**INTRODUCTION**

In contrast to Precision Glass Molding (PGM), Nonisothermal Glass Molding (NGM) has recently promised an economically viable technology for manufacturing complex precision components made of glass. Optical glass components produced by such advanced replication-based technology can be found in numerous applications such as interior automobile components, 3D displays, lighting, and sensor devices. The fundamental distinction of this technology, compared to PGM, is the ability to separate the molding cycle from the time-consuming heating and cooling stages. Thanks to this beneficial property, it enables the hot-forming process at higher glass temperature while the temperature of the mold can be chosen significantly lower. The technological benefits, accordingly, are short process cycle, prolonging mold lifetime, flexible selection of mold materials and scalability.

In NGM process, the temperature difference between glass and mold bodies is significant. Consequently, the heat transfer occurring at the glass-mold interface, i.e., thermal contact

**Abstract**

Heat transfer at the interfacial contact is a dominant factor in the thermal behavior of glass during nonisothermal glass molding process. Recent research is developing reliable numerical approaches to quantify contact heat transfer coefficients. In most previous studies, however, both theoretical and numerical models of thermal contact conductance in glass molding attempted to investigate this factor by either omitting surface topography or simplifying the nature of contact surfaces. In fact, the determination of the contact heat transfer coefficient demands a detailed characterization of the contact interface including the surface topography and the thermomechanical behavior of the contact pair. This paper introduces a numerical approach to quantify the contact heat transfer by means of a microscale simulation at the glass-mold interface. The simulation successfully incorporates modeling of the thermomechanical behaviors and the three-dimensional topographies from actual surface measurements of the contact pair. The presented numerical model enables the derivation of contact heat transfer coefficients from various contact pressures and surface finishes. Numerical predictions of these coefficients are validated by transient contact heat transfer experiments using infrared thermography to verify the model.

**KEYWORDS**
infrared thermography, nonisothermal glass molding, surface characterization, thermal contact conductance
conductance, becomes the dominant mechanism influencing the thermal behavior of the contact pair. After being brought into contact with the cold mold, the glass temperature largely reduces, even up to hundreds of Kelvin. Such temperature drop of the glass at the contact surface certainly affects the form accuracy and the optical properties of molded glass components. Early studies indicated the influences of the glass temperature on the final shape of the molded lenses. Likewise, Pallicity et al revealed necessity of pressure-dependent thermal contact conductance for the simulation of the glass molding to precisely predict the form deviation and the birefringence of molded lenses. Particularly, a recent work by Kreilkamp et al emphasized the major role of the interfacial temperature on the chill ripple formation and the form deviation of the molded glass optics. Therefore, if the thermal contact conductance is known beforehand, the general prediction of the form accuracy and the optical properties of the molded glass components is enhanced.

There has been very limited research on quantification of the thermal contact conductance for glass molding processes, though. In most of the earlier works, the thermal contact conductance was commonly assumed without adequate experimental justifications. Such deficit is likely due to the complexity of the contact heat transfer experimental setup for glasses. Several investigations for quantifying thermal contact conductance are performed, nevertheless, only at low contact pressure. Recently, a conjugate gradient method combined with infrared thermography has been introduced by the authors. By this method, transient contact heat transfer coefficients over a wide range of molding pressures, interfacial temperatures and surface finishes were experimentally determined.

Beside the experimental studies, modeling the contact heat transfer in glass processing is also conducted by using analytical solutions and numerical approaches. However, these models either neglect the surface topography of the contact pair or simplify the nature of the contact surfaces. In this regard, the computation of contact heat transfer coefficient by means of modeling approach is deficient if the actual topography of the contact surfaces is not considered.

The aim of this paper is to apply and validate an existing numerical approach regarding the quantification of thermal contact conductance in context of glass-molding processes. The approach permits modeling both actual surface topographies and the deformation behaviors of the contact pair, i.e., the glass and mold, to overcome the deficiencies of the current modeling approaches. More specifically, this approach succeeds in dealing with the microscale simulation of contact mechanics and the thermal behavior of three-dimensional (3D) topographies gained by noncontact optical surface measurement. Experiments are carried out to confirm the validity of the proposed approach. The numerical model promises a reliable simulation tool to obtain the thermal contact conductance in glass molding, while expensive efforts on performing contact heat transfer experiments can be avoided.

2 | EXPERIMENTAL SETUP

In order to validate simulation outcomes, experimental results are prerequisite. In this study, the determination of the contact heat transfer coefficients (CC) is relied on an experimental setup established by the authors. The setup uses an infrared thermographic camera (IR-camera) to enable transient temperature measurements, from which CC are derived. Details of the experimental approach have been fully discussed in the work of Vu et al. The objective of this chapter is to highlight essential aspects of the experimental setup.

Figure 1 presents a schematic description of experimental setup for the determination of CC. The experiments are conducted in the press machine Füller GT52, a machine model specifically designed for the NGM process. The glass is placed in between two mold specimens. Borosilicate SUPRAX® 8488 glass and temperature-resistant 1.2787 stainless steel (AISI431) are chosen for this study. The contact heat transfer coefficients are derived from the transient surface temperatures of both glass and mold measured at the positions adjacent to the interface during contact. To minimize the spatial error by the thermographic measuring technique, the upper
mold was fixed permanently in space; therefore, the pixels on the mold surface chosen for the data acquisition by the IR-camera remain at constant positions during the entire measurement. The glass specimen is brought into contact with the upper mold by the upward movement of the lower axis. The PID controller of the machine controls the force exerted, corresponding to the predefined contact pressure chosen for individual experiments.

In addition, particular manipulations are aimed to minimize ambient influences on tracking temperatures of the specimens. Three heating devices are equipped to the press machine for precisely controlling the preset temperatures of the specimens. On the one hand, the upper mold is mainly heated up by a heating coil placed at the top of the mold. Besides, an induction ring is supplemented surrounding the mold surface. This induction ring acts as a compensation heater to reduce the transverse heat loss to the environment. Accordingly, the combined heating concept leads to homogeneous temperature distribution in the upper mold (Figure 2A, left). On the other hand, the glass is heated up in an oven, placed slightly below the pressing position. The oven is set at the predefined temperature with an accuracy of ±0.5°C. Sufficiently long heating time, approximately 20 minutes, is cared for satisfying the temperature homogeneity of the glass specimen. After heating, the glass is driven out of the oven vertically and quickly brought into contact with the upper mold within less than a second to reduce heat losses to the environment. Furthermore, thin black layers are coated on the surfaces of both specimens to minimize influences of ambient radiation being reflected from the measuring surfaces.

During glass pressing, the transient temperature fields of the glass and the upper mold are captured by the IR-camera, the VarioCAM® HD head provided by InfraTech. The camera can detect the IR radiation emitted from measuring objects in the spectral range from 7.5 ± 14 μm and determines temperature fields of both investigating specimens for further thermal analysis. This device enables a temperature measuring range up to 2000°C and the measurement accuracy of ±1°C. Full-frame data acquisition of 30 Hz with high resolution of 1.024 × 768 pixels is chosen to record the temperature fields.

A thin black coating is layered on the surfaces of both glass and upper mold specimens, as mentioned earlier not only to reduce the influences of the ambient radiation, but to precisely determine the emissivity of the measuring objects. This is a high-temperature resistance paint with emissivity of approximately 0.9. In addition, due to a slight deformation of the surfaces under the contact pressure, the temperatures were measured adjacent to the contact interface, which is 250 μm further away from the contact line of both glass and mold, to avoid the noise of measured data signals. Figure 2A provides an example of the temperature fields recorded by the IR-camera. Here, the temperature fields of the contact pair are exhibited at two sequential stages: (a) before pressing, to exhibit the temperature distribution of the upper mold when the glass is being heated in the oven further below the contact area (Figure 2A, left); and (b) during pressing, to demonstrate the temperature fields at first contact when the glass is quickly brought into the mold (Figure 2A, middle), and after 2 seconds of contact time when a steady-state CC is gained (Figure 2A, right). Using the IR-images, the temperature changes of the contact pair during the pressing stage are plotted. As illustrated in Figure 2B, the recorded temperature profiles of the glass and mold are gently smooth over the entire pressing period without any disturbing noise. Thereupon, such temperature inputs promise an accurate determination of the derived contact coefficients and stability of the inverse heat transfer solution.

In addition to the actual experiments, 3D topographical surface profiles of the glass and upper mold are measured by noncontact optical interferometry. Figure 3 displays the roughness of four mold surfaces by different machining methods. Two relatively rough surfaces manufactured with rough ($R_a = 0.47 \, \mu m$-mold 1) and fine grinding ($R_a = 0.16 \, \mu m$-mold 2) are exhibited in Figure 3A,B, respectively. In addition, ultra-precision mold surface finishes machined by ultrasonic-assisted diamond turning, typically used for the mold manufacturing in nonisothermal glass molding, are provided in

---

**FIGURE 2** (A), IR-image of temperature fields; and (B) Temperature changes measured at 250 μm from the contact interface during glass pressing. (Process parameters: initial temperature of the contact pair-640°C/450°C, contact pressure-20 MPa)
Figure 3C,D. Figure 3D exhibits the newly machined surface after the diamond turning ($R_a = 6 \text{ nm}$-mold 4). In contrast, the surface presented in Figure 3C is initially machined by diamond turning and then undergone several glass molding cycles. It is observed that the roughness of mold 3 sharply decreases after the first molding cycles, mainly due to plastic deformation of surface asperities and wear phenomena such as oxidation, glass adhesion or scratch, and afterwards remains almost unchanged. Figure 3C provides the surface roughness measured after 20 molding cycles ($R_a = 35 \mu\text{m}$-mold 3). Such surface profiles are used to investigate the influences of the surface alteration on the contact heat transfer mechanism in both experimental and numerical approaches. Considering the glass specimen, identical profiles manufactured by fine polishing with a roughness of $R_a = 2 \text{ nm}$ are used for all experiments.

To quantify pressure-dependent thermal contact conductance, experiments are performed with applied loads ranging from 2 to 20 MPa. Such pressure range is equivalent to the pressing forces commonly utilized in glass-forming processes. The glass pressing process contains two sequential steps: the position control and force control. First, the glass is quickly brought into contact with the upper mold and pressed until the predefined force is attained; afterwards, this force is kept constant for another 10 seconds and respective temperature changes of the glass and mold surfaces are recorded. For the validation of simulation results, the mold temperature of 450°C and glass temperature of 650°C (corresponding viscosity $\log \eta \approx 10.5 \text{ dPas}$) are chosen.

### 3 | NUMERICAL MODELING

The numerical model presented in this research can be divided into three major parts, including surface geometry, mechanical modeling and thermal modeling, and has been introduced in detail by Frekers et al. Based on the presented numerical model, simulations of contact heat transfer are performed by a self-written MATLAB program. The simulation tool has been validated in previous research. Modeling of the surface geometry, mechanical deformation and thermal modeling are consecutive parts of the simulation. Hence, this chapter begins with the modeling of the surface geometry and following mechanical deformations of the contact surfaces. Lastly, the thermal modeling containing the final deformed surfaces calculated from the mechanical modeling as inputs for the determination of CC is elucidated.

#### 3.1 | Modeling of surface geometry

Either measured or generated surfaces can be utilized for the modeling of the surface geometry. The latter employs characteristic parameters such as roughness or mean slope
to generate the investigated surface. Regardless, both surface types are described by a half-space model, i.e., the 3D surface geometry is represented by using column and rows of a two-dimensional matrix as $x$, $y$-position and corresponding matrix values $z(x, y)$ as the surface height. This procedure, compared to the surface modeling using Finite Element Method (FEM), permits beneficially larger surface areas to be investigated. In addition, the amount of data required for the numerical computation is significantly reduced.

As mentioned in the experimental approach, four different mold surfaces are investigated (Figure 3), using the same measured glass surface for each contact mold. To reduce computational loads, the initial resolution of $640 \times 480$ pixels is reduced to a lower resolution ($320 \times 240$ pixels) by fast-Fourier-transformation (FFT) and following reduced to a quadrangular shape of $240 \times 240$ pixels for modeling. Prior investigations have shown that such resolution is a good compromise between accuracy and computational cost. Figure 4 exhibits an example of the surface sections with the resolution of $240 \times 240$ elements modeled from the oxidized mold surface (mold 3, Figure 3C) and the glass surface.

### 3.2 Mechanical modeling

The mechanical modeling used in this research is mainly based on work presented by Tian et al. and Goerke and Willner. Similar to the method for surface representation, the half-space method is utilized as a framework for the elasto-plastic deformation. The mechanical model couples the applied load with the total displacement—the sum of the elastic and plastic deformation—of two contacting surfaces. First of all, based on the measured profiles, mean planes of the measured glass and mold surface are determined. Then, they are used as the origins of the coordinate systems for the respective surface profile ($z_G(x, y)$ and $z_M(x, y)$), by which the mean separation length $\delta$ is defined as the distance between the two mean planes (Figure 5A). The present algorithm is developed based on the computation of the surface deformation and the resulting contact pressure for one single surface. For this reason, the measured surfaces are combined into one single surface profile ($z_G(x, y) + z_M(x, y)$), and they are partitioned after the deformation calculation. To realize the contact spots and the resulting contact pressure, the combined surface is moved against a rigid plane with the distance $\delta$.

![Figure 4](image1.png) Modeled surface sections of the mold 3 (top) and glass (bottom) for numerical investigation

![Figure 5](image2.png) 2D schematic representation of contacting surfaces. (A) Surface profiles of glass and mold; (B) Simplified contacting surfaces; and (C) Representation of surface overlaps
(Figure 5B). While the distance is reduced, more and more surface spots of the combined surface profile get into contact with the rigid plane (Figure 5C). The overlap with this plane is described by the matrix \( u_\varepsilon \):

\[
\varepsilon (x,y) = (z_G(x,y) + z_M(x,y)) - \delta. \tag{1}
\]

The matrix \( u_\varepsilon \) contains positive entries representing the actual overlap of the surfaces (spots in contact) and negative entries representing the missing distance to get respective surface spots into contact (no contact). Following, negative entries are set to zero, as these do not take part in the actual surface deformation.

Since both pressure distribution and surface deformation are unknown, an iterative procedure needs to be applied. By varying \( \delta \), the resulting surface overlap is obtained and serves as input for determination of the pressure distribution over the contact surface, defined by Equation (2). The contact pressure is resulted from elastic deformation of the surfaces and is calculated by introducing a rigidity matrix \( C \). This matrix correlates the effect of the contact pressure \( p \) at element \( \ell \) with the deformation \( u_\varepsilon \) at element \( k \).

\[
u_{\varepsilon,\ell}(x,y) = \sum_{k=1}^{\ell} C_{\ell k} \cdot p \cdot (x_k,y_k). \tag{2}
\]

The mathematical definition of the rigidity matrix and the considered material properties are described in the work of Goerke and Willner. However, since the displacement of the surface overlap \( u_\varepsilon \) is known by iteration from Equation (1), the pressure profile is solved by replacing the elastic deformation with the total surface overlap, yielding a set of linear equations:

\[
p = C^{-1} u_\varepsilon. \tag{3}
\]

This set of linear equations is successively solved with a Gauss-Seidel algorithm. Furthermore, the local cell pressure \( p \) cannot exceed the material micro-hardness \( H \), as at this pressure level the surface starts to deform plastically restraining further increases of the load. Hence, the algorithm limits the local pressure to a range between 0 and the micro hardmess \( H \). Concluding, the described procedure is embedded in an iteration loop in which the mean separation length \( \delta \) is varied until the target pressure is reached.

After the iteration, the total overlap is given in only one matrix (Equation 1) and following needs to be associated to the corresponding partners. First, the partitioning of elastic deformation, as indicated in the work of Tian et al., can be realized by defining the elastic strain on each surface as a function of the mechanical properties of the respective material:

\[
u_{\varepsilon,\ell} E_G = \frac{u_{\varepsilon,\ell} E_M}{1 - v_G^2} = \frac{u_{\varepsilon,\ell} E_M}{1 - v_M^2}. \tag{4}
\]

Here, the elastic modulus \( E \) and Poisson number \( \nu \) of each material are used to define the elastic strain. Then, Equation (4) contains two unknowns—the elastic displacements of glass \( u_{\varepsilon,\ell} \) and mold \( u_{\varepsilon,\ell} \) respectively. However, since the total elastic displacement is already known from the previously solved iteration, the partitioning of the elastic deformation of each surface is achieved by Equation (5).

\[
u_{\varepsilon,\ell} (x,y) = u_{G,\ell} (x,y) + u_{M,\ell} (x,y). \tag{5}
\]

Finally, for partitioning of the plastic deformation, it is assumed that the glass surface does not undergo the plastic deformation during contact at the temperature of investigations in this study. For glass, it is commonly considered to be elastic at temperatures below \( T_g \) and viscoelastic at molding temperature range above \( T_g \). The validity of this assumption is solidified based on the fact that the surface temperature of glass drops quickly near transition temperature \( T_g \) only after a few seconds being brought into contact with the cold mold (Figure 2). At this temperature, the glass surface turns out a brittle solid. Still, viscoelastic deformation can occur; however, such time-dependent deformation happens at a much larger time scale (>hundreds seconds) than the time to achieve the steady-state contact heat transfer condition, which is less than 2 seconds. Accordingly, it is reasonable to presume that the plastic deformation is solely taking place on the steel solid, e.g., the mold surface, defined by Equation (6):

\[
u_{\varepsilon,\ell} (x,y) = u_{\varepsilon} (x,y) - u_{\varepsilon,\ell} (x,y). \tag{6}
\]

Finally, the deformed surface profiles of the glass, \( z'_G (x,y) \), and the mold \( z'_M (x,y) \) are realized by Equation (7) and serves as a basis for the following thermal calculation.

\[
\begin{cases}
z'_G (x,y) = z_G (x,y) - u_{G,\ell} (x,y) \\
z'_M (x,y) = z_M (x,y) - u_{M,\ell} (x,y) - u_{M,\ell} (x,y).
\end{cases} \tag{7}
\]

### 3.3 Thermal modeling

After modeling the surface deformation, the half-space is extended to a three-dimensional body. It is enabled by discretizing the deformed surface profile in \( z \)-direction into discrete cells leading to three-dimensional matrix mesh grid. Cells, which are equal to one, represent solid material, while remaining cells, equal to zero, correspond to surface cavities. However, compared to the authors’ previous work where a homogenous mesh grid was used to discretize the whole domain, a refinement algorithm is developed for this study to reduce computational costs, while maintaining a high resolution of the surface profile. One refinement level equals the intersection of width, length and height of a cell, leading to eight subcells for each refinement level. Therefore, the \( x \) - and \( y \)-axis
resolution from the mechanical modeling (240 × 240) is chosen for the finest resolution of the length and width at the contact zone, whereas it decreases at distances further away from the contact interface. Such arrangement is demonstrated in Figure 6, emphasizing a higher refinement level with a dark grey tone. Also, a considerable large length-to-height ratio of the cell is chosen to resolve the nanoscale roughness (z-axis) of the contact surface geometries in glass molding, in conjunction with the measured surface area (x- and y-axis) in mm- or µm-scale. Depending on the actual surface roughness of the investigated molds (mold 1-4), the resolution of the cell height varies between 2-20 nm.

After mesh generation, the steady-state temperature field of the contact pair can be determined by solving the heat equation:

\[ \lambda \frac{\partial^2 T}{\partial x^2} + \lambda \frac{\partial^2 T}{\partial y^2} + \lambda \frac{\partial^2 T}{\partial z^2} = 0. \]  

(8)

In this study, numerical implementation for solving the temperature field is accomplished by means of an implicit finite difference scheme. Boundary conditions required for solving Equation (8) are depicted in Figure 6. The bottom of the lower specimen-the glass-is set to a constant temperature. This assumption is feasible because, as mentioned earlier in the experimental procedure chapter, the glass is heated in the oven after a sufficiently long time permitting a homogeneous temperature distribution of the glass body. In contrast, a heat flux boundary condition is applied on the top of the upper specimen-the mold tool. As the investigated surface area is range of less than 1 mm², relatively small heat fluxes in range of 0.15 W are applied. Besides, external surfaces and faces of noncontacting cavities are assumed as adiabatic boundary conditions. Finally, it is noteworthy to point out that, although the temperature changes of the contact pair in a short time scale are dominated by heat conduction through the glass-mold interface, natural convection and particular radiation would become more significant at the investigating temperature above 600°C. These phenomena, however, are not considered in this current thermal model but are sought in our future work.

Figure 7 presents an exemplary temperature field of a cross section at varying contact pressure. Based on the calculated steady-state temperature profile, the temperature of the top of the domain \( T_u \) can be determined, and following the thermal contact conductance \( h_c \) is quantified by using Equation (9):

\[ \dot{q}'' = \frac{T_u - T_l}{R_c} \iff R_c = \frac{T_u - T_l}{\dot{q}''} = \frac{l_G}{\lambda_G} + \frac{l_M}{\lambda_M} + \frac{1}{h_c} \]

\[ \Rightarrow h_c = \left( \frac{T_u - T_l}{\dot{q}''} - \frac{l_G}{\lambda_G} - \frac{l_M}{\lambda_M} \right)^{-1}. \]  

(9)

Here, the total thermal resistance \( R_c \) is correlated with the temperature difference of the upper \( (T_u) \) and lower \( (T_l) \) bodies, and the applied boundary heat flux \( (\dot{q}'') \). Following, the resistance is divided into three major contributions: heat conduction resistance in upper and lower bodies, defined by the ratio of the corresponding body length \( (l) \) and its thermal conductivity \( (\lambda) \), and the thermal contact conductance itself. Rearranging terms, then, yields the final relation to determine the thermal contact conductance \( h_c \) as a function of the known properties.

4 | RESULTS AND VALIDATION

4.1 | Experimental results

Figure 8 demonstrates the transient contact heat transfer coefficient for an exemplary mold surface-mold 3. First, the
temperature changes of the glass and mold over the pressing time are recorded (Figure 8A). Such temperature data are used as inputs for the derivation of the transient contact heat transfer coefficient using conjugate gradient method (Figure 8B). Details of the inverse method and the corresponding computational algorithm used to determine the contact heat transfer coefficient can be referred in the work of Vu et al.3 The derived time-dependent contact coefficient reveals the evolution of heat transfer phenomenon that corresponds with the raise of the applied force during the glass pressing stage (Figure 8C). It rapidly increases when two bodies are brought into contact, followed by gradually approaching a constant value. The increase of applied force promotes a growth of actual contact area due to the glass flow into the microscale cavities of the mold surface. The consequence is the enhancement of heat flux across the interface, resulting in a sharp rise of the contact coefficient. In contrast, when the predefined force is attained, the contact heat transfer approaches its steady-state conditions, ie the contact coefficient remains almost unchanged. The contact heat transfer coefficients at this steady state, $h_{c,\text{steady}}$, are time-averaged and used for the following validation of numerical results.

Figure 9 summarizes the steady contact coefficients of all four different surface finishes. The plot illustrates the influence of surface roughness on the heat transfer across the contact interface in glass molding. Herein, the contact coefficient of the rough surface-mold 1-is much smaller than those of smooth surface-mold 4. This observation strengthens the importance of modeling the nature of microscale surfaces for the thermal contact conductance investigation. The smooth surface enhances the actual contact area growth and thus increases heat transfer across the interface.

Interestingly, the increasing slope of the oxidized surface-mold 3-is not comparable to other mold surfaces. It is observed that, though the average roughness of mold 3 ($R_a = 35$ nm) is much smaller than mold 2 ($R_a = 0.16$ µm), the contact coefficients of these surfaces are not much different at high contact pressures. These experiment results can probably be explained by the flattening effect and the alteration of thermomechanical properties due to the already performed loading cycles. When a surface is newly machined, its microasperities are elastically plastically deformed for the first time under the contact load and become flattened. As a result, the actual contact area increases sharply along with the applying load, which is the case for the fresh surfaces-mold 1, 2 and 4. In contrast, the surface of the mold 3 has been undergone several molding cycles before the experimental investigation. Under this circumstance, there exist thin oxide layers on its surface, which has been illustrated by the scanning electron microscope investigation.3 The oxide layers contain not only higher hardness but also lower thermal conductivity compared to the newly machined surface material, and subsequently cause an increase in heat resistance. In addition, the surface asperities, after sustaining the plastic deformation during earlier molding cycles, prompt an increase in hardness and become less flattened, accordingly. Such mechanical alteration of the surface morphology results in a less significant change in the surface topographies compared to newly machined surfaces. This experimental observation exemplifies the necessity of modeling the deformation behavior of the contact pair in the simulation of contact heat transfer.

4.2 | Simulation results and validation

For visualization purposes, numerical and experiment results are presented in individual plots for each mold surface finish (Figure 10A-D). Within the medium applying loads ($P = 8-16$ MPa), very good agreement between the simulation and experiment results is found, except mold 3 of which the simulated contact coefficients are slightly higher than
experiment ones. The discrepancy can possibly be explained by the alteration of mold’s surface morphology as discussed earlier in Section 4.1. Due to the existence of oxide layers on the surface and the hardened asperities, the actual surface of mold 3 possesses a higher thermal resistance, leading to lower contact coefficients obtained from the experiment. In the numerical simulation, however, the property of oxidized and hardened surfaces is not considered. This assumption turns out less heat resistance at the interface, a larger contact area, and consequently higher computed contact coefficients. This finding reveals a necessity to consider the changes of surface characteristics in future work.

In addition, a slight oscillation of the simulated contact coefficients in the range of the medium applying loads are observed. This behavior can be explained with the elastic deformation of barely contacting asperities that loses contact when the pressure at adjacent asperities increases. A schematic demonstration explaining the loss of contact spots due to elastic deformation is qualitatively shown in Figure 11. As the pressure increases at the left contact spot, this causes a regression in contact spots next to it, which leads, to a decrease in thermal contact conductance. However, by further increasing the contact pressure, these spots get into contact again resulting in an increase of thermal conductance.

Figure 10 shows that the computed contact coefficients underestimate the experiment results at low contact pressures and overestimate those at high loads. Such discrepancy observed at the extreme pressing loads can probably be explained by the current mechanical modeling considered by this study. The nature of glass behavior at forming temperature above $T_g$ exhibits a viscoelastic deformation to some extent. It means that the amount of glass deformation is strongly governed by its viscosity. Decrease in temperature causes a significant increase of viscosity and the deformation resistance. For this reason, when the glass

**FIGURE 8** Derivation of pressure-dependent contact heat transfer coefficient of the mold 3-glass pair. (A) Temperature profiles of the contact pair measured at 250 µm from the contact line; (B) Transient contact heat transfer coefficient; and (C) Recorded applying load

**FIGURE 9** Experimental measurement of steady contact heat transfer coefficients
is brought into contact with the colder mold, the temperature of contacting asperities abruptly drops to $T_g$ by means of heat transfer across the interface. The consequence is the significant increase of the deformation resistance occurring at these local contact spots; meanwhile, temperatures of neighboring asperities are still sufficiently high, as exemplarily illustrated in Figure 7, maintaining the viscoelastic behavior. As long as the applied force is remained, the neighboring spots continue filling into the microscale asperities of the mold surface. Accordingly, further growth of the contact area is occurred, until these surrounding spots contact with the mold surface become “frozen”. Similar study on the simulation of nonisothermal hot glass forming of microstructure optics demonstrates this observation. Such glass filling behavior into the microscale cavities of the mold surface explains the slight increase of the contact coefficient during the period of holding force. However, only a steady state, rather than a time-dependent deformation model is used. Hence, at low contact pressures, the flattening of the surface asperities and the consequent contact area computed by the simulation model is less compared to the actual contact area attained in the real molding experiments. Hence, the computed contact coefficients are underestimated.
At high loads, however, plastic deformation becomes the prevailing process in the mechanical modeling by the simulation. This prevailing deformation, in fact, neglects hardening effects that arise for example due to the surface deformation of the mold or increasing viscosity of the glass in corresponding experiments. Thus, the actual contact area is higher leading to the overestimation of the thermal contact conductance.

5 | CONCLUSIONS AND FUTURE RESEARCH

This paper presents numerical and experimental data quantifying the contact heat transfer across the interface in the glass molding process. The contact heat transfer coefficients are obtained from the modeling of individual 3D topographical geometries of the contact surfaces. Rather than using generated surfaces represented by generic characteristics, the actual surface geometries directly implemented from topographical measurements enables the avoidance of miscalculations, normally overestimated contact coefficients as pointed out in previous studies.16 In addition, the numerical approach successfully incorporates the mechanical model representing the deformation behavior of the glass and mold surfaces to the thermal modelling. Accordingly, the simulation tool permits numerical predictions of contact heat transfer coefficients in dependence on the pressure and surface roughness of the contact pair.

Four different mold surface finishes are used for the validation with the contact pressure ranging from 2 to 20 MPa. The experiment contact coefficients are derived from the transient temperature measurements, recorded by an infrared thermal camera, and using inverse heat transfer method. The simulation results are promising within the medium applying loads, while there exists a discrepancy at extreme load cases. The discrepancy is attributed to the mechanical model used for studying the deformation behavior of the glass surface. Future work, therefore, focuses on the implementation of the viscoelastic behavior into the simulation tool.

ACKNOWLEDGEMENT

The author Anh Tuan Vu gratefully acknowledges the sponsor of the German Academic Exchange Service (DAAD) for this research. Thorsten Helmig gratefully acknowledges the foundation by Deutsche Forschungsgemeinschaft (DFG, German Research Foundation)-Project-ID 174223256-TRR 96.

REFERENCES

1. Vu A-T, Kreilkamp H, Dambon O, Klocke F. Nonisothermal glass molding for the cost-efficient production of precision freeform optics. Opt Eng. 2016;55(7):071207.
2. Viskanta R, Lim JM. Theoretical investigation of heat transfer in glass forming. J Am Ceram Soc. 2001;84:2296–302.
3. Vu AT, Vu AN, Liu G, et al. Experimental investigation of contact heat transfer coefficients in nonisothermal glass molding by infrared thermography. J Am Ceram Soc. 2018;101:1–19.
4. Ananthasayanam B, Joseph PF, Joshi D, Gaylord S, Petit L, Blouin VY, et al. Final shape of precision molded optics: Part-II validation and sensitivity to material properties and process parameters. J Therm Stress. 2012;35(7):614–36.
5. Joshi D. Thermo-mechanical characterization of glass and its effect on predictions of stress state, birefringence and fracture in precision glass molded lenses. PhD Thesis. Clemson, SC: Graduate School of Clemson University; 2014. cited 2019 Aug 26. Available from: https://tigerprints.clemson.edu/all_dissertations/1463/
6. Pallicity TD, Vu AT, Ramesh K, Mahajan P, Liu G, Dambon O. Birefringence measurement for validation of simulation of precision glass molding process. J Am Ceram Soc. 2017;100:4680–98.
7. Kreilkamp H, Vu AT, Dambon O, et al. Nonisothermal glass molding of complex LED optics. In: 77th conference on glass problems: ceramic engineering and science. Sundaram SK (ed). New York, NY: Wiley; 2017: pp. 141–9. cited 2019 Aug 26. Available from: https://onlinelibrary.wiley.com/doi/pdf/10.1002/9781119417507.ch13
8. Kreilkamp H, Vu AT, Dambon O, et al. Replicative manufacturing of complex lighting optics by non-isothermal glass molding. Proc. SPIE 9949, Polymer Optics and Molded Glass. Optics: Design, Fabrication, and Materials 2016:99490B; 2016.
9. Yi AY, Jain A. Compression molding of aspherical glass lenses-a combined experimental and numerical analysis. J Am Ceram Soc. 2005;88:579–86.
10. Jain A, Yi AY. Numerical modeling of viscoelastic stress relaxation during glass lens forming process. J Am Ceram Soc. 2005;88(3):530–5.
11. Dora Pallicity T, Ramesh K, Mahajan P, Vengadesan S. Numerical modeling of cooling stage of glass molding process assisted by CFD and measurement of residual birefringence. J Am Ceram Soc. 2016;99:470–83.
12. Yan J, Zhou T, Masuda J, Kuriyagawa T. Modeling high-temperature glass molding process by coupling heat transfer and viscous deformation analysis. Precis Eng. 2009;33(2):150–9.
13. Dambon O. Development of a synergistic computational tool for material modeling. Grant Agreement number: N°NMP3-SL-2009-233524. SIMUGLASS Final report. 2013; cited 2019 Aug 26. Available from: http://cordis.europa.eu/project/rcn/93011_en.html
14. Sarhadi A, Hattel JH, Hansen HN. Three-dimensional modeling of glass lens molding. Int J Appl Glass Sci. 2015;6(2):182–95.
15. Dambon O. Development of a synergistic computational tool for material modeling. Grant Agreement number: N°NMP3-SL-2009-233524. SIMUGLASS Final report. 2013; cited 2019 Aug 26. Available from: http://cordis.europa.eu/project/rcn/93011_en.html
16. Sarhadi A, Hattel JH, Hansen HN. Three-dimensional modeling of glass lens molding. Int J Appl Glass Sci. 2015;6(2):182–95.
17. Xie J, Zhou T, Ruan B, Du Y, Wang X. Effects of interface thermal resistance on surface morphology evolution in precision glass molding for microlens array. Appl Opt. 2017;56:6622–30.
18. Thompson MK. A multi-scale iterative approach for FE modeling of thermal contact resistance. PhD Thesis. Cambridge, MA: Massachusetts Institute of Technology; 2007. cited 2019 Aug 26. Available from: https://dspace.mit.edu/handle/1721.1/42069
19. Frekers Y, Helmig T, Burghold EM, Kneer R. A numerical approach for investigating thermal contact conductance. Int J Therm Sci. 2017;121:45–54.
18. Vu AT, Kreilkamp H, Krishnamoorthi B-J, Dambon O, Klocke F. A hybrid optimization approach in non-isothermal glass molding. Proceedings of the 19th International ESAFORM Conference on Material Forming: Nantes, France. AIP Conference Proceedings. 1769:040006; 2016.

19. Tian X, Bhushan B. A numerical three-dimensional model for the contact of rough surfaces by variational principle. J Tribol. 1996;118(1):33–42.

20. Goerke D, Willner K. Normal contact of fractal surfaces-experimental and numerical investigations. Wear. 2008;264:589–98.

21. Liu G, Vu AT, Dambon O, Klocke F. Glass material modeling and its molding behavior. MRS Adv. 2017;2(16):875–85.

22. Siedow N, Grosan T, Lochegnies D, Romero E. Application of a new method for radiative heat transfer to flat glass tempering. J Am Ceram Soc. 2005;88:2181–7.

23. Vu AT, Vogel PA, Dambon O, et al. Vacuum-assisted precision molding of 3D thin microstructure glass optics. Proc. SPIE 10683, Fiber Lasers and Glass Photonics: Materials through Applications, 106830C; 2018.

**How to cite this article:** Vu AT, Helmig T, Vu AN, et al. Numerical and experimental determinations of contact heat transfer coefficients in nonisothermal glass molding. *J Am Ceram Soc*. 2019;00:1–12. [https://doi.org/10.1111/jace.16756](https://doi.org/10.1111/jace.16756)