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To cite this article: B. S. Yilbas, S. S. Akhtar & C. Karatas (2014) Laser Cutting of Triangular Geometry into Alumina Tiles: Morphological Changes and Thermal Stress Analysis, Machining Science and Technology, 18:3, 424-447, DOI: 10.1080/10910344.2014.925376

To link to this article: https://doi.org/10.1080/10910344.2014.925376

Published online: 01 Aug 2014.

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LASER CUTTING OF TRIANGULAR GEOMETRY INTO ALUMINA TILES: MORPHOLOGICAL CHANGES AND THERMAL STRESS ANALYSIS

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Laser cutting of triangular geometry into 5-mm-thick alumina tile is carried out. Temperature and stress fields are predicted by using ABAQUS finite element code in line with the experimental conditions. Experiments are carried out to validate the predictions of temperature and the residual stress in the cutting section. Thermocouples are incorporated in temperature measurements while X-ray diffraction technique is accommodated to obtain the residual stress at the kerf surface. The morphological changes in the cutting section are examined by using optical and scanning electron microscopy, energy dispersive spectroscopy, and X-ray diffraction. It is found that temperature and residual stress predictions agree well with their counterparts obtained from the experiments. The optical and SEM micrographs reveal that the cut sections are free from large size defects such as large-scale cracks and sideways burnings. The maximum value of von Mises stress occurs at the mid-thickness of the workpiece due to the formation of high magnitude of thermally induced strain in this region.

Keywords alumina tiles, laser cutting, thermal stress, triangular geometry

INTRODUCTION

Laser machining of ceramic tiles offers considerable advantages over the conventional machining methods. Some of these advantages include non-mechanical contact between the workpiece and the cutting tool, precision of operation, and high speed processing. Since the process involves with non-mechanical contact, the mechanical properties of the ceramic, such as hardness and fracture toughness, do not influence the end product quality. Although laser machining of ceramic tiles has many advantages, high...
stress fields are developed during the process due to the high temperature gradients, which cause thermally induced cracks at the machined sections. Despite the fact that the laser control machining can reduce the defect sites (Yilbas et al. 2011, 2012, 2013), further investigation into laser cutting of ceramic is essential, particularly for the geometries having the sharp edges or corners.

Considerable research studies were carried out to examine laser cutting of ceramic tiles. The active stressing technique for delaying fracture during laser cutting of alumina was investigated by Akarapu and Segall (2006). They introduced the numerical technique, incorporating the probabilistic fracture mechanics, to examine the actively induce compressive thermal-stresses for control fracture during laser cutting of cantilevered alumina. The effect of a re-cast laser on the laser cutting of alumina ceramics plates was investigated by Fu et al. (2004). They showed that the fast cooling of the liquid phase caused formation of extensive network of cracks on the kerf surface, which degraded mechanical strength and thermal shock resistance of the alumina plate.

Laser cutting of alumina plates incorporating the controlled fracture technique was studied by Tsai and Chen (2003). They indicated that the fracture surface mainly composed of the columnar grain region produced by resolidification of the melted material and the intergranular fracture region produced by anisotropic thermal expansion. The statistical analysis for striation formation due to laser cutting of ceramics was introduced by Wee et al. (2008). They demonstrated that the inclination of the striation was most affected by the interaction time and the long interaction time and the irradiance caused long striation wavelengths. The influence of machining parameters on the surface roughness of laser micro-machined alumina-aluminum ceramics were examined by Biswas et al. (2008).

They demonstrated that for certain range of laser parameters, the surface roughness of the machined parts reduced considerably. The probabilistic model for crack formation in laser cutting of ceramics was studied by Lee and Ahn (2003). They introduced the Bayesian probabilistic model for the crack formation over thin alumina plates during laser cutting, and the model proposed could predict the critical cutting front angle, which was directly related to the initiation of crack formation. Laser cutting of aluminum nitride and stress formation were examined by Molian et al. (2008). They demonstrated that the thermal stress method used for laser cutting of ceramics offered significant benefits such as improved precision, better cut quality, higher cutting speed, and lower energy losses. Laser cutting of aluminum nitride plates was studied by Migliore and Ozkan (2003).

They developed a new processing regime for aluminum nitride cutting, in which the effective processing could be accomplished. Laser water
jet-assisted cutting of alumina via evaporative and thermal stress fracture modes was investigated by Shehata et al. (2007). They demonstrated that 60% increase in flexural strength was obtained for laser water jet-assisted cut plates over laser cut plates, and the thermal damage as well as loss in the surface integrity were considerably diminished for laser water jet-assisted cutting process. Laser cutting of alumina substrates using a femtosecond laser source was examined by Wang et al. (2010).

They showed that the proper selection of laser cutting parameters, it was possible to obtain defect free cuts such as small tapered, crack and delamination free cut sections. Laser cutting of mullite-alumina was examined by Pou et al. (2000). They indicated that the pulsed Nd:YAG laser connected to an optical fiber was a feasible and flexible tool for a successful cutting of elements made of mullite-alumina. The fiber laser cutting of electronic alumina ceramics was carried out by Chen et al. (2011). They showed that the striation-free cutting could be achieved at high laser power settings and the coloration at the kerf surface was associated with the tetragonal alumina induced by nitrogen assisting gas. Nd:YAG laser cutting of alumina sheets was investigated by Yan et al. (2012). They proposed a model for pulsed laser striation-free cutting of alumina sheets. The multibeam fiber laser cutting was studied by Ove et al. (2009). They applied the multibeam patterns to control the melt flow out of the cut kerf resulting in improved cut quality.

Although laser cutting of ceramic tiles were investigate previously (Yilbas et al., 2011, 2012, 2013), the effect of sharp edges on the temperature and stress fields in the cutting section was left obscure. Therefore, in the present study, laser cutting of rectangular geometry into the alumina tile is carried out. Temperature and stress fields in the cutting section are predicted by using the ABAQUS finite element code in line with the experimental conditions. The morphological and micro-structural changes in the cut sections were examined incorporating the optical and scanning electron microscopy, energy dispersive spectroscopy, and X-ray diffraction. Temperature and residual stress formed in the cut section were measured by using the thermocouples and X-ray diffraction technique, and the findings are compared with their counterparts obtained from the predictions of the simulations for validation purposes.

MATHEMATICAL ANALYSIS

In the model study, temperature-dependent properties, latent heat effects due to melting, and elasto-plastic analysis are incorporated. Although the thermal stress analyses are given in the early studies (Yilbas et al., 2011, 2012, 2013), they are provided here due to the completeness of the arguments. The schematic view of the laser cutting of rectangular geometry and
the coordinate system is shown in Figure 1. The heat equation pertinent to the laser heating process can be written as:

$$\rho \frac{\partial (H(T))}{\partial t} = (\nabla (k(T) \nabla T)) + \rho U \frac{\partial (C_p(T) T)}{\partial x} + S_o \quad (1)$$

where $H$ is the temperature-dependent enthalpy including the latent heat of solidification, $k$ is the temperature-dependent thermal conductivity, $\rho$ is the density, $U$ is the moving heat source velocity resembling the laser cutting velocity, and $S_o$ is the heat source term resembling the laser beam, which is:

$$S_o = I_o \delta e^{-\delta z} (1 - r_f) e^\left(-\frac{x^2 + y^2}{a^2}\right) \quad (2)$$

$I_o$ is laser power peak density, $\delta$ is the absorption coefficient, $a$ is the Gaussian parameter, $r_f$ is the surface reflectivity, and $x$ and $y$ are the axes. The absorption coefficient of the laser beam ($\delta$) after the key-hole formation is
considered, i.e.,
\[
\delta = \frac{1}{t_h} \ln \left( \frac{I_o}{I_L} \right)
\]
(3)

where \( t_h \) is the thickness of the workpiece, \( I_o \) is the peak power intensity at the workpiece surface, \( I_L \) is the laser power intensity at the workpiece thickness. The laser beam axis is parallel to the z-axis (Figure 1). It should be noted that the laser beam intensity distribution is assumed to be Gaussian at the irradiated surface.

The convection and radiation boundary conditions are considered at the free surface of the workpiece. Therefore, the corresponding boundary condition is:

At the irradiated surface (top surface),
\[
\frac{\partial T}{\partial z} = \frac{h_f}{k} (T_s - T_{amb}) + \frac{\varepsilon \sigma}{k} (T_s^4 - T_{amb}^4)
\]
where \( h_f (= 3000 \text{ W/m}^2\text{K}) \) (Shuja and Yilbas, 2000) is the forced convection heat transfer coefficient due to the assisting gas.

At the bottom surface:
\[
\begin{align*}
\frac{\partial T}{\partial y} &= \frac{h}{k} (T_s - T_{amb}) + \frac{\varepsilon \sigma}{k} (T_s^4 - T_{amb}^4) \\
\frac{\partial T}{\partial z} &= \frac{h}{k} (T_s - T_{amb}) + \frac{\varepsilon \sigma}{k} (T_s^4 - T_{amb}^4)
\end{align*}
\]
where \( h (= 20 \text{ W/m}^2\text{K}) \) is the heat transfer coefficient due to natural convection, and \( T_s \) and \( T_{amb} \) are the surface and ambient temperatures, respectively, \( \varepsilon \) is the emissivity (\( \varepsilon = 0.9 \) is considered), \( \sigma \) is the Stefan–Boltzmann constant \( (\sigma = 5.67 \times 10^{-8} \text{ W/m}^2\text{K}^4) \). At far away boundary (at edges of the solution domain) constant temperature boundary is assumed \( (T = 293 \text{ K}) \), i.e.,

\[
x = \infty \ ; \ y = \infty \ ; \ z = t_h \rightarrow T = 293 \text{ K}
\]
where \( t_h \) is the thickness of the workpiece \( (t_h = 0.005 \text{ m}) \). Initially (prior to laser cutting), the substrate material is assumed to be at constant ambient temperature, i.e., \( T = T_{amb} \), which is considered as constant \( (T_{amb} = 293 \text{ K}) \).

Equation 1 is solved numerically with the appropriate boundary conditions to predict the temperature field in the substrate material. Table 1 gives the data used in the simulations in line with the experimental conditions. In order to incorporate the melting during the heating, the enthalpy method is used (ABAQUS, 2013). In this case, the specific heat capacity is associated with the internal energy gain of the substrate material, i.e., \( C_p (T) = \frac{\partial U}{\partial T} \).
TABLE 1a Simulation Conditions

<table>
<thead>
<tr>
<th>Length (m)</th>
<th>Thickness (m)</th>
<th>$I_o$ (W/m²)</th>
<th>$I_l$ (W/m²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.01</td>
<td>0.005</td>
<td>$3.1 \times 10^{12}$</td>
<td>$2.13 \times 10^{12}$</td>
</tr>
</tbody>
</table>

However, the internal energy gain during the phase change is associated with the latent heat of fusion, which is given separately in terms of solidus and liquidus temperatures (the lower and upper temperature bounds of the phase change range) and the total internal energy associated with the phase change (ABAQUS, 2013). Since the primary interest is the stress field developed in the laser irradiated section, the flow field generated in the liquid phase in the surface vicinity during the laser melting process is omitted. This is due to that the thermal stress field is considered to be almost zero in the melt pool, since only negligibly small hydrodynamic pressures are present in the melt pool.

However, molten flow from the kerf surface modifies the heat transfer characteristics such as cooling rates at the surface. This, in turn, alters the temperature gradients and thermal stress field in the vicinity of the kerf surface. Due to the complex nature of the molten flow and modelling difficulties incorporating its influence on the heat transfer characteristics, this effect is simplified in terms of the convection heat transfer from the irradiated surface. Therefore, heat transfer coefficient, resembling the simplified form of the effect of the molten flow on the heat transfer rates, is incorporated in the analysis.

In the thermal stress analysis, the solidification involves a small strain; therefore, the assumption of small strain is adopted in this work. The thermal strains that dominate thermo-mechanical behavior during solidification are on the order of only a few percent (Risso et al., 2006). Several previous solidification models (Risso et al., 2006) confirm that the solidified metal part undergoes only small deformation during solidification. The displacement spatial gradient is small $\nabla u = \partial u/\partial x$ so $\nabla u \cdot \nabla u \approx 1$ and the linearized strain tensor becomes (Mase and Mase, 1999):

$$\varepsilon = \frac{1}{2} [\nabla u + (\nabla u)^T]$$

The small strain formulation can be used, where the Cauchy stress tensor is identified with the nominal stress tensor $\sigma$, and $b$ is the body force density with respect to initial configuration.

$$\nabla . \sigma(x) + b = 0$$

(5)
The rate representation of total strain in this elastic-viscoplastic model is given by (ABAQUS, 2013)

\[ \dot{\varepsilon} = \dot{\varepsilon}_{el} + \dot{\varepsilon}_{ie} + \dot{\varepsilon}_{th} \]  

(6)

where \( \dot{\varepsilon}_{el} \), \( \dot{\varepsilon}_{ie} \), \( \dot{\varepsilon}_{th} \) are the elastic, inelastic (plastic + creep), and thermal strain rate tensors respectively. Stress rate \( \dot{\sigma} \) depends on elastic strain rate, and in this case of linear isotropic material and negligible large rotations, it is given by Equation (7) in which “:” represents inner tensor product.

\[ \dot{\sigma} = \mathbf{D} : (\varepsilon - \varepsilon_{ie} - \varepsilon_{th}) \]  

(7)

\( \mathbf{D} \) is the fourth-order isotropic elasticity tensor given by Equation (8):

\[ \mathbf{D} = 2\mu \mathbf{I} + \left( K_B - \frac{2}{3}\mu \right) \mathbf{I} \otimes \mathbf{I} \]  

(8)

Here \( \mu \), \( K_B \) are the shear modulus and bulk modulus respectively and are in general functions of temperature, while \( \mathbf{I} \) are fourth- and second-order identity tensors and “\( \otimes \)” is the notation for outer tensor product.

Inelastic strain includes both strain-rate independent plasticity and time dependant creep. Creep is significant at the high temperatures of the solidification processes and is indistinguishable from plastic strain (Li and Thomas, 2004). The inelastic strain-rate is defined here with a unified formulation using a single internal variable (Anand, 1982; Lush et al., 1989), equivalent inelastic strain \( \varepsilon_{el} \) characterizes the micro-structure.

The equivalent inelastic strain-rate \( \varepsilon_{el} \) is a function of equivalent stress \( \bar{\sigma} \), temperature \( T \), equivalent inelastic strain \( \varepsilon_{el} \).

\[ \varepsilon_{el} = f(\bar{\sigma}, T, \varepsilon_{el}) \]  

(9)

\[ \bar{\sigma} = \sqrt{\frac{2}{3} \sigma'_{ij} \sigma'_{ij}} \]  

(10)

\( \sigma' \) is a deviatoric stress tensor defined in Equation (9).

\[ \sigma'_{ij} = \sigma_{ij} - \frac{1}{3} \sigma_{kk} \delta_{ij} \]  

(11)

The workpiece is assumed to harden isotropically, so the von Mises loading surface, associated plasticity and normality hypothesis in the
Prandtl–Reuss flow law is applied (Mendelson, 1983):

\[
(\dot{\varepsilon}_{ie})_{ij} = \frac{2}{3} \varepsilon_{el} \frac{\sigma'_ij}{\sigma}
\]  

(12)

Thermal strains arise due to volume changes caused by both temperature differences and phase transformations, including solidification and solid-state phase changes, i.e.,

\[
(\varepsilon_{th})_{ij} = \int_{T_o}^{T} \delta_{ij}\alpha_TdT
\]

(13)

where \(\alpha_T\) is coefficient of thermal expansion, and \(T_o\) is the reference temperature and \(\delta_{ij}\) is Kronecker’s delta.

Finite element discretization is carried out using the ABAQUS software (2013) and the simulation is performed using the sequential thermal-stress analysis. In the analysis, 132140 hexahedral elements are used for the thermal-stress discretization. In addition, for the heat transfer and stress analysis, DC3D8 (8-node linear heat transfer and stress brick) type of elements are used. Since the melting is modeled through a temperature dependent specific heat capacity allowing the latent heat of melting and solidus and liquidus temperatures of the substrate material, the solid-liquid interface can be determined from the enthalpy balance (ABAQUS, 2013). However, non-presence of the external mechanical forces during the laser processing, the contact elements satisfying mechanical and thermal equilibrium across the phase change are not incorporated in the simulations (Ming and Hua, 2010). Temperature data are transferred to the elements used for the stress analysis through the connectivity matrix. This provided less computational time for the converged results.

The fixed boundary conditions are applied on the both ends of the workpiece resembling the experimental laser heating situation. Laser intensity with the Gauss distribution and prescribed velocity of 10 cm/s along the x-axis through user subroutine DFLUX is applied to the thermal model. The Gauss parameter “a” is \(a = 0.000333\) m, in accordance with the experimental power intensity distribution. Cooling was allowed to continue until all of the elements reach initial temperature (293 K). The temperature-time history resulted from the thermal analysis is used as input to the thermal stress analysis. The workpiece is modeled as von Mises elastic-plastic material with isotropic hardening and with a yield stress that changes with temperature. In order to assess the effect of laser cutting parameters on the numerical simulations, laser cutting speed is changed to increase and reduce by 20% (12 cm/s and 8 cm/s).

The findings of numerical predictions reveal that the maximum stress increases over 30% (∼ 4 GPa) when the cutting speed reduces to 8 cm/s.
while the maximum stress reduces by 8% (~ 2.8 GPa) when cutting speed increases to 12 cm/s. Consequently, reducing cutting speed has larger impact on the maximum value of von Mises stress than that corresponding to high cutting speed (12 cm/s). Table 1 gives the properties of alumina used in the simulations.

**EXPERIMENTAL**

A CO₂ laser (LC-ALPHAIII) delivering nominal output power of 1800 kW was used to irradiate the workpiece surface. The laser output consists of high-frequency repetitive pulses with the frequency of 1500 pulses per second. The wavelength of the laser is 10.6 μm and laser output intensity is Gaussian with TEM₀₀ mode. The nominal focal length of the focusing lens was 127 mm and the laser beam diameter focused at the workpiece surface was 0.3 mm. Nitrogen assisting gas emerging from the conical nozzle and co-axially with the laser beam was used. The cut was realized with a single pass. It should be noted that surface reflectivity reduces the absorption of the incident beam in the irradiated region.

However, as the melting temperature is reached, the reflectivity reduces (Čtibor et al., 2007) and incident power becomes sufficient to cut the alumina tile in a single pass. The cutting experiments were repeated after incorporating various cutting conditions and parameters. The parameters

<table>
<thead>
<tr>
<th>α (1/T)</th>
<th>T_solidus (K)</th>
<th>T_liquidus (K)</th>
<th>L_m (kJ/kg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>7.5 × 10⁻⁶</td>
<td>2260</td>
<td>2323</td>
<td>900</td>
</tr>
</tbody>
</table>
resulting in the optimum defect free cutting are considered for the laser cutting simulations. The laser cutting parameters are given in Table 2.

In the cutting experiments, alumina ($\text{Al}_2\text{O}_3$) tiles with 5 mm thickness were used as workpieces. JEOL JDX-3530 scanning electron microscope (SEM) was used to obtain photomicrographs of the cross-section of the workpieces after the tests. EDS analysis was carried out at the kerf surface, and the data obtained was given in Table 3. A Bruker D8 Advance CuKα radiation machine was used for XRD analysis. A typical setting of XRD was 40 kV and 30 mA. It should be noted that the residual stress measured using the XRD technique provided the data in the surface region of the cut section. This is because of the penetration depth of CuKα radiation into the surface, which is on the order of few micrometers.

In XRD measurements, the position of the diffraction peak exhibits a shift as the specimen is rotated by an angle $\psi$ and the magnitude of the shift is related to the magnitude of the residual stress. The relationship between the peak shift and the residual stress ($\sigma$) is given (Khana et al., 2005):

$$\sigma = \frac{E}{(1 + \nu) \sin^2 \psi} \frac{(d_n - d_o)}{d_o}$$  \hspace{1cm} (14)

where $E$ is Young’s modulus, $\nu$ is Poisson’s ratio, $\psi$ is the tilt angle, $d_o$ is the stress free spacing, and $d_n$ are the $d$ spacing measured at each tilt angle. If there are no shear strains present in the specimen, the $d$ spacing changes linearly with $\sin^2 \psi$. Figure 2 shows the linear dependence of $d(311)$ on $\sin^2 \psi$ in the region of laser cut surface. The $\gamma$-$\text{Al}_2\text{O}_3$ peak takes place at 37.5°, which corresponds to the (311) plane with the interplaner spacing of 1.23 Å. The slope of the linear variation as shown in Figure 2 is $-1.192 \times 10^{-12}$ m/degrees. The elastic modules and the Poisson’s ratio of alumina are 380 GPa and 0.27, respectively; therefore, the

The error related to the EDS analysis is on the order of 3%.
residual stress determined from the XRD technique at the surface vicinity is on the order of $-2.8 \pm 0.09$ GPa, which is compressive. XRD measurements are repeated three times and the error related to the measurements is on the order of 3%.

To validate temperature predictions, three thermocouples was used to monitor the temporal variation of surface temperature at the location 0.45 mm away from the laser irradiated spot center along the y-axis to avoid the melting of the surface of the thermocouple during the laser cutting process, as shown in Figure 1. The thermocouples output were calibrated according to the previous study (Yilbas et al., 1990). The temperature measurement experiments were repeated five times and the error was estimated in the order of 5%.

**RESULTS AND DISCUSSION**

Laser cutting of a triangular geometry into a 5-mm-thick alumina tile is considered and thermal stress field developed in the cutting section is simulated by using ABAQUS finite element code. The morphological changes in the cutting section are examined through incorporating the analytical tools. The predictions of surface temperature and residual stress are validated by using the experimental data.

Figure 3 shows temporal variation of surface temperature predicted from the simulations and obtained from the thermocouple data. It is found that both results are in good agreement. The discrepancies between both results are within the limit of the experimental error. In addition, the residual stress predicted from the simulation is 3.1 GPa and its counterpart obtained from the XRD technique is $2.8 \pm 0.09$ GPa. It is evident that both findings are
in good agreement and the differences between both results are negligibly small.

Figure 4 shows temperature distribution along the top and the bottom circumferences of the triangulate geometry for different cooling periods while Figure 5 shows temperature contours in the cutting section at the onset of the cooling period. It should be noted that the cooling period initiates at $t = 0.33$ s. Temperature attains low values in the region close to the corners of the triangular shape. This is attributed to the conduction heat transfer from the corner region to its surroundings, since the surface area in the corner region is larger than the surface area at the side edges of the triangular geometry. Temperature exceeds the liquidus temperature of the substrate material at the irradiated spot center at the onset of cooling period. This indicates the superheating of liquid phase, which takes place in the central region of the irradiated spot.

Moreover, the mush zone is formed in the region next to the liquid region, since temperature remains between the solidus and liquidus temperatures of the substrate material in this region. The decay of temperature is gradual along the initially cut edge of the triangle because of the convection, conduction, and radiation heat losses from the edge of the triangle. However, temperature decay is sharp in the region next to the mushy zone, which corresponds to the region lately cut by a laser beam. Temporal decay of temperature is high as the cooling period progresses, which is more
FIGURE 4  Temperature distribution along the top and the bottom circumference of triangle geometry for different cooling periods.
pronounced along the lately cut edge due to the attainment of high temperature in this region. As the cooling period progresses further, temperature reduces to initial temperature (298 K) along all the edges of the triangle; in which case, the cooling period ends ($t = 500\ s$). In the case of the bottom circumference of the triangle geometry, temperature behavior is similar to that corresponding to the top circumference. However, temperature values are slightly less than those of the top circumference. This is attributed to the
absorption of the laser beam intensity prior to reaching the bottom surface of the workpiece during the cutting process.

Figure 6 shows von Mises stress distribution along the top and bottom circumferences of the triangular geometry for different cooling periods while Figure 7 shows von Mises stress contours at the onset of cooling period. von Mises stress becomes low along the edges where temperature is high. This behavior is attributed to temperature dependent elastic modulus, which reduces with increasing temperature (Table 1). As the cooling period increases, von Mises stress increases considerably, particularly in the corner regions of the triangle geometry. This is associated with one of all of the followings: temperature in the corner region is low due to high rate of conduction in this region; which, in turn, results in high value of the elastic modulus as compared to side edges of the triangle geometry, and ii) high cooling rates causes rapid changes in the temperature gradients in this region while giving rise to the development of the high thermal strain.

In addition, von Mises stress becomes the minimum in the neighbour hood of the corners of the triangle geometry. This can be explained in term of the self-annealing effect of the heated region in the corners. The maximum value of the von Mises stress in the corner is on the order of 3.1 GPa, which is due to local increase, i.e., it appears as a spike in Figure 6. In laser straight or circular cutting of alumina tiles, the maximum value of the von Mises stress is in the order of 2.3 GPa (Yilbas et al., 2011, 2012, 2013), which is much less than that corresponding to the corner of the cut section. When comparing von Mises distribution along the top and bottom circumferences of the triangle geometry, the stress behavior is almost similar, except the stress values are slightly lower at the bottom circumference than those corresponding to the top circumference. This is associated with temperature behavior along the circumference of the triangle geometry, which is lower at the bottom circumference than that of the top circumference.

Figure 8 shows von Mises stress distribution along the z-axis (thickness of the workpiece) at the end of the cooling period (t = 500 s) at different locations along the cut edges of the triangular geometry (Figure 1). von Mises stress attains low values in the region close to the top and bottom surfaces of the workpiece. It should be noted that z = 0 corresponds to the top surface and z = 5 mm corresponds to the bottom surface of the workpiece. The attainment of low von Mises stress is attributed to the free expansion of the surface. In this case, the workpiece surface is free to expand during the heating cycle and free to contract during the cooling cycle; therefore, the thermal strain developed becomes low in this region. However, in the region of the mid-thickness, von Mises stress attains relatively higher values than that at the surface region. This is attributed to the compressive stresses developed during the thermal expansion of the workpiece; in which case, this region is not free to expand and it undergoes excessive compression.
FIGURE 6 von Mises stress distribution along the top and the bottom circumference of triangle geometry for different cooling periods.
FIGURE 7 von Mises stress contours in the cut section at the onset of cooling period.

during the cutting process. The maximum magnitude of von Mises stress is on the order of 3.1 GPa, which corresponds to location #3.

Figure 9 shows optical photographs of laser cut triangular geometry while Figure 10 shows the SEM micrograph of the kerf surface. The cut sections are free from large defects such as major cracks or large scale sideways burnings. The closed examination of the optical photograph shows that striation patterns are formed at the edges of the cut section and the depth of striation patterns increase towards the bottom edge of the kerf. The formation of the striation patterns is related to the melt instability and the
cooling effect of the assisting gas jet effect on the molten flow. The assisting gas purges the molten flow exiting the cut section through exerting a drag force on the molten flow surface. However, depending on the depth of the liquid layer at the kerf surface, the rate of fluid strain alters while modifying the frictional force in the molten flow. This alters the amount of material purged from the kerf exit. Since the melt instability modifies the thickness of the molten material at the kerf surface, temporal fluctuations of the liquid thickness cause the formation of elongated and shallow grooves while initiating the striation formation at the surface. The cooling effect of the assisting gas increases the viscosity of the molten flow at the kerf surface; consequently, high viscosity is resulted towards the kerf exit. This contributes to the formation of deep striations towards the kerf exit. In addition, the combination of the high frictional force, due to high viscosity, and the surface tension force results in the dross attachment at the kerf exit, which can clearly observed at the kerf exit. On the other hand, micro-cracks take place at the surface, which can be observed from an SEM micrograph (Figure 10).

This is attributed to formation of high stress levels around the edges of the cut sections as predicted from the simulations (Figure 6). In this case, high stresses are mainly confined in the close region of the cut edges, since it decays sharply as the distance normal to the cut edge increases, which can be observed from three-dimensional view of stress field (Figure 7).

The experimental findings are in line with these findings; in which case, these cracks are shallow in depth and do not conform to the base material. Therefore, the formation of large-scale cracks at the kerf is suppressed by the micro-crack network. The crack formation at the kerf surfaces (Figure 10)
lowers the cut section quality, provided that these cracks do not extend into the substrate material substantially to limit the practical application of the end product.

It should be noted that once the crack network is formed, the strain energy is released in the surface region while suppressing the large-scale crack formation in the cutting section. Figure 11 shows XRD diffractogram of laser cut section. The formation of metastable $\gamma$-$\text{Al}_2\text{O}_3$ phase into $\alpha$-$\text{Al}_2\text{O}_3$ takes place through the thermal relaxation of non-equilibrium $\gamma$-$\text{Al}_2\text{O}_3$ phase (Damani and Makroczy, 2000). The formation of nitride specie takes place at the kerf surface because of nitrogen at high pressure, which is used in the
cutting experiments. This situation can be observed from $AlON$ peak in the diffractogram. Therefore, $Al_2O_3$ experiences a reducing reaction forming $AlO$ ($Al_2O_3 \rightarrow 2AlO + 1/2 O_2$). The use of nitrogen at high pressure, as an assisting gas, triggers the formation of $AlON$ at the surface through the reaction $2AlO + N_2 \rightarrow 2AlON$. The presence of nitrogen in the cut section is evident from EDS data. It should be noted that the quantification of light elements involves with error in the energy dispersive spectroscopy data; however, its presence is evident in Table 3.

![SEM micrograph of laser cut surface.](image1)

**FIGURE 10** SEM micrograph of laser cut surface.

![X-ray diffractogram of cut section.](image2)

**FIGURE 11** X-ray diffractogram of cut section.
CONCLUSION

Laser cutting of triangular geometry into 5-mm-thick alumina tile is carried out and a thermal stress field developed during the cutting process is simulated using ABAQUS finite element code in line with the experimental conditions. The morphological changes in the cutting section are examined by incorporating optical and scanning electron microscopy, energy dispersive spectroscopy, and X-ray diffraction technique. Temperature and residual stress simulations are validated through the thermocouple data and the data obtained from the XRD technique, respectively. It is found that temperature and residual stress predictions agree well with their counterparts obtained from the experiments. von Mises stress attains high values in the corners of the triangular cut geometry. This is associated with high cooling rates, which take place in this region due to conduction heat transfer while generating high strains.

The maximum temperature well exceeds the liquid temperature of the substrate material while modifying the temperature gradients in the cutting section. von Mises stress attains high values at the mid-thickness of the workpiece at the end of the cooling period. This behavior is associated with the presence of the thermo-mechanical constraint in this region during the expansion and contraction of the workpiece. On the other hand, free expansion of the workpiece surfaces lowers von Mises stress in the surface region. Optical photographs reveal the formation of the striation patterns at the kerf surface, which are related to the melt instabilities occurring in molten flow. The depth of striation pattern increases towards the kerf exit and the combination of increased frictional force, due to increased viscosity and the surface tension force, causes the dross attachment at the kerf exit. The use of high-pressure nitrogen, as an assisting gas, results in the formation of AlON compound at the kerf surface, which contributes to hardness and the molten viscosity at the kerf surface.

FUNDING

The authors acknowledge the support of Dean of Scientific Research for funded Project (SB121012), King Fahd University of Petroleum and Minerals, Dhahran, Saudi Arabia and Karmetal AS in Turkey.

NOMENCLATURE

\(a\) \hspace{1cm} Gaussian parameter
\(b\) \hspace{1cm} body force density
\(C_p\) \hspace{1cm} specific heat
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$d_o$</td>
<td>stress free spacing</td>
</tr>
<tr>
<td>$d$</td>
<td>spacing measured at each tilt angle</td>
</tr>
<tr>
<td>$D$</td>
<td>fourth-order isotropic elasticity tensor</td>
</tr>
<tr>
<td>$E$</td>
<td>Young’s modulus</td>
</tr>
<tr>
<td>$H$</td>
<td>temperature dependent enthalpy including the latent heat of solidification</td>
</tr>
<tr>
<td>$h_f$</td>
<td>forced convection heat transfer coefficient due to the assisting gas</td>
</tr>
<tr>
<td>$h$</td>
<td>heat transfer coefficient due to natural convection</td>
</tr>
<tr>
<td>$I_L$</td>
<td>laser power intensity at the workpiece thickness</td>
</tr>
<tr>
<td>$I_o$</td>
<td>laser power peak density</td>
</tr>
<tr>
<td>$I$</td>
<td>fourth-order identity tensors</td>
</tr>
<tr>
<td>$\bar{I}$</td>
<td>second-order identity tensors</td>
</tr>
<tr>
<td>$k$</td>
<td>temperature dependent thermal conductivity</td>
</tr>
<tr>
<td>$K_B$</td>
<td>bulk modulus</td>
</tr>
<tr>
<td>$r_f$</td>
<td>surface reflectivity</td>
</tr>
<tr>
<td>$S_o$</td>
<td>heat source term resembling the laser beam</td>
</tr>
<tr>
<td>$t_h$</td>
<td>workpiece thickness</td>
</tr>
<tr>
<td>$T_s$</td>
<td>surface temperature</td>
</tr>
<tr>
<td>$T_{amb}$</td>
<td>ambient temperatures</td>
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<tr>
<td>$U$</td>
<td>internal energy</td>
</tr>
<tr>
<td>$u$</td>
<td>displacement</td>
</tr>
<tr>
<td>$x$</td>
<td>axis</td>
</tr>
<tr>
<td>$y$</td>
<td>axis</td>
</tr>
<tr>
<td>$z$</td>
<td>axis</td>
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**Greek Symbols**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>$\alpha_T$</td>
<td>coefficient of thermal expansion</td>
</tr>
<tr>
<td>$\delta$</td>
<td>absorption coefficient</td>
</tr>
<tr>
<td>$\delta_{ij}$</td>
<td>Kronecker’s delta</td>
</tr>
<tr>
<td>$\varepsilon$</td>
<td>emissivity ($\varepsilon = 0.9$ is considered)</td>
</tr>
<tr>
<td>$\varepsilon_{el}$</td>
<td>elastic strain rate tensor</td>
</tr>
<tr>
<td>$\varepsilon_{ie}$</td>
<td>inelastic (plastic + creep) strain rate tensor</td>
</tr>
<tr>
<td>$\varepsilon_{th}$</td>
<td>thermal strain rate tensor</td>
</tr>
<tr>
<td>$\varepsilon_{el}$</td>
<td>equivalent inelastic strain</td>
</tr>
<tr>
<td>$\varepsilon_{el}$</td>
<td>equivalent inelastic strain-rate</td>
</tr>
<tr>
<td>$\nu$</td>
<td>Poisson’s ratio</td>
</tr>
<tr>
<td>$\rho$</td>
<td>density</td>
</tr>
<tr>
<td>$\psi$</td>
<td>tilt angle</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>Stefan-Boltzmann constant</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>nominal stress tensor</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>stress rate</td>
</tr>
<tr>
<td>$\mu$</td>
<td>shear modulus</td>
</tr>
</tbody>
</table>
⊗ notation for outer tensor product
σ′ deviatoric stress tensor

REFERENCES


