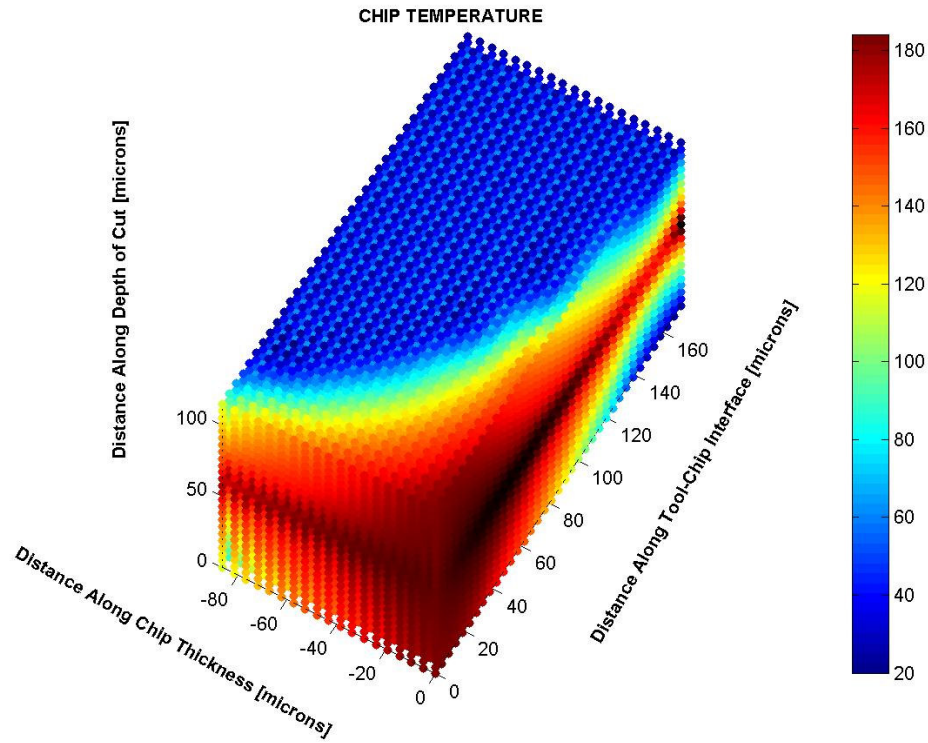


(d)

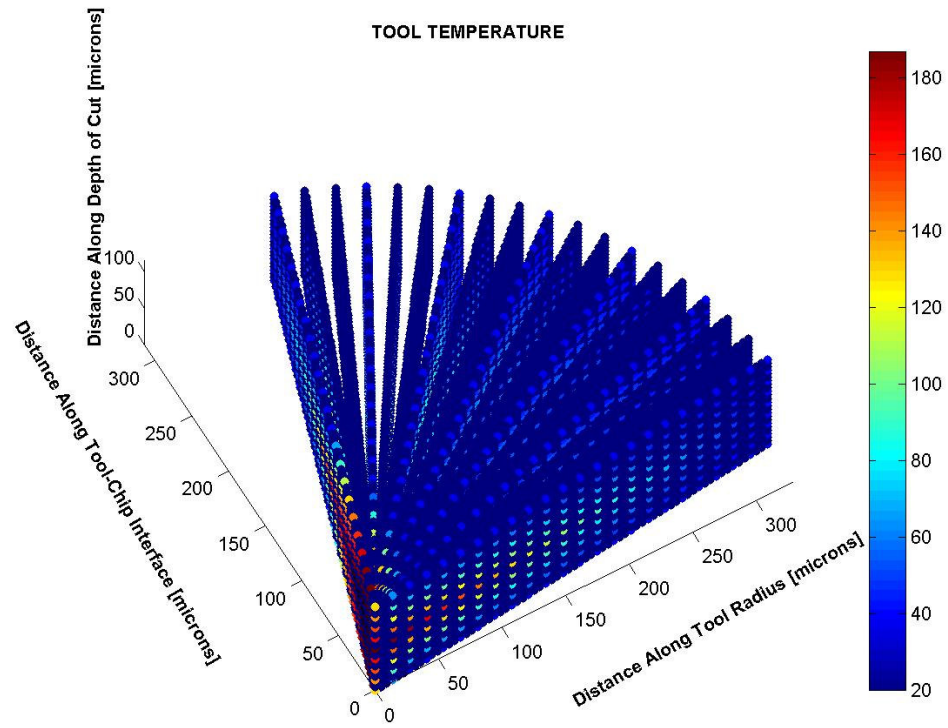
Fig. A.53: Validation for case 53. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



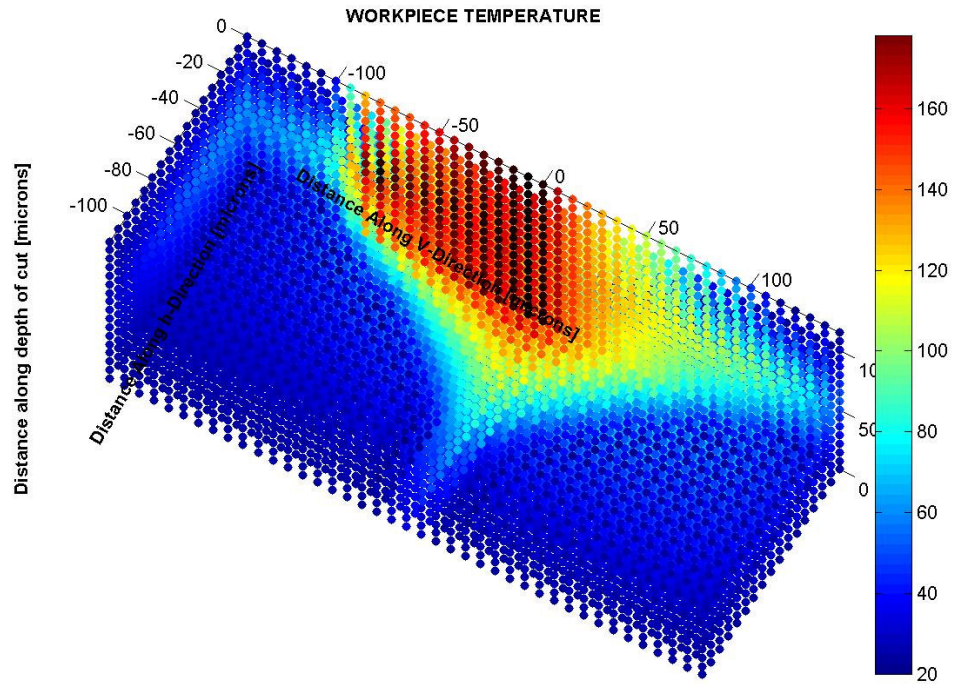
(a)



(b)

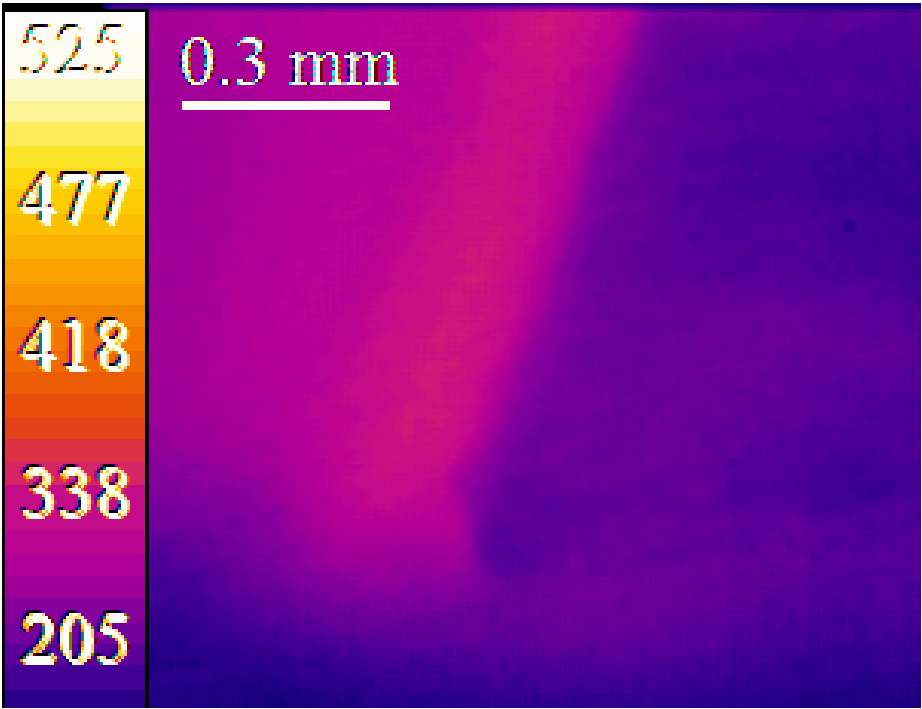


(c)

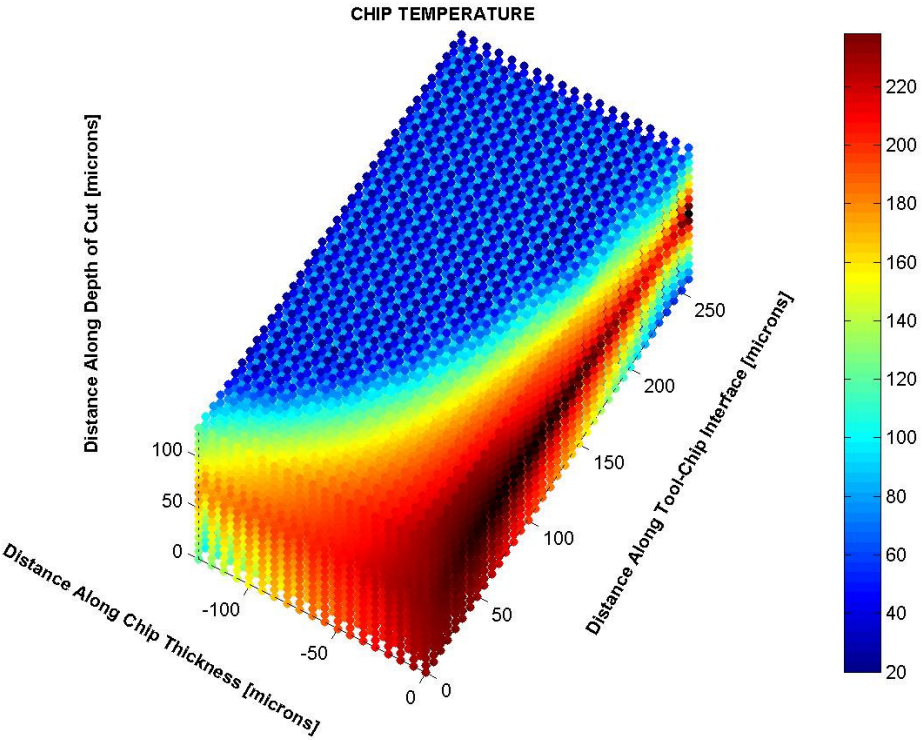


(d)

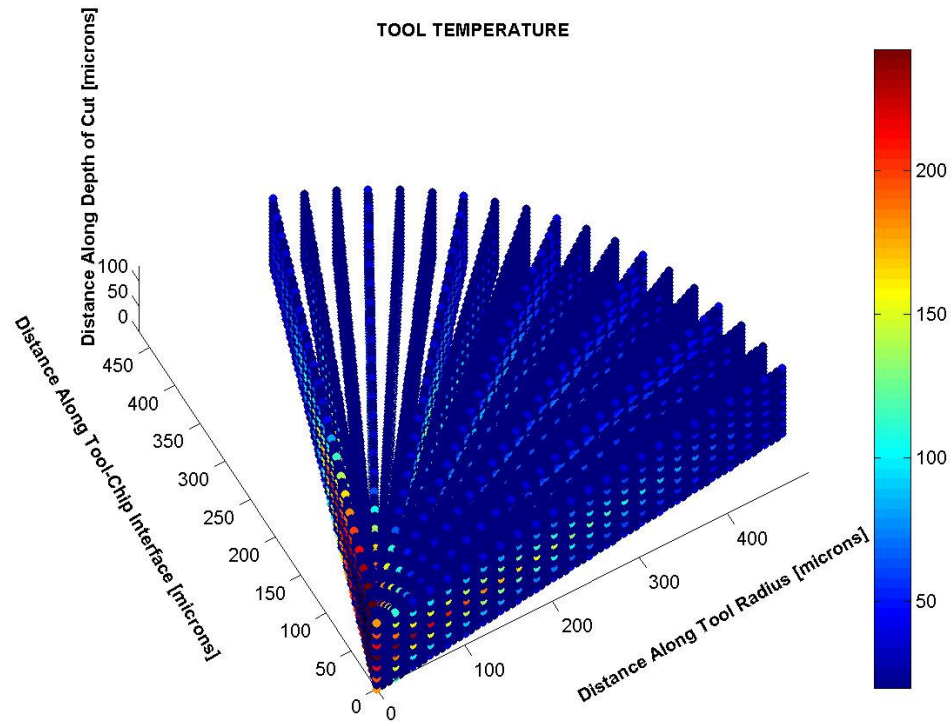
Fig. A.54: Validation for case 54. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



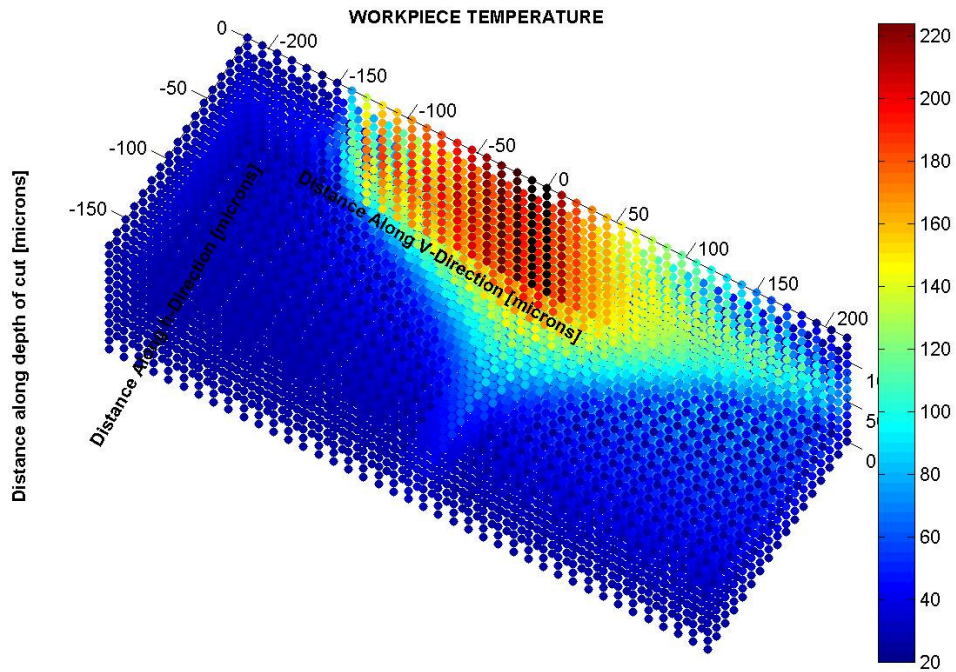
(a)



(b)

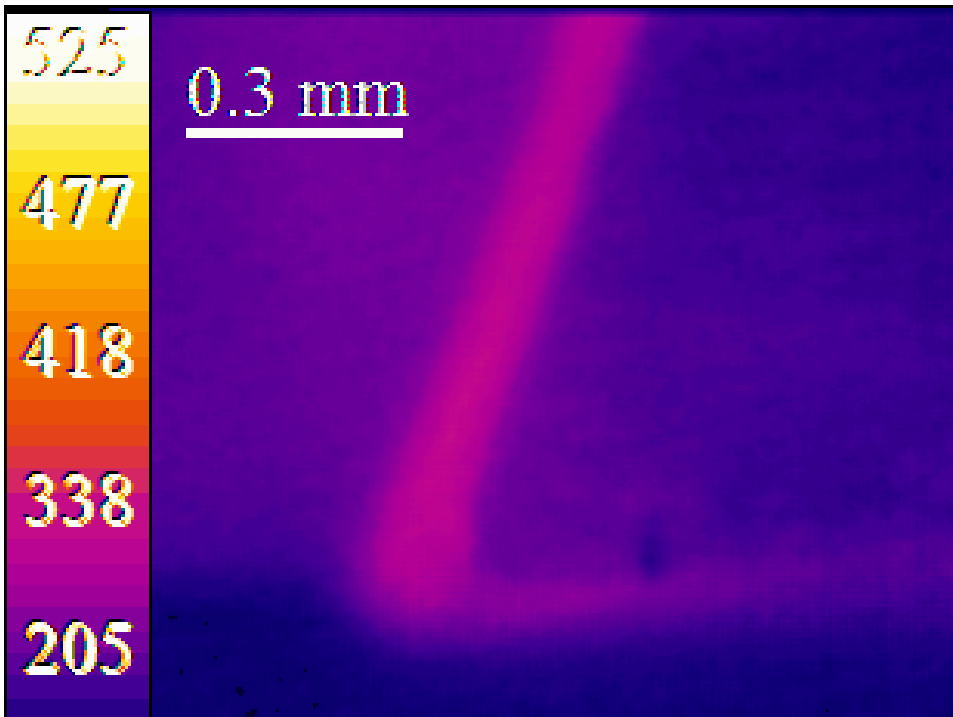


(c)

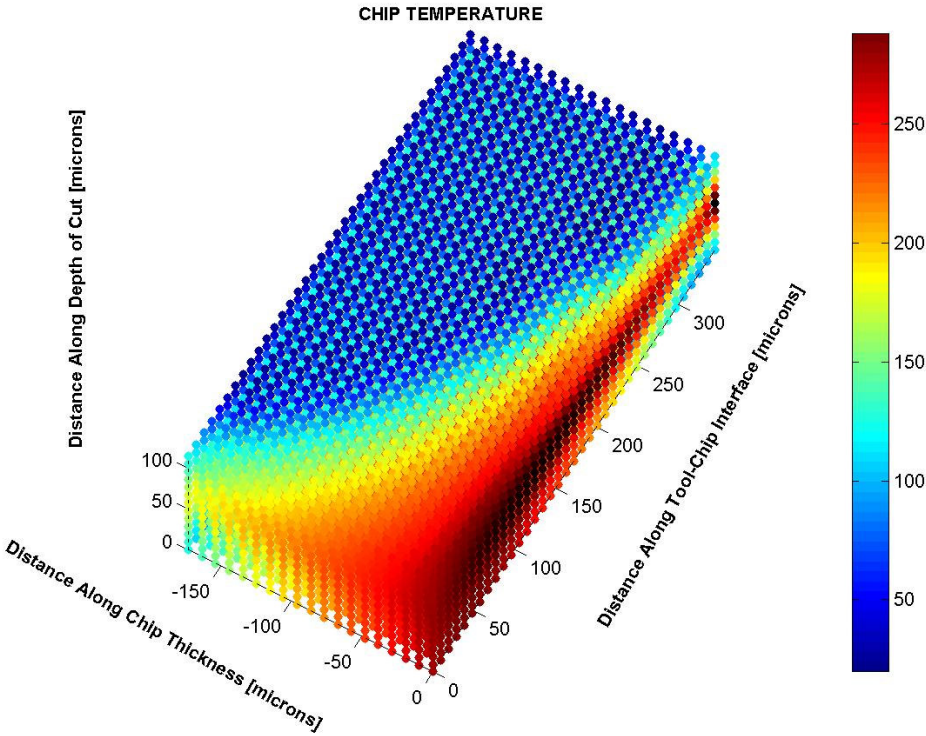


(d)

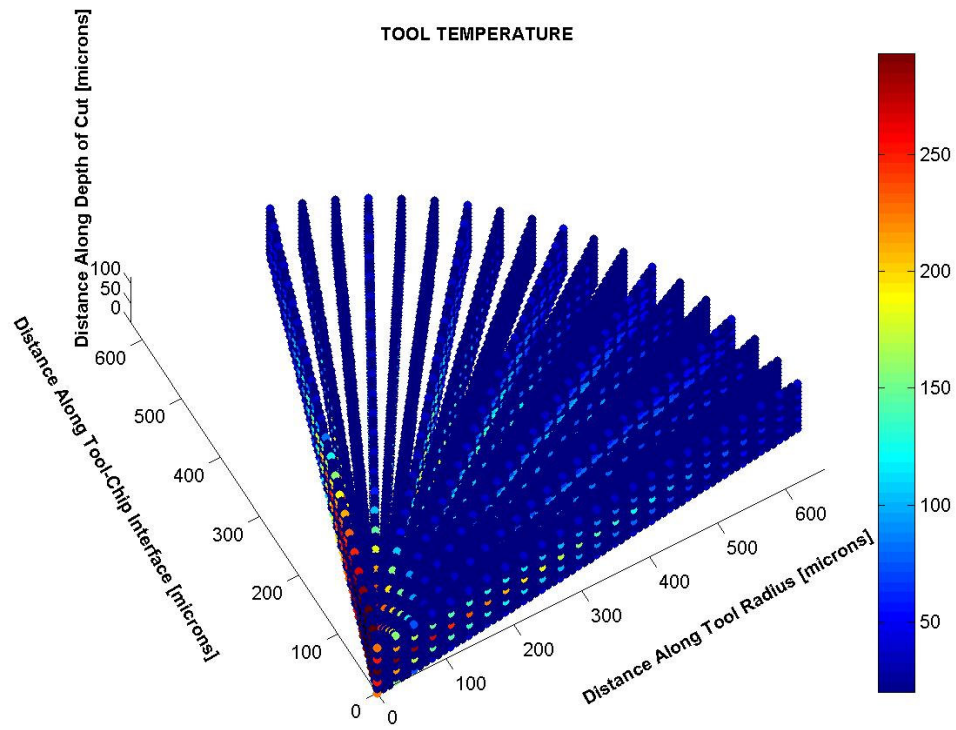
Fig. A.55: Validation for case 55. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



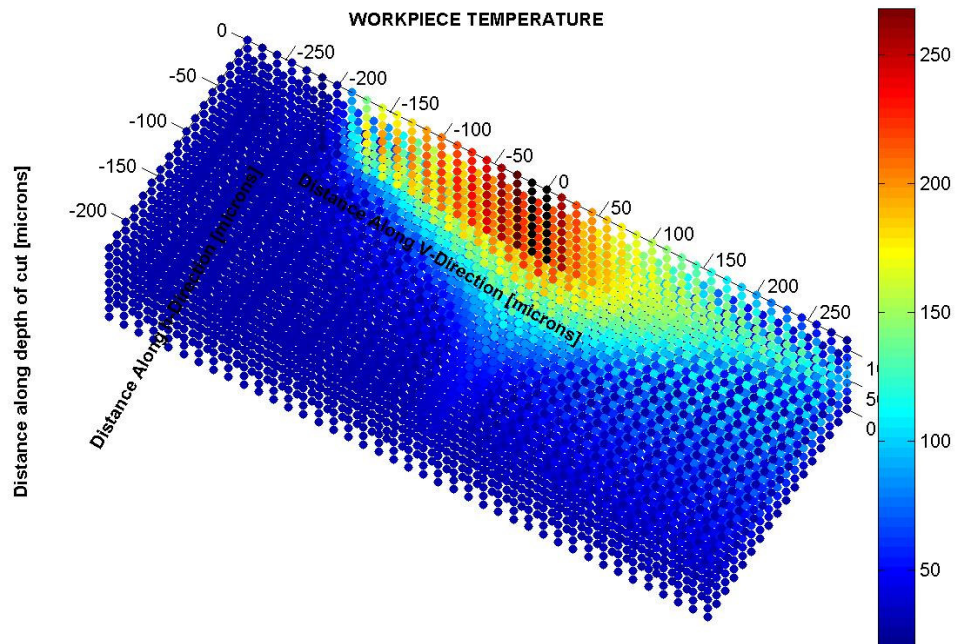
(a)



(b)

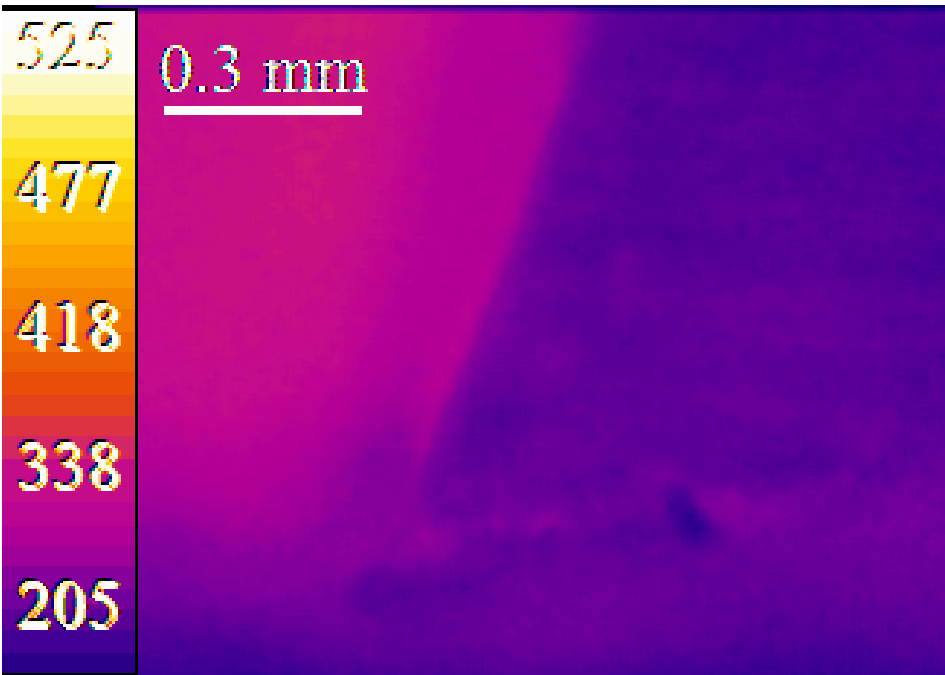


(c)

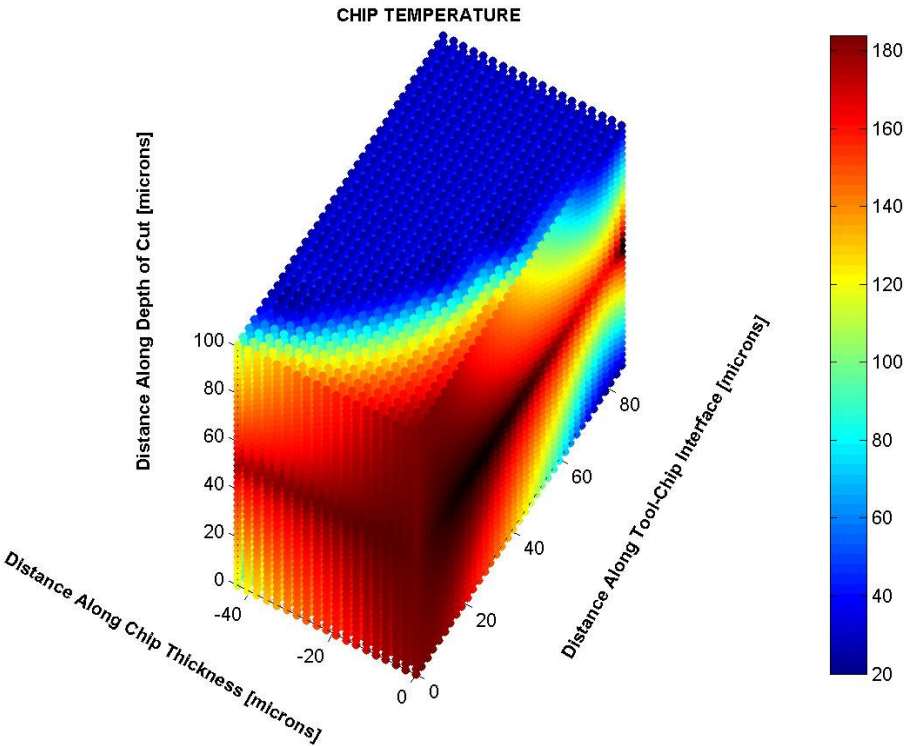


(d)

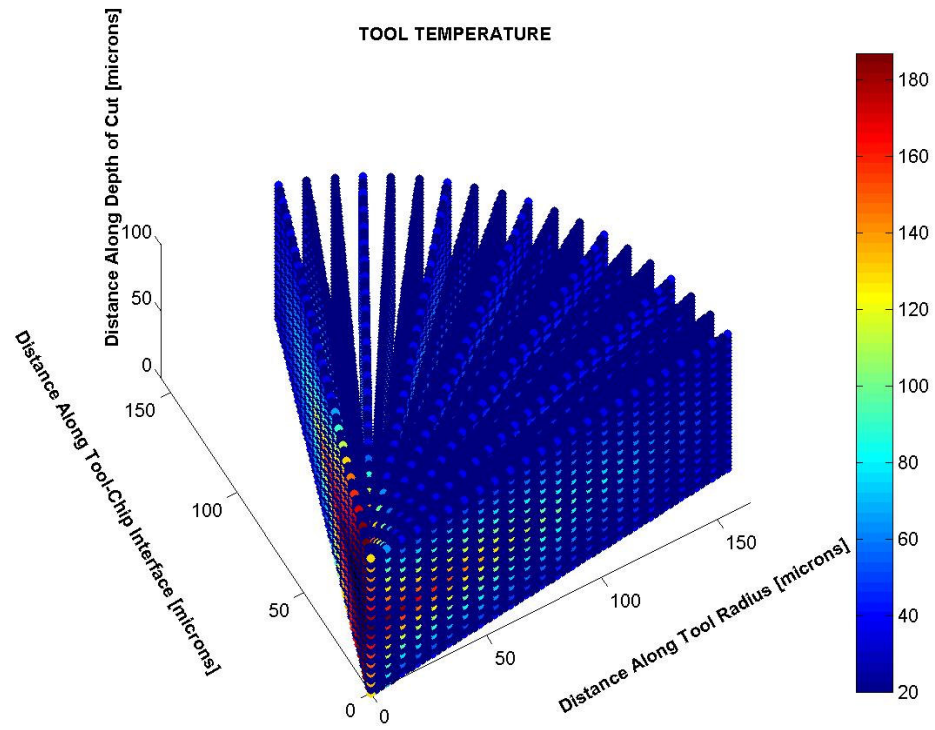
Fig. A.56: Validation for case 56. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



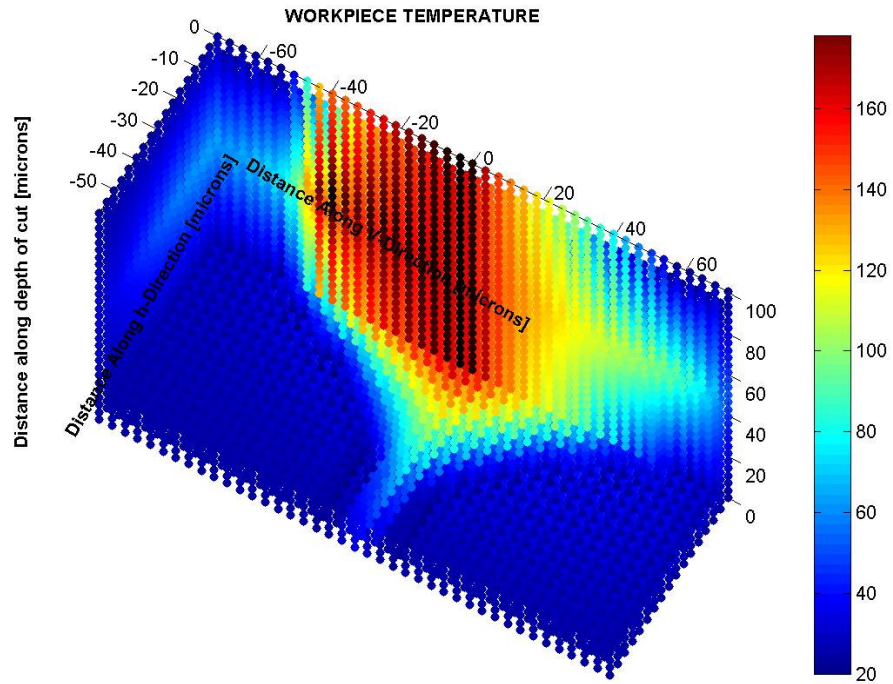
(a)



(b)

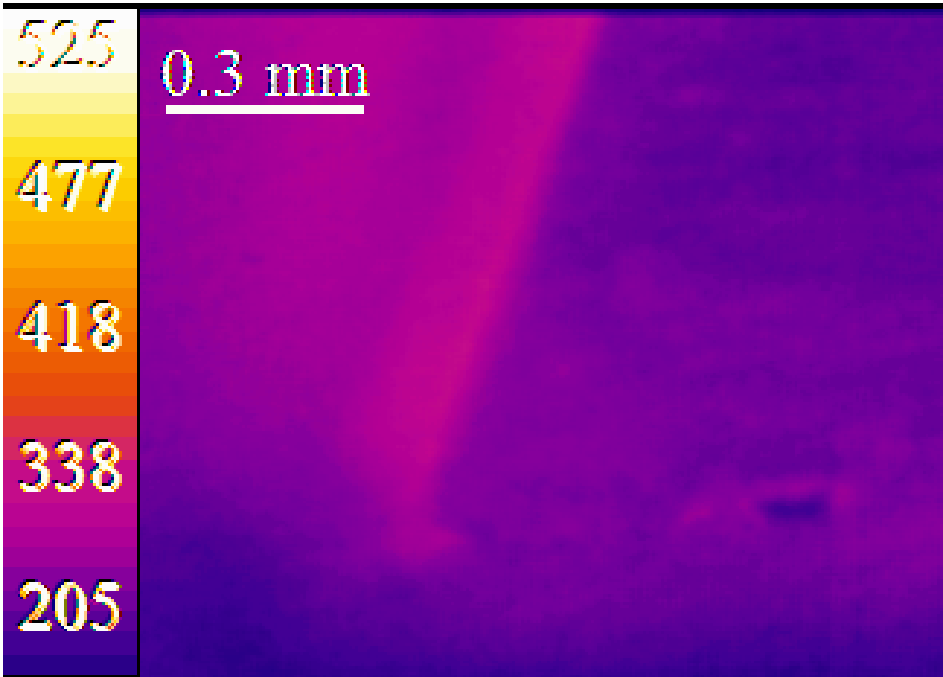


(c)

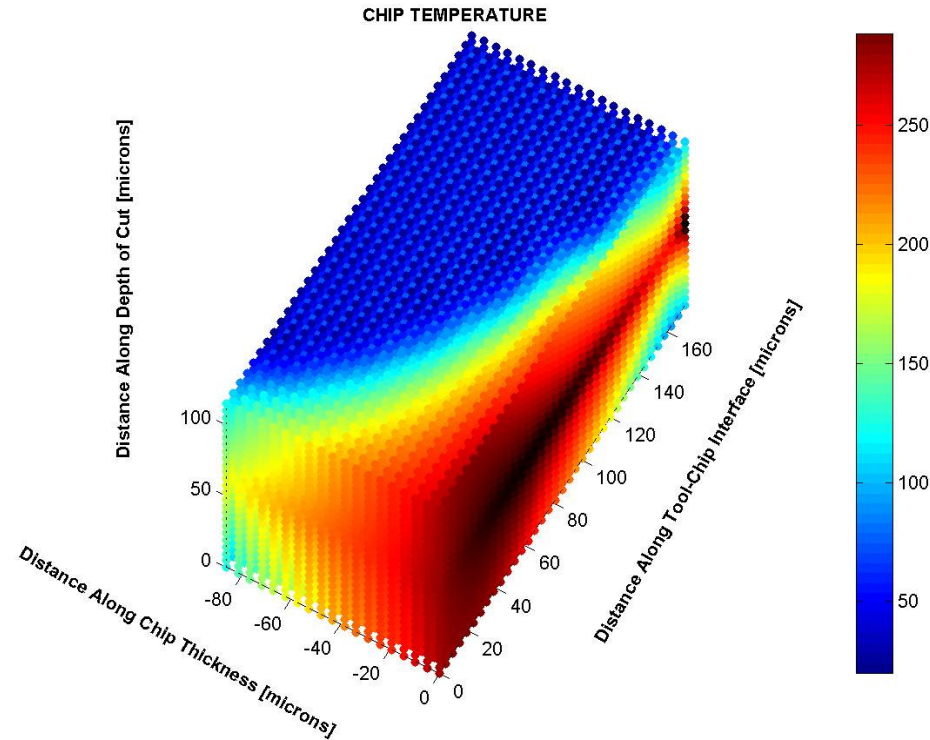


(d)

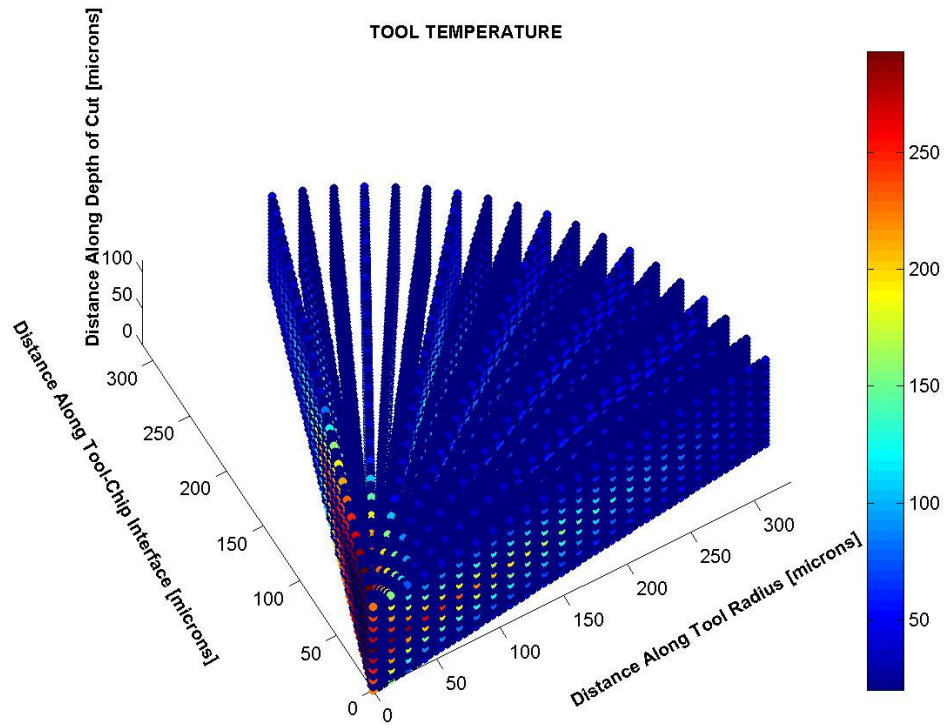
Fig. A.57: Validation for case 57. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



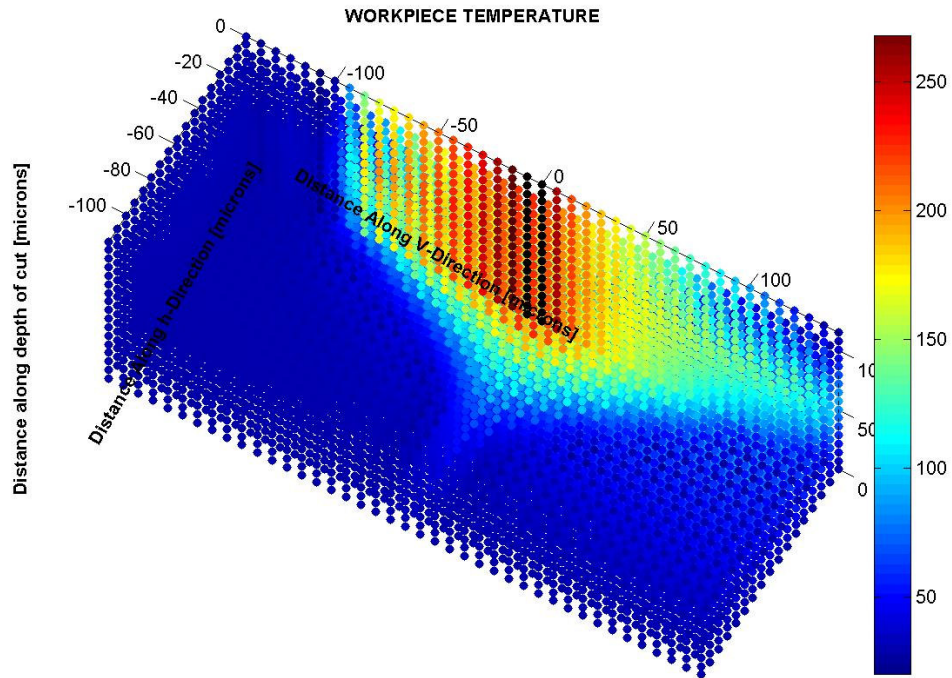
(a)



(b)

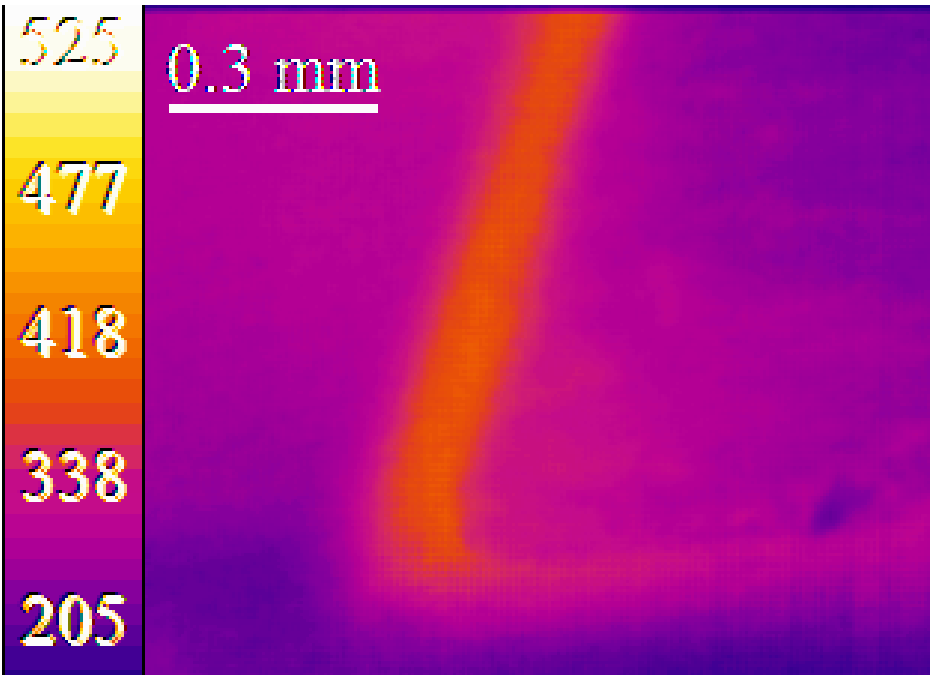


(c)

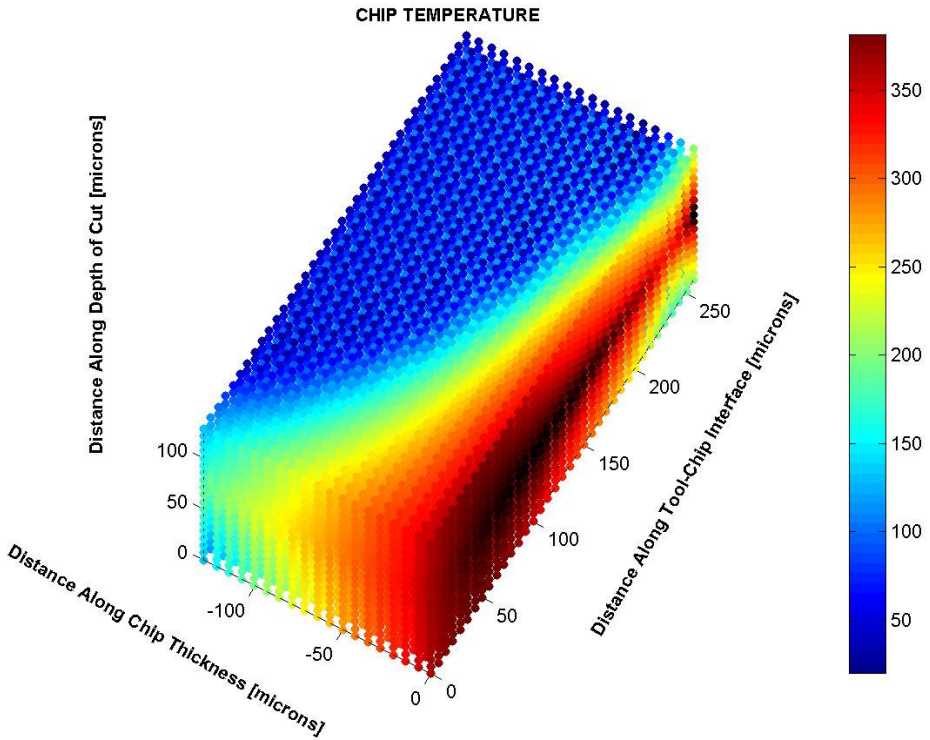


(d)

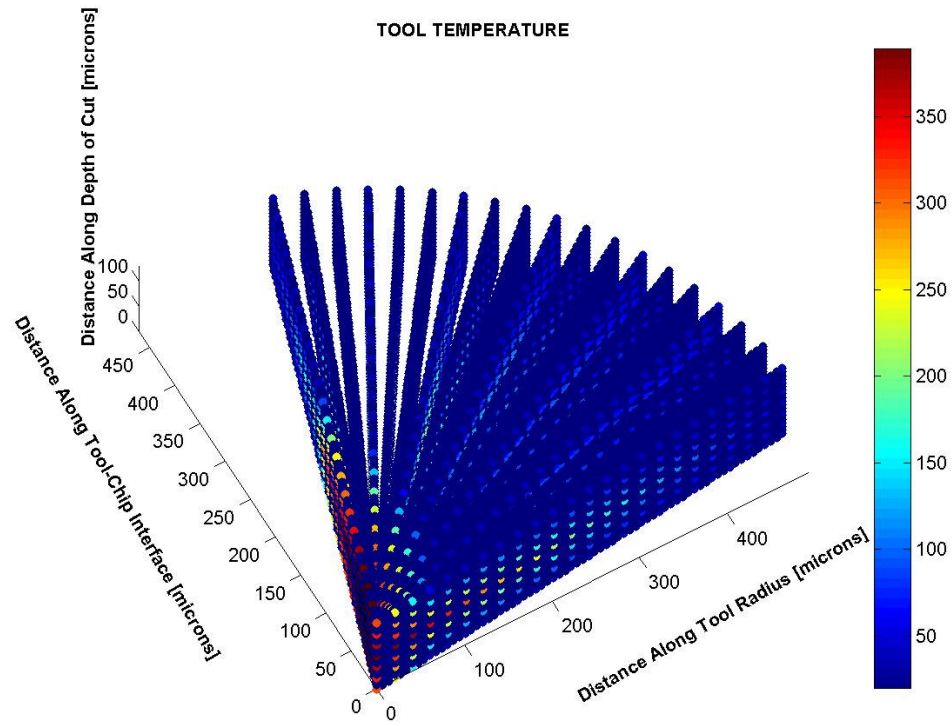
Fig. A.58: Validation for case 58. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



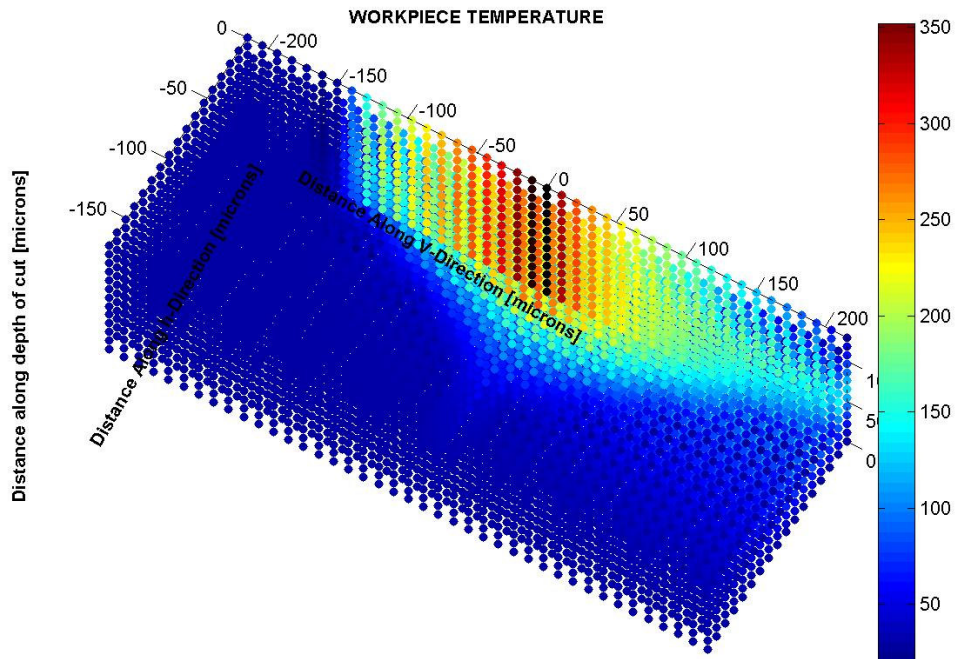
(a)



(b)

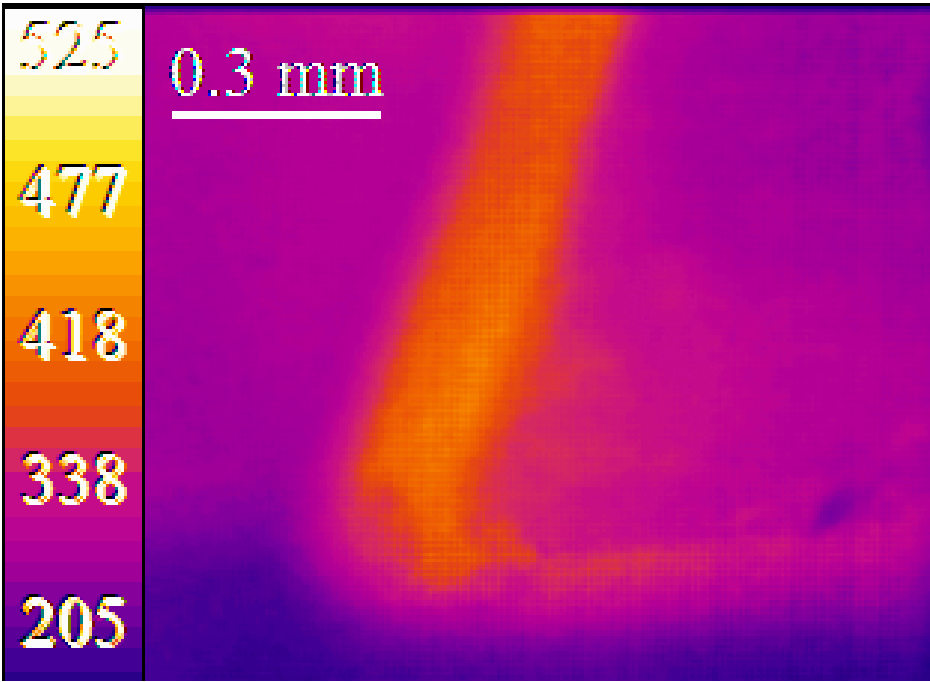


(c)

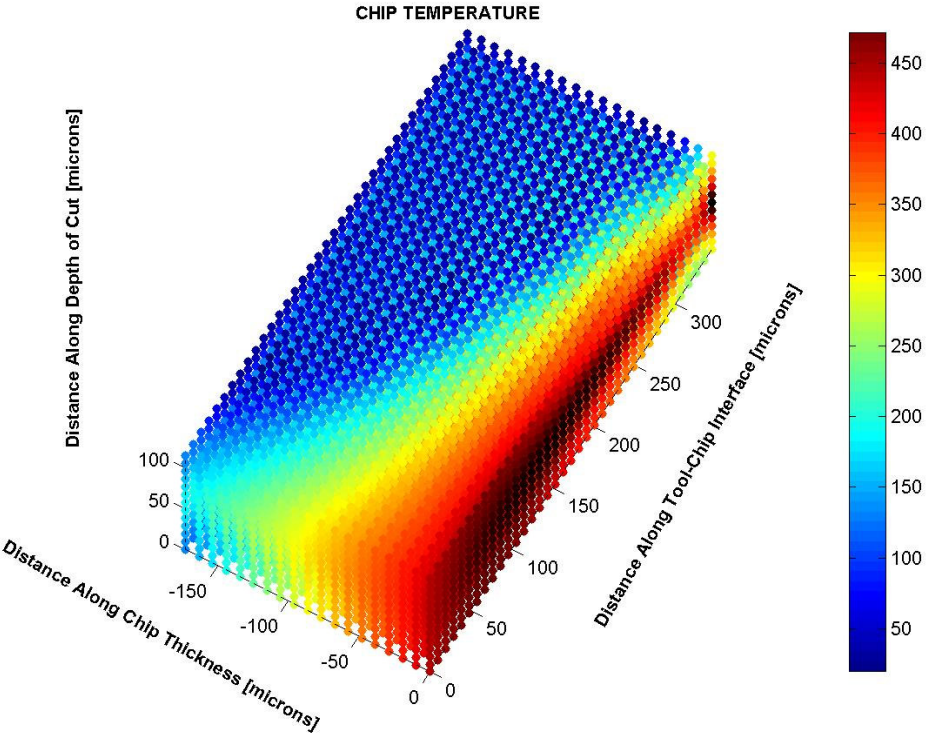


(d)

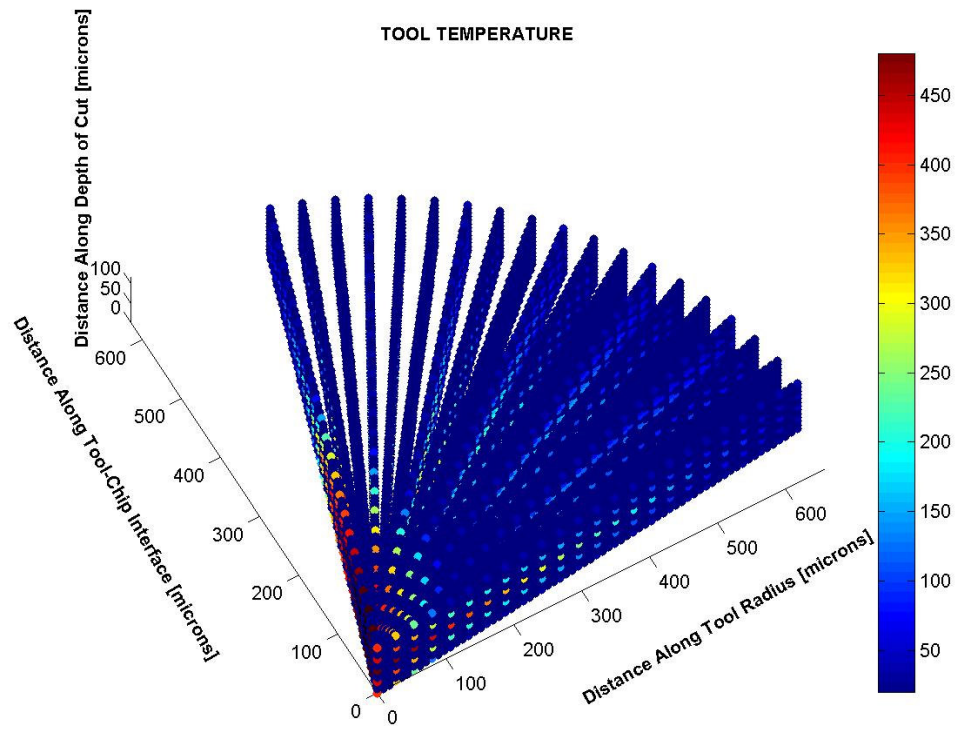
Fig. A.59: Validation for case 59. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



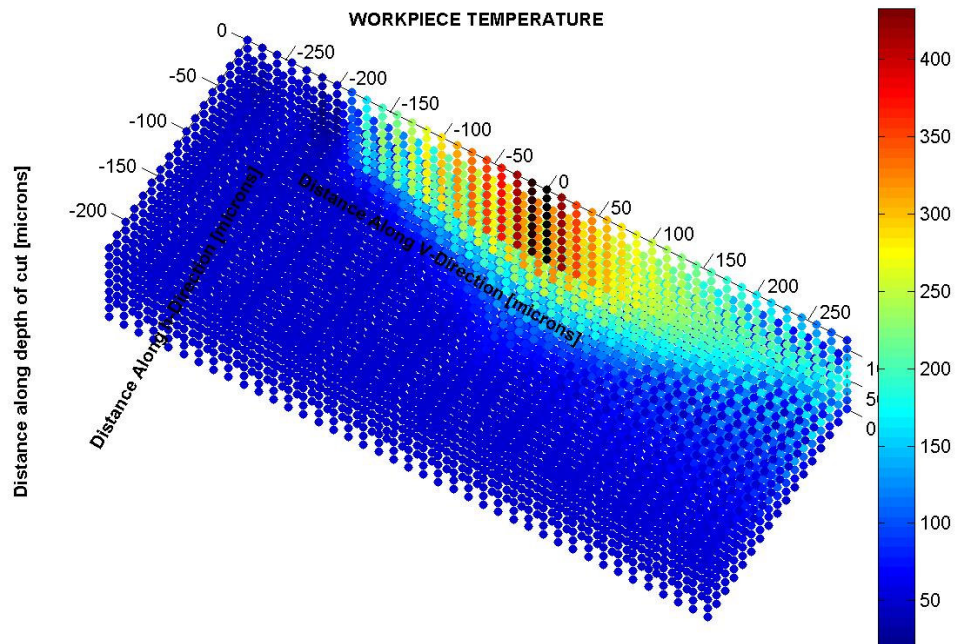
(a)



(b)

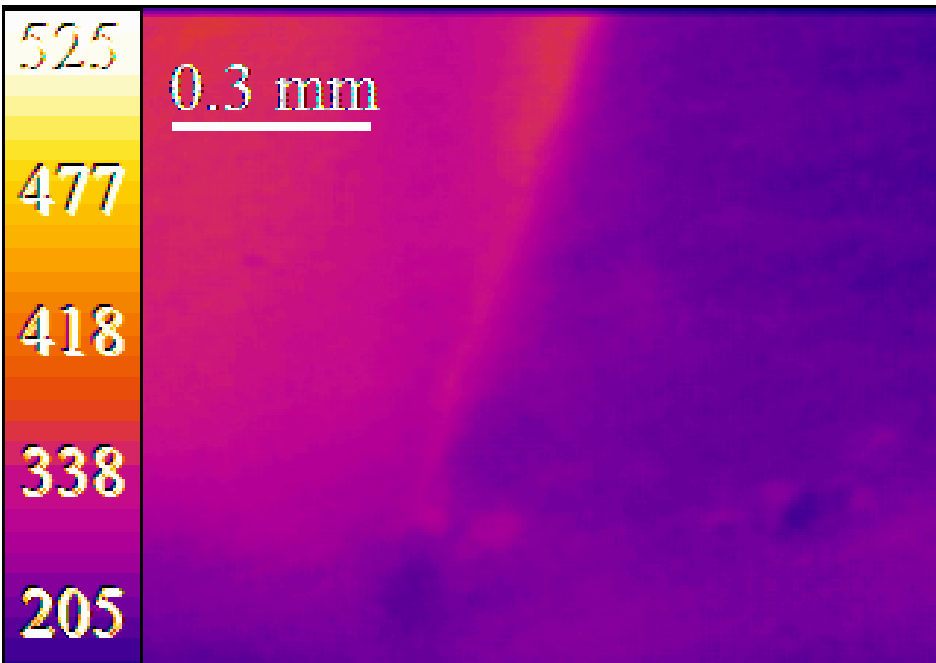


(c)

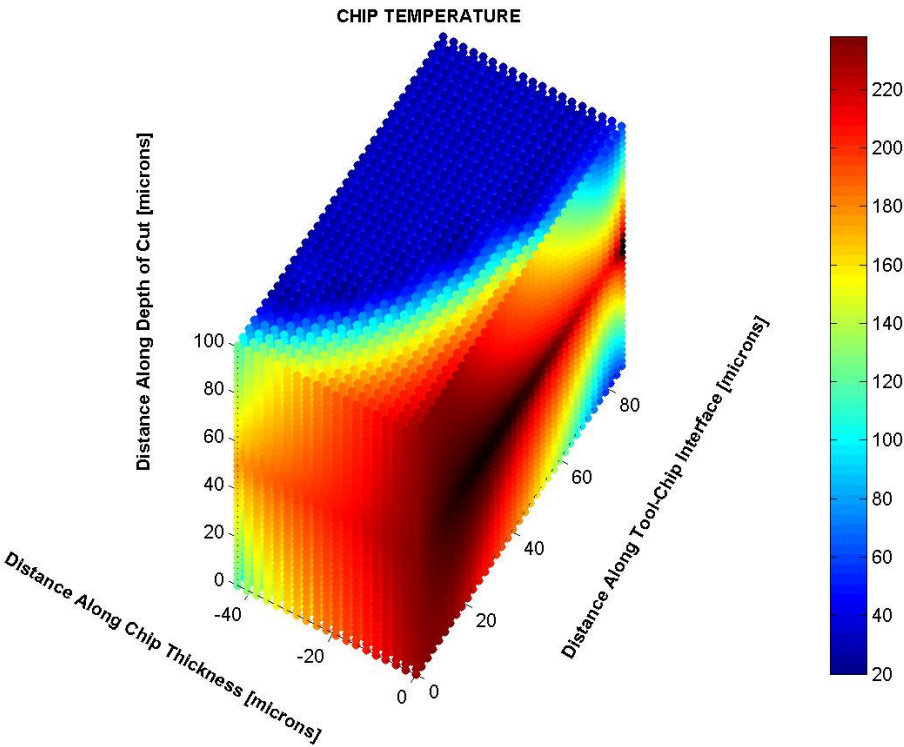


(d)

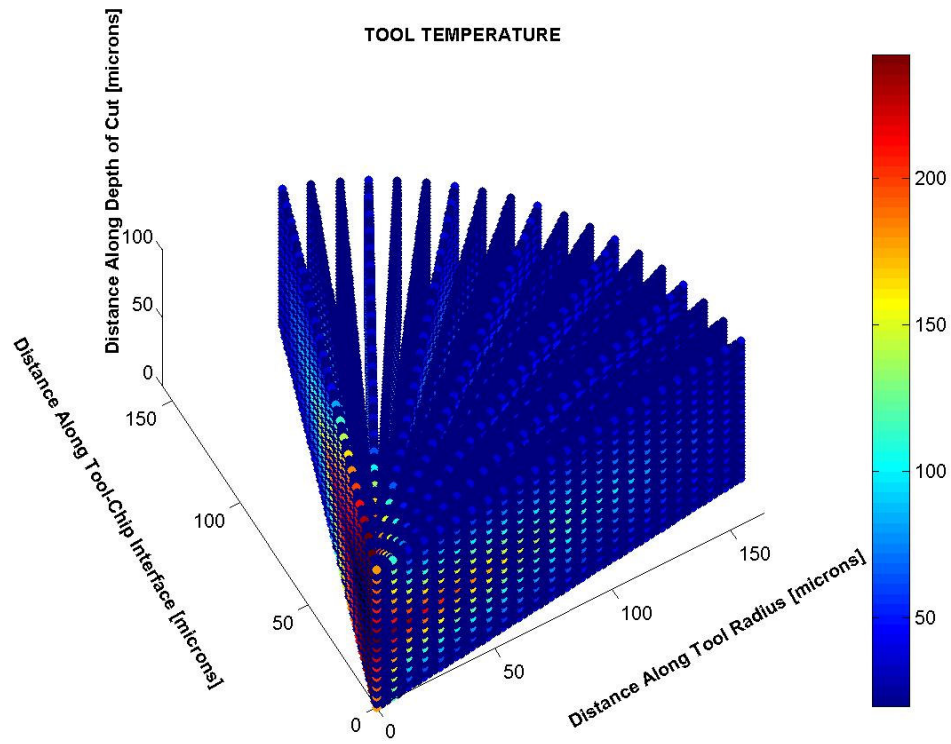
Fig. A.60: Validation for case 60. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



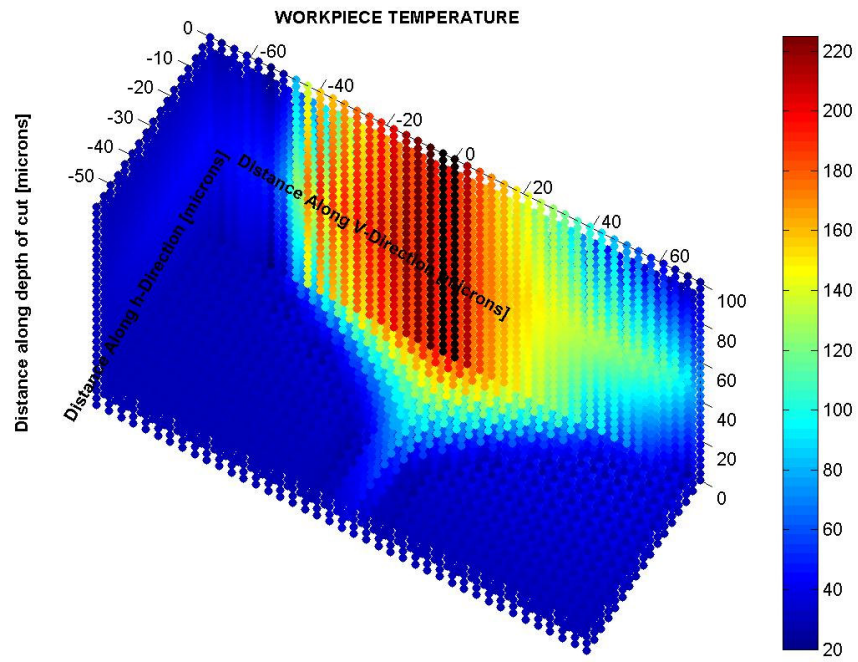
(a)



(b)

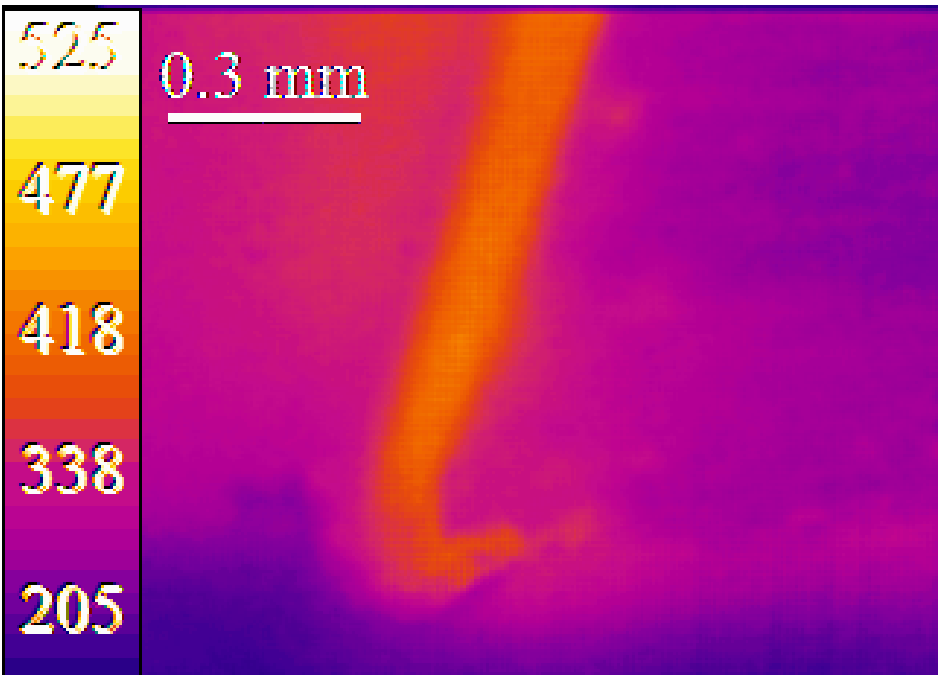


(c)

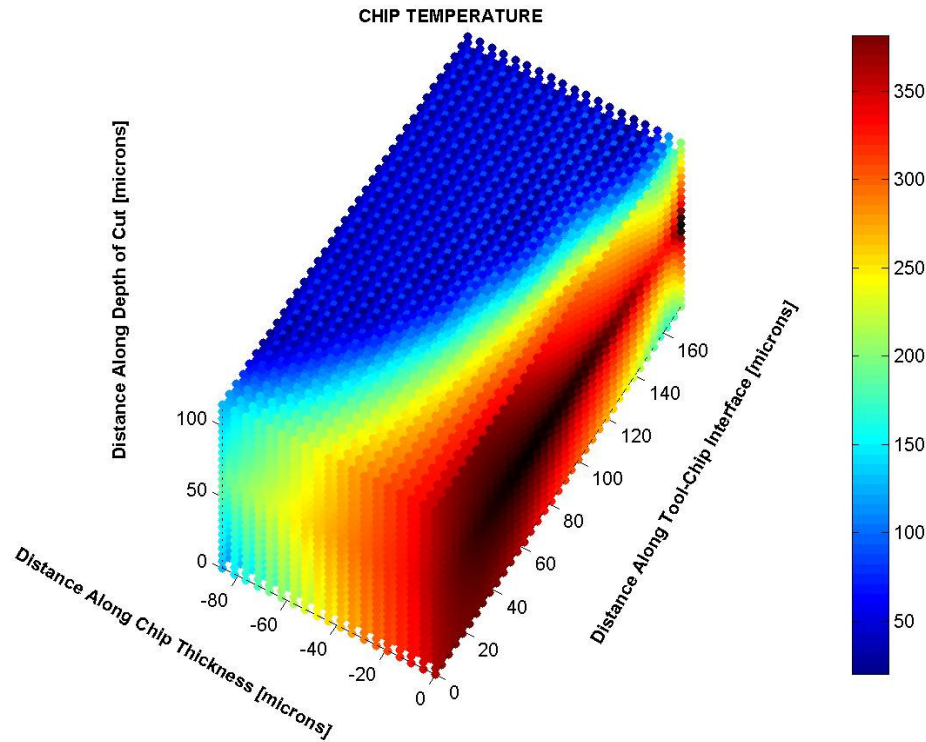


(d)

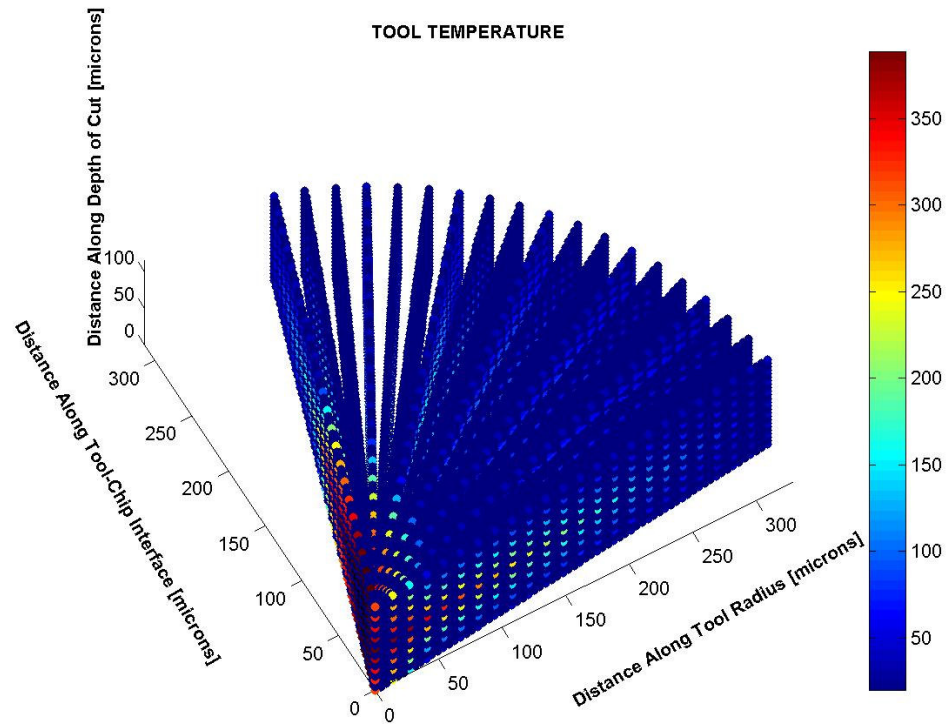
Fig. A.61: Validation for case 61. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



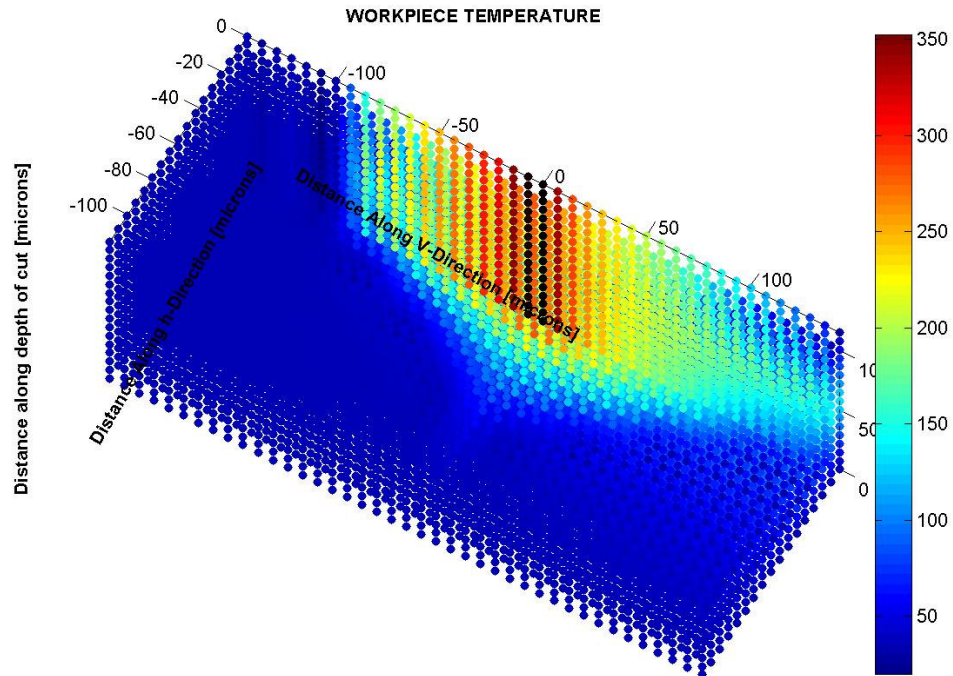
(a)



(b)

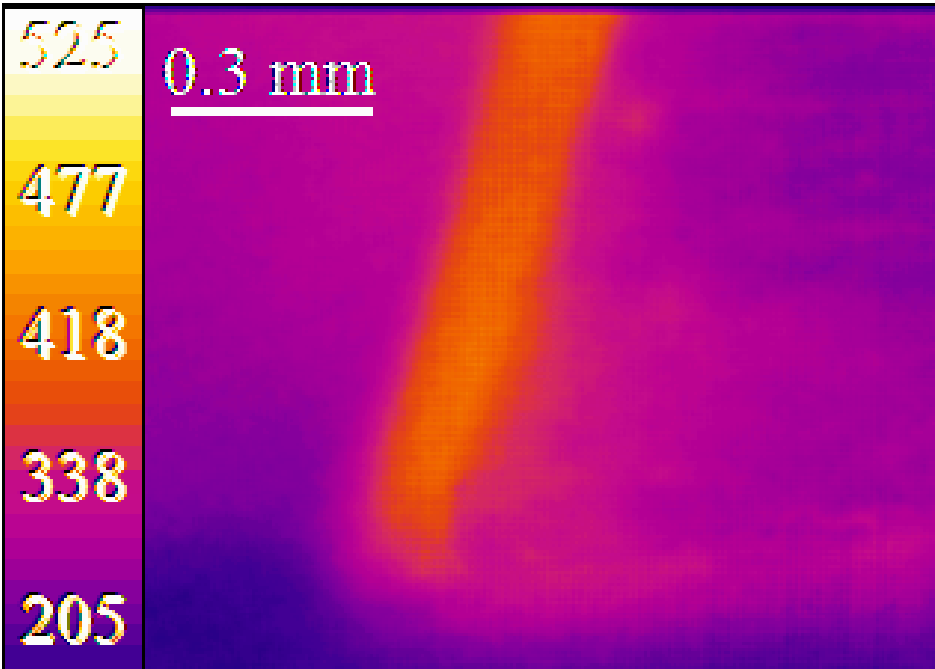


(c)

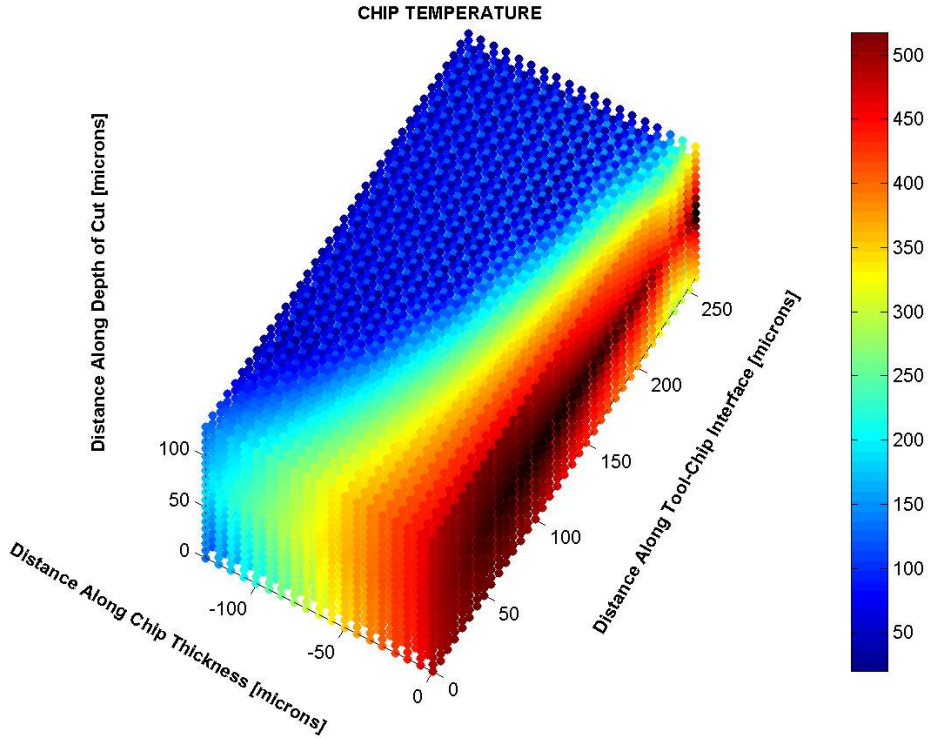


(d)

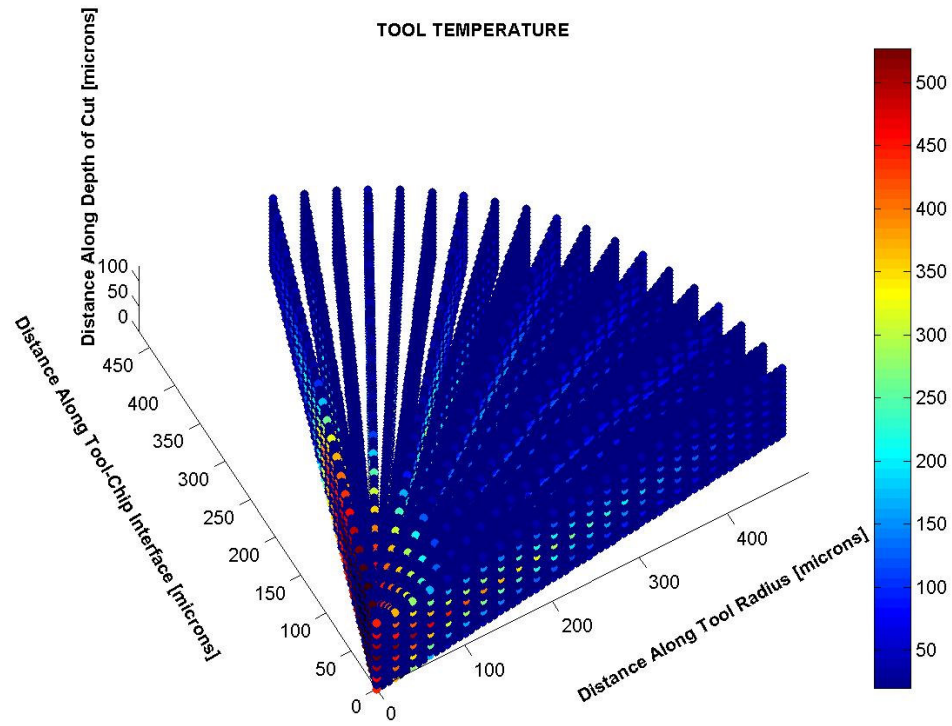
Fig. A.62: Validation for case 62. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



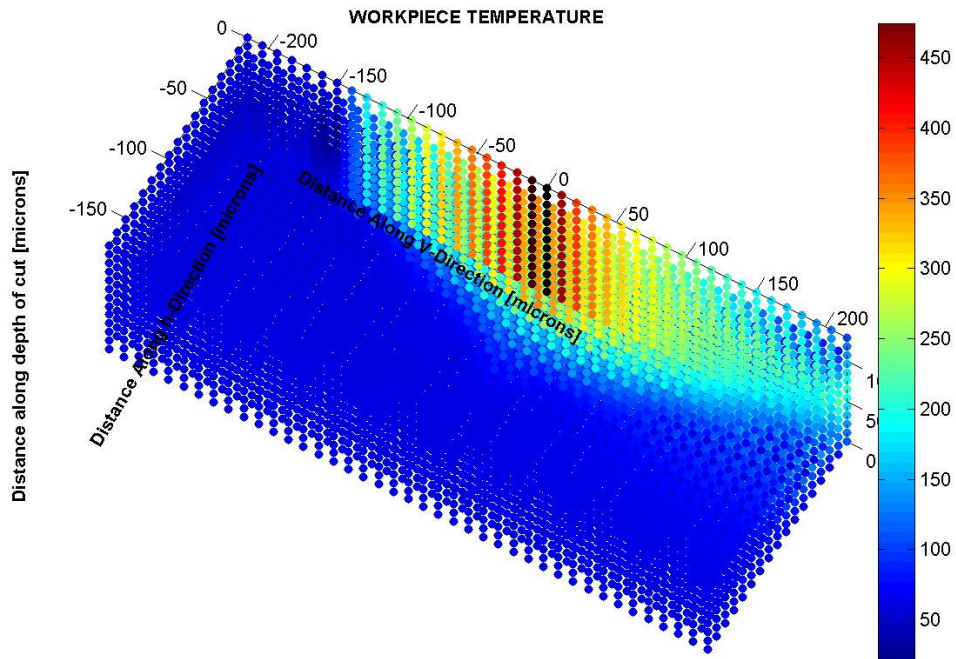
(a)



(b)

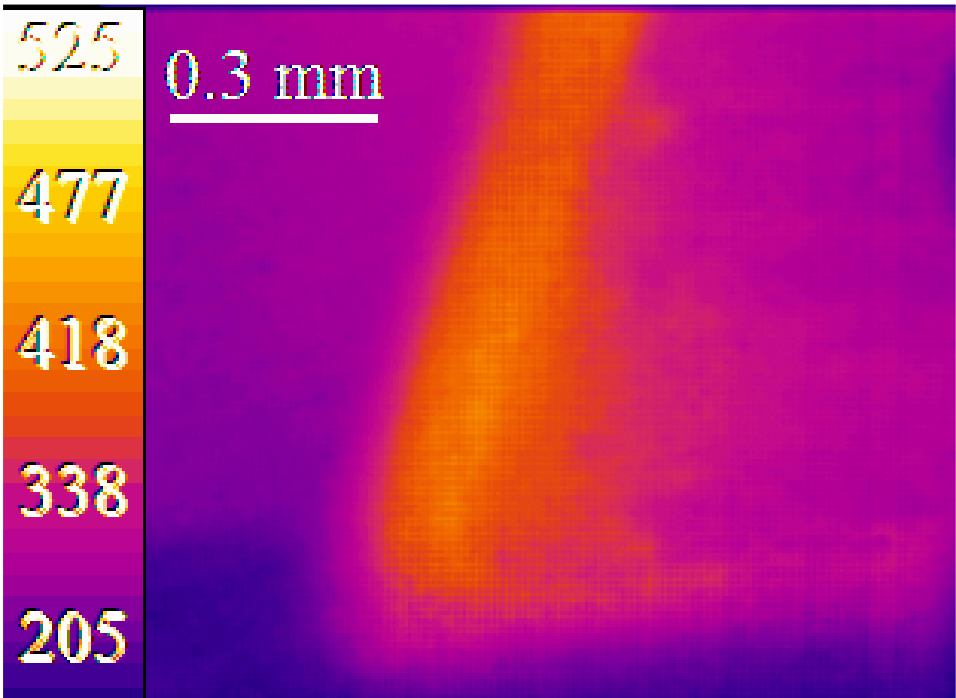


(c)

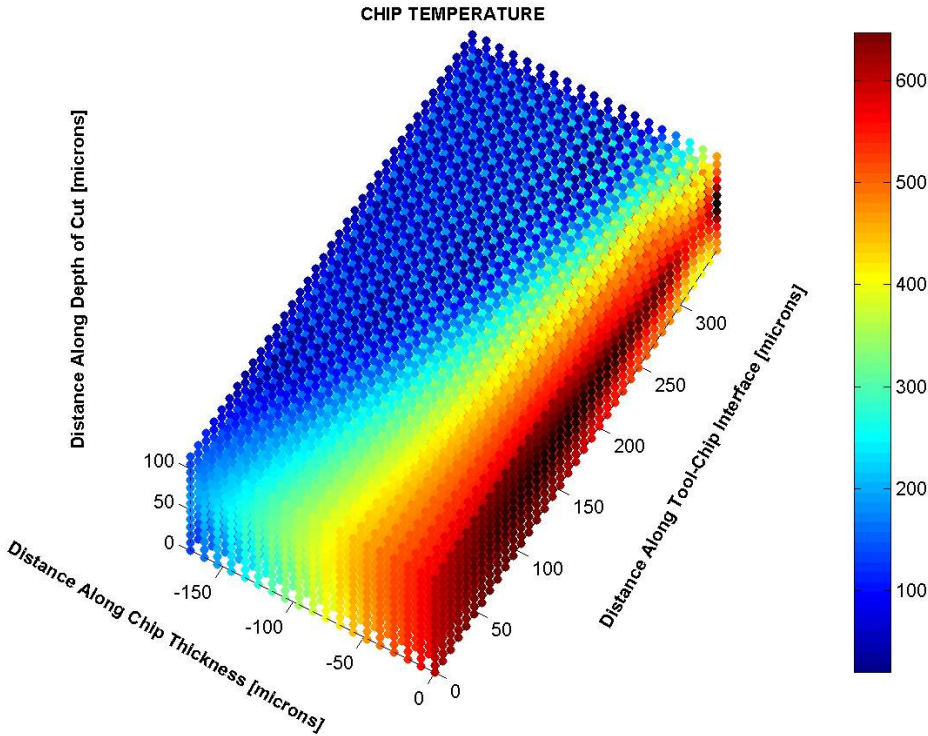


(d)

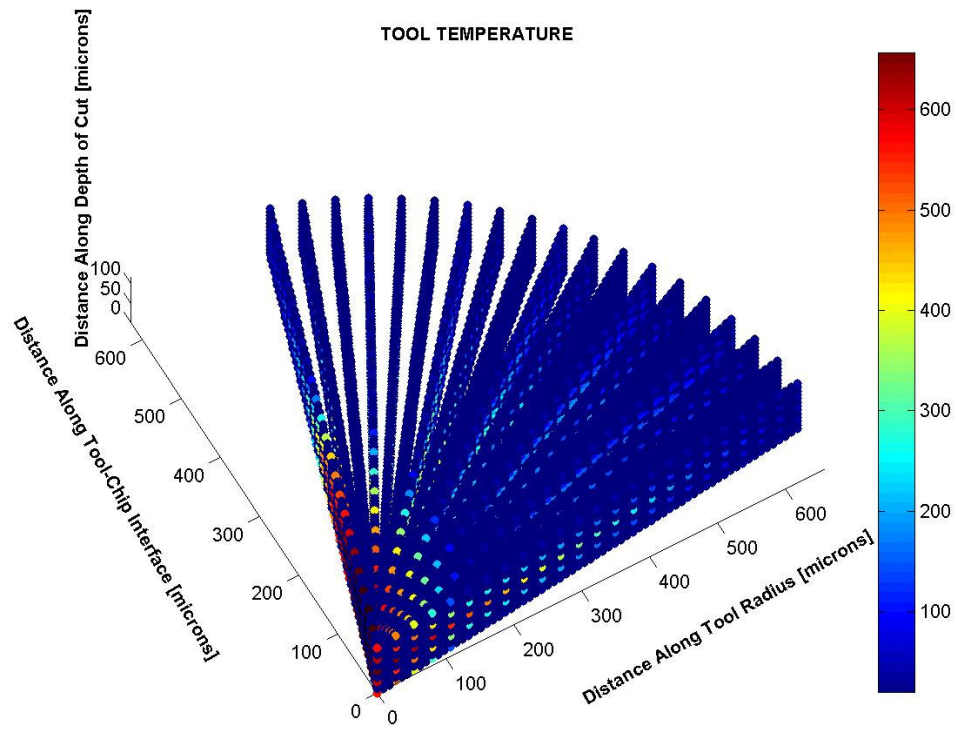
Fig. A.63: Validation for case 63. (a) Experimental result for the tool temperature field (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece



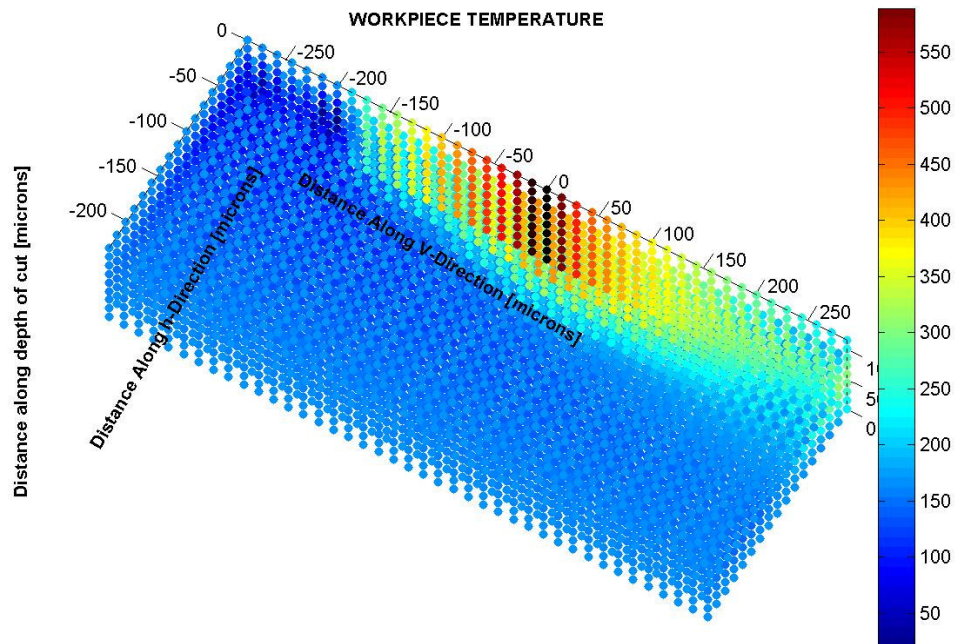
(a)



(b)



(c)



(d)

Fig. A.64: Validation for case 64. (a) Experimental result for the tool temperature field
 (b) Temperature prediction for the chip, (c) Tool, and (d) Workpiece

VITA

Durul Ulutan completed his high school education in Karşıyaka Anadolu Lisesi, İzmir, Turkey, in 2001. He was in top 1% in college selections, which he chose to study in Koç University. He received his B. Sc. degree in Mechanical Engineering with an honors degree, in 2005.

Between September 2005 and August 2007, he has been involved in M. Sc. program in Mechanical Engineering at Koç University, with teaching and research assistantship responsibilities. He has been involved within an exchange program between January-July 2006 in University of British Columbia, BC, Canada.