Residual Stresses and Strength of Friction Welded Ceramic/Metal Joints

Joint geometry dominates residual stresses and strength of friction welded ceramic/metal joints

BY R. WEISS

ABSTRACT. Based on friction welding experiments of various ceramic materials to an aluminum alloy, the influence of residual stresses on the strength of ceramic/metal joints was examined numerically. Heat conduction calculations to estimate the temperature distribution have been conducted by the Finite Element Method (FEM), using experimental data for input. Afterward, residual stresses introduced through cooling and stresses introduced through a tensile test were determined by FEM. By applying multiaxial Weibull statistics to the ceramic specimen and examining tensile strength for different geometries of the joint, different process parameters and different material combinations were estimated and compared.

Introduction

In most engineering fields, the application of ceramics is often restricted by the availability of an adequate joining technique. For the joining of ceramics to metals, brazing, active brazing and diffusion welding have been established. As earlier work indicates (Refs. 1-4), friction welding may be an interesting and cost effective alternative in many applications, provided that the strength of friction welded joints reached or exceeded the strength of those joints produced by other techniques. The strength of a ceramic/metal joint is not only determined by the strength of the interface, but also by the residual stresses introduced through the different thermal expansion in most material combinations. The influence of these residual stresses on the joint strength is the focus of this examination.

Experimental Procedure and Numerical Model

Welding Experiments

In preceding welding experiments (Refs. 4, 5), cylindrical specimens 10 mm in diameter and 50 mm in length of four different ceramic materials (alumina, zirconia, MgO-PSZ, silicon carbide and silicon nitride) were friction welded to the aluminum alloy Al-Si1MgMn — Fig. 1. Material data of all materials involved and welding parameters are given in the appendix.

Among other data, speed of rotation, burnoff and torque (PSZ specimen only) were recorded. Additionally, temperature in the friction plane was measured for a few PSZ samples using thermocouples of approximately 0.36 mm in diameter. To mount the thermocouples, holes were drilled by ultrasonic drilling, from the friction face to the cylindrical face. For all ceramic materials employed, sound joints were obtained. However, after the joints were removed from the machine, the alumina specimens showed inclined cracks, starting at the ceramic/metal interface, of up to 7 mm long.

Temperatures measured at two different radial positions in the friction plane show a steep increase in the initial stage of the process and a rather gentle increase afterward — Fig. 2. Toward the end of the friction process, an almost constant temperature was reached. The temperatures measured at the interface were in the range from \( T_{\text{max}} = 520^\circ\text{C} \) to \( T_{\text{max}} = 580^\circ\text{C} \), depending on radial position and welding parameters. Figure 2 also shows a calculated temperature curve, obtained by FEM calculations (Ref. 6).

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The strength of the joints was determined by tensile tests. Due to the small number of tests for each set of parameters, Weibull parameters for the strength distribution have not been determined.

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To indicate the potential of these material combinations, the highest value of fracture strength measured, supplemented by the strength of the bulk material in terms of the Weibull parameters, is given in Table 1.

Fracture occurred either at the interface or in the ceramic specimen at similar strength values — Fig. 3. If fracture occurred in the ceramic specimen, it took place close to the interface. A convex ceramic layer about 2 mm thick remained on the aluminum specimen. In the case where fracture occurred at the interface, electron microscopy revealed that on the entire surface of both fracture faces small particles of the other material adhered.

Residual Stresses

The processes in the friction stage of ceramic/metal friction welding are very complex and not yet completely understood. As a result, modeling the friction stage for numerical examinations would be rather involved. Since the object of this work is not to predict the friction process but to examine the influence of various parameters, e.g., geometry of the joint and material combination, on the resulting joint strength, a different ap-

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Approach was chosen. As will be shown subsequently, it is the thermal mismatch, effective during the cooling stage, that plays the most significant role in inducing residual stresses, therefore, the thermomechanical calculations begin at the end of the friction stage. All the parameters describing the state of the joint at this moment (temperatures, stresses, geometry) must be determined. They constitute the "initial conditions" of the subsequent calculations.

Estimation of Strains

At any time, stresses at any point in the metal specimen cannot exceed the yield stress (strain hardening neglected). The yield stress is strongly temperature dependent, so the maximum stress at any point in the metal depends on the local temperature. At 400°C, a temperature exceeded in a region from the interface up to some 5 mm in the metal at the end of the friction stage, the yield strength of the aluminum alloy is as low as 20 MPa, while Young's modulus is only reduced from 70 GPa to about 24 GPa at 400°C. Now, information about the deformation processes in the friction stage can only be passed along into the cooling stage by means of elastic stresses and the according strains. Therefore, at the end of the cooling process, the effect of stresses due to plastic deformation (yielding, formation of flash) during the friction stage, expressed in terms of elastic strains, is in the order of magnitude of

\[ \varepsilon_{\text{el}} \approx \frac{\sigma_{\text{yield}}}{E} = \frac{20 \text{ MPa}}{24000 \text{ MPa}} = 0.083\% \]  

with \( \sigma_{\text{yield}} \) being the yield stress at the local temperature reached at the end of the friction stage and \( E \) being Young's modulus.

On the other hand, the difference in thermal strains \( (\Delta \alpha \cdot \Delta T) \), which arises during cooling (when the yield strength is increasing again due to decreasing temperatures), is almost completely taken over from the metal close to the interface, as the ceramic is very stiff and does not yield. That difference in thermal strains is approximately

\[ \varepsilon_{\text{th}} = (\Delta \alpha \cdot \Delta T) \approx 1.5 \cdot 10^{-5} \frac{1}{K} \cdot 550K \approx 0.82\% \]  

This is about ten times larger than the elastic strains due to plastic deformation. The stress distribution after cooling is dominated by the stresses arising during the cooling stage, due to thermal mismatch. Therefore, at the end of the friction stage, when bonding is assumed to take place, the stresses in the metal are neglected.

Initial Conditions

Geometry: Geometry, i.e., the shape of the joint after the friction process with the flash fully developed, just before the cooling begins, is approximated by the shape of the joint after cooling. As deformations due to thermal strains during cooling are small compared to the dimensions of the specimen (< 1%), the error introduced by

![Fig. 1](image1.png)

Fig. 1 — A friction welded joint, finite element model and magnifications of the critical point.

![Fig. 2](image2.png)

Fig. 2 — A — Temperature at the interface vs. time (measured and calculated); B — Distance from the interface (calculated).
this simplification is small.

**Temperature Distribution.** To determine the temperature distribution at the end of the friction stage, one- and two-dimensional uncoupled heat conduction calculations have been made using the Finite Element Method (FEM). A time-dependent surface heat source in the interface was assumed. Its spatial integral over the friction surface was chosen to equal the measured friction power. This follows from the assumption that all the energy supplied by the friction process is dissipated into heat. The burnoff and the resulting creation of the flash causes severe problems for the FEM calculations. Hot metallic material is extruded from the region close to the interface into the flash. The interface therefore moves axially forward, deeper into the metal specimen. To model this process, the heat source would have to move forward, "deleting" these elements, which would be between the heat source and the ceramic surface. The deleted elements represent the material that, in reality, would have gone into the flash. This behavior could be modeled with finite elements, assuming the velocity of this forward movement (burnoff) is given. The burnoff rate, however, is not known in advance; it depends on all process parameters, as the mechanical and thermal processes are coupled in nature. Deleting these elements therefore is not possible. To account for the burnoff the following was done:

The finite elements representing the material that would have gone into the flash cannot be removed, but they can be rendered inactive. In that sense, inactive means that they no longer take part in the heat conducting and heat storing processes. This behavior can be obtained by artificially modifying their thermal properties from the moment they are to be excluded. Now, this moment is temperature dependent, as it is the material that is soft enough, i.e., hot enough, that is extruded into the flash. If, for temperatures higher than this "softening point," the material parameters are rendered in the sense that the specific heat is largely reduced and the thermal conductivity strongly increased, then the material that reaches this temperature will not further heat up but instantly conduct the heat to the cooler regions of the metal. The elements become inactive, identical temperatures at the ceramic friction face and at the assumed momentary metallic friction face are guaranteed. Thus, the coupling of thermal and mechanical processes is reintroduced through the temperature dependance of the softening point. The burnoff rate now results automatically from this calculation; it is equal to the increase of constant temperature in the inactive region; however, the softening temperature has to be known. In reality, this, of course, is a temperature range, depending on the material, the heat treatment and other parameters. If the temperatures at the interface have been measured in welding experiments of the respective materials for identical welding parameters, this softening temperature can be taken as the maximum temperature measured. The accuracy of the thermal calculation can then be estimated by comparing the burnoff measured in the experiments and the length of the ineffective region in the calculation. For all cases examined, these were in reasonably good agreement (error ≤ 25%), indicating that the total energy balance of the process is approximately accounted for by this simplified approach.

One drawback of this method is that only one-dimensional calculations are possible. In two-dimensional calculations, the heat source would not remain planar, which obviously does not make sense. Therefore, the radial component of the temperature distribution either has to be taken from temperature measurements or has to be approximated through a calculation neglecting the burnoff. The two-dimensional temperature distribution then has to be approximated by a combination of these. Figure 2 shows the result of a one-dimensional calculation. Figure 2A gives the temperature curve for the interface, calculated and measured; Fig. 2B gives the temperature distribution along the axis of symmetry of the specimen at the end of the friction stage. The inactive area ("burnoff") is clearly visible as an area of constant temperature.

**Thermal stresses.** From Fig. 2 it is obvious that the temperature distribution is very inhomogeneous, especially in the ceramic (PSZ), which has a very low thermal conductivity. The ceramic therefore is exposed to thermal stresses, even before stresses due to thermal mismatch can evolve (so far, ceramic and metal are assumed to be separate bodies). These thermal stresses are calculated in a separate FEM calculation for the ceramic specimen, with temperature being the only load. Thermal stresses in the metal are neglected as the temperature gradient is much smaller and, furthermore, thermal stresses are limited by the yield strength of the metal, which is very low in the hot region close to the interface, as pointed out earlier.

**Calculation of Residual Stresses.**

With the initial conditions being determined, the calculation of the residual stresses after the cooling (thermal mismatch) is done applying the following assumptions and simplifications:

- The temperature distribution is given by the results from the calculations described above;

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**Table 1 — Measured Maximum Fracture Strength**

<table>
<thead>
<tr>
<th>Material Combination</th>
<th>Maximum Fracture Strength (MPa)</th>
<th>Weibull Parameters of Bulk Ceramic in 4-Point-Bending(^a) (Ref. 7)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MgO-PSZ/Al-Si1MgMn</td>
<td>200 (29,000)</td>
<td>m = 20, b = 500</td>
</tr>
<tr>
<td>SiC/Al-Si1MgMn</td>
<td>70 (10,150)</td>
<td>m = 8, b = 350</td>
</tr>
<tr>
<td>Si(_3)N(_2)/Al-Si1MgMn</td>
<td>60 (8700)</td>
<td>m = 10, b = 600</td>
</tr>
<tr>
<td>Al(_2)O(_3)/Al-Si1MgMn</td>
<td>Not Tested (Cracks)</td>
<td></td>
</tr>
</tbody>
</table>

\(^a\) b is often referred to as \(\sigma_0\), but is different from \(\sigma_0\) used later in this report.

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**Fig. 3 — Fracture surfaces. A — Fracture at the interface; B — Fracture in the ceramic specimen.**
Due to nonuniform temperature distribution, the ceramic specimen is subjected to thermal stresses. These are determined separately and constitute initial conditions of the subsequent calculation.

The metal specimen is assumed to be free of thermal stresses at this moment, as the metal is able to reduce stresses by creep, relaxation and recrystallization; furthermore, the temperature gradient is much smaller than in the ceramic specimen;

A "perfect joint" is assumed at the interface; imperfections or interface cracks are neglected;

Linear elastic material behavior is assumed for the ceramic specimen;

The behavior of the metal is described by a multilinear plasticity law with limited isotropic hardening (yield strength $\sigma_Y = 180$ MPa, ultimate tensile strength $\sigma_{UTS} = 250$ MPa). The results presented here have been obtained using temperature-independent material parameters. The influence of temperature-dependent strength parameters was examined and was found to have only little effect on the residual stresses (Ref. 6);

For the results presented here, axial pressure is neglected;

Axial symmetry is assumed.

The result of this calculation is the stress distribution in the joint. In Fig. 4, the stresses along certain lines (indicated in the small plots) are plotted for joints of the four different ceramic materials used in the experiments. The underlying temperature distributions were calculated under the assumption that the maximum interface temperature is the same for all material combinations, as this temperature is primarily determined by the softening behavior of the metal. Thus for the different material combinations, the heat flux measured for PSZ was iteratively multiplied by a factor until the calculations yielded identical maximum temperature for all material combinations. In these diagrams, the distance from the interface or from the critical point, normalized with the radius $R$ of the specimens, is plotted in logarithmic scale.

From Fig. 4A, one can see that the ceramic is exposed to compressive stresses close to the interface. This behavior arises as all four ceramic materials have a much smaller coefficient of thermal expansion than the aluminum alloy. It should be noted that the stresses in the ceramic seem to exhibit singular behavior, i.e., they rise toward infinity as the critical point is approached. The equilibrium of forces requires that tensile stresses result in the metal. However, as the metal can undergo plastic deformation, these tensile stresses do not rise toward infinity. Also, the flash stiffens the metal part in the vicinity of the critical...
point, slightly reducing deformation and stresses in the metal and increasing those in the ceramic, compared to a hypothetical joint without flash. Figure 4D also shows singular behavior of the stresses in the ceramic. Axial stresses in the ceramic edge seem to rise toward infinity, showing differences for the different ceramic materials.

Figure 5 shows the stresses along the same lines for three joints of PSZ to AlSi1MgMn. This figure was obtained using different welding parameters than those from Fig. 4. The joints differ only in geometry, the first represents a joint after welding (with flash), the second a joint after removal of the flash (ideally cylindrical joint). The third represents a joint with an additional groove close to the interface, machined after removal of the flash — Fig. 6.

The singular character of the radial stresses in the ceramic close to the interface is lost if the flash is removed or if a groove is added after removal of the flash. The same is true for the axial stresses along the ceramic edge. However, the axial stresses at the interface, close to the critical point, are shifted from negative values into the positive range, thus increasing the dangerous tensile stresses in the ceramic and at the interface.

From these stress distributions alone, it is difficult to decide which material or which geometry is most suitable for friction welding to the aluminum alloy AlSi1MgMn. Axial stresses at the interface are highest for the joint with zirconia, axial stresses along the edge of the ceramic specimen are highest for the joint with silicon carbide. In addition, the stresses have to be put in relation to the strength of the corresponding ceramic material. The difficulties increase, if the distribution of stresses does not only differ quantitatively but qualitatively (Fig. 5). In this case, it is almost impossible to rate the different stress distributions.

Therefore, a criterion has to be found that allows comparison of different stress distributions with respect to their harmfulness to the ceramic specimen — a criterion that fulfills this requirement is the probability of failure.

Residual Stress Measurements

Only a few measurements of residual stresses were undertaken on the PSZ-AlSi1MgMn joint, due to the difficulties of these measurements in ceramic/metal joints. Measurements are difficult because of extremely high stress gradients close to the critical point. Furthermore, the area of interest, the ceramic surface close to the critical point, is hidden behind the flash. Removal of the flash, however, alters the residual stress distribution dramatically — Fig. 5. Also, PSZ cannot be drilled by conventional methods, making measurement by the drilling method almost impossible.

![Fig. 5 — Stresses along different lines in the joint for different flash geometry.](image-url)
The measurements of radial stresses in the metal specimen by the drilling method (conducted at the Staatliche Materialprüfungsanstalt, MPA, University of Stuttgart, a partner in the friction welding research program) yielded stresses similar to those obtained by the FEM calculations (error ≤ 15%). The stresses were measured between 2 mm and 6 mm from the interface. Taking all the simplifications and assumptions into account, this is in reasonable agreement.

The probability of failure for the ceramic component of the joint, this probability of failure is calculated by the program STAU, developed at the Institute for Reliability and Failure Analysis, University of Karlsruhe (Refs. 8, 9). The program needs for input the stress distribution in the form of FEM results, the Weibull parameters of the ceramic material and the failure criterion (multiaxiality criterion) to be used. It must be emphasized that only failure in the ceramic specimen is taken into consideration by this calculation, failure at the interface or in the metal cannot be predicted.

For the calculation of the probability of failure, a few more simplifications have to be made. The most important simplification is necessary because of the singular character of the stresses in the vicinity of the free edge of the interface. In linear elastic theory, stresses at such a point rise toward infinity. As the FEM is not able to describe this behavior in an adequate manner, FEM results for the elements next to this point are meaningless. These elements have to be exempt from the calculation of the probability of failure. This will cause an error in the calculated probability of failure, because a small but highly loaded region is neglected. However, the size of this region is chosen identically for all calculations. The results, therefore, do have meaning if they are compared to each other. The values obtained do underestimate the probability of failure and should be compared only qualitatively to experimental results. It also has to be noted that the size of the neglected region, depicted in Fig. 6, is 40 μm x 40 μm. For the PSZ used, this equals approximately the size of one grain (average 50 μm). Thus, even without a singularity, the FEM results would be inaccurate in such a small region, as the continuum mechanics approach disregards the real microscopic structure of the material.

Equation 3 gives the basic equation of the calculation of the probability of failure, implemented in the program STAU,

\[
P_f = 1 - \exp \left( - \frac{1}{V_0} \int \int \int \frac{1}{4\pi k} \left( \frac{\sigma_{eq}}{\sigma_0} \right)^m \sin \theta \, d\theta \, d\varphi \, dV \right)
\]

where \( V \) is the volume of the specimen, \( V_0 \) is the unit volume, \( \sigma_0 \) is the equivalent stress (not identical with the yield criterion in plasticity), describing the effect of the multiaxial stresses in terms of an equivalent uniaxial quantity (calculated using a flaw

**Table 2 — Probability of Failure after Cooling (with Flash)**

<table>
<thead>
<tr>
<th>Material</th>
<th>PF</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al-SiMgMn-PSZ</td>
<td>3.1*10^-8</td>
</tr>
<tr>
<td>Al2O3</td>
<td>1.0*10^-2</td>
</tr>
<tr>
<td>SiC</td>
<td>1.3*10^-2</td>
</tr>
<tr>
<td>Si3N4</td>
<td>2.3*10^-5</td>
</tr>
</tbody>
</table>

**Table 3 — Probability of Failure for Different joint Geometries after Cooling**

<table>
<thead>
<tr>
<th>Material</th>
<th>Joint Geometry</th>
<th>PF</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al-SiMgMn-PSZ</td>
<td>with Flash</td>
<td>3.3*10^-9</td>
</tr>
<tr>
<td>Al2O3</td>
<td>Flash Removed</td>
<td>9.1*10^-11</td>
</tr>
<tr>
<td>SiC</td>
<td>Groove Added</td>
<td>2.8*10^-11</td>
</tr>
</tbody>
</table>

Fracture of ceramic materials is initiated from natural volume flaws or from surface flaws. Fracture occurs if one crack exhibits unstable (catastrophic) crack growth, i.e., if for the most dangerous combination of crack position, crack length, crack orientation and stress distribution the fracture resistance of the material is exceeded. As flaws are distributed in a statistical manner, strength of ceramic parts can only be given in terms of probability of failure.

For the ceramic component of the joint, this probability of failure is calculated by the program STAU, developed at the Institute for Reliability and Failure Analysis, University of Karlsruhe (Refs. 8, 9). The program needs for input the stress distribution in the form of FEM results, the Weibull parameters of the ceramic material and the failure criterion (multiaxiality criterion) to be used. It must be emphasized that only failure in the ceramic specimen is taken into consideration by this calculation, failure at the interface or in the metal cannot be predicted.

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Equation 3 gives the basic equation of the calculation of the probability of failure, implemented in the program STAU,
model and a failure criterion, $\theta$ and $\phi$ are the angles describing the orientation of a flaw and $c_0$ and $m$ are the Weibull parameters of the ceramic material.

Before the results of these calculations are presented, the course of such a calculation is demonstrated in a scheme — Fig. 7.

**Results**

Table 2 gives the probability of failure $P_f$ for the different material combinations after cooling, e.g., under residual stresses only.

Table 2 clearly shows that the joint with PSZ has the lowest probability of failure, while that for the joint with $\text{Si}_3\text{N}_4$ is about 3 orders of magnitude, for the ones with $\text{Al}_2\text{O}_3$ and $\text{SiC}$ even 5 orders of magnitude higher. More important for the application of these joints is the strength, i.e., the failure probability under external load, which can be obtained for the joints. It is calculated for a joint subjected to tensile loading. Again, it has to be emphasized that these strength values describe failure of the ceramic due to residual stresses and stresses from external loading. No statement is made about the strength of the interface. Interfacial failure may occur before these values are reached. Figure 8 shows the probability of failure versus the applied tensile stress. The joint with $\text{Al}_2\text{O}_3$ yields the lowest strength, the one with $\text{SiC}$ is slightly better, but shows larger scatter (i.e., the curve is less steep). The joint with PSZ yields an excellent scatter at even higher strength, whereas the joint with $\text{Si}_3\text{N}_4$, which reaches the highest strength values, shows very wide scatter. For small or zero loading, its probability of failure exceeds that for the joint with PSZ (Table 1).

From Table 2 and Fig. 8 it follows that the $\text{Al-Si1MgMn}$-PSZ joint seems to have the highest potential in regard to joint strength at small scatter. Table 3 gives the probability of failure for the $\text{Al-Si1MgMn}$-PSZ joint with flash and the joints of the same material combination with the flash removed and a groove added.

The removal of the flash seems to have a positive influence on the residual stresses. The probability of failure is strongly reduced. An additional groove reduces the probability of failure even further. Under external load, the joint with the flash fails at lower tensile stresses than either the one without flash or the one with an additional groove — Fig. 9. A better picture of this situation can be obtained if the results are plotted on a Weibull diagram, which clearly shows that for all stress levels the joint with the flash removed exhibits a lower probability of failure, i.e., it should be stronger. On the other hand, there is the increase of the axial stresses at the interface (Fig. 5C), which may lead to early interface failure, thus it

![Fig. 8 — Probability of failure vs. external load for different material combinations.](image)

![Fig. 9 — Probability of failure vs. external load for PSZ-Al-Si1MgMn joints; A — Linear scale; B — Logarithmic scale.](image)
is difficult to judge whether the higher strength values predicted can really be obtained. However, it is clear that it has strong influence on the behavior of the joint. Results for variations of geometry or process parameters can be found in Refs. 5 and 6. Experiments verifying these numerical results in terms of strength were undertaken and the results will be published in a separate paper (Ref. 10), together with more experimental examinations.

Conclusions

For a wide range of process parameters, strength values can be expected that reach or even exceed those commonly obtained from other joining techniques. The theoretical examinations clearly indicate that the edge geometry of the joint in the vicinity of the interface (flash) has strong influence on joint strength. Improvement of joint strength seems to be possible by optimization of the geometry in the vicinity of the interface. The influence of welding parameters on joint strength through residual stresses is comparatively small. However, welding parameters may have great influence on joint strength by means of the bonding process, resulting in higher or lower interface strength.

Furthermore, the good suitability of the combination PSZ-Al-Si1MgMn for the friction welding process, which was observed in the experiments, was confirmed through the calculations. Finally, the FEM post-processor STAU proved to be a powerful tool to rank the results of FEM calculations for different materials, geometries or loads in terms of susceptibility to failure.

Acknowledgment

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References

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8. Institute for Reliability and Failure Analysis, University of Karlsruhe, P.O. Box 3640, D-76021 Karlsruhe: STAU — A post processor for a finite elemente program for the calculation of the failure probability of multiaxially loaded ceramic components, User Manual 2.1, 1997.

Table A — Material Data

<table>
<thead>
<tr>
<th>Material:</th>
<th>AI-SiMgMn</th>
<th>PSZ</th>
<th>Al₂O₃</th>
<th>SiC</th>
<th>Si₃N₄</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young's Modulus E</td>
<td>(GPa)</td>
<td>70</td>
<td>200</td>
<td>400</td>
<td>350</td>
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<tr>
<td>Poisson's Ratio ν</td>
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<td>0.3</td>
<td>0.26</td>
<td>0.22</td>
<td>0.20</td>
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<tr>
<td>Density ρ</td>
<td>(g/cm³)</td>
<td>2.7</td>
<td>6.0</td>
<td>3.9</td>
<td>3.0</td>
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<tr>
<td>Thermal Expansion Coeff. α</td>
<td>(10⁻⁶K⁻¹)</td>
<td>23.4</td>
<td>11.0</td>
<td>8.0</td>
<td>4.0</td>
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<tr>
<td>Thermal Conductivity λ</td>
<td>(W/mK)</td>
<td>200.0</td>
<td>2.5</td>
<td>26.0</td>
<td>100.0</td>
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<tr>
<td>Specific Heat c</td>
<td>(J/gK)</td>
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<td>0.5</td>
<td>0.9</td>
<td>0.6</td>
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<tr>
<td>Yield Strength oₙ</td>
<td>(MPa)</td>
<td>180</td>
<td>—</td>
<td>—</td>
<td>—</td>
</tr>
<tr>
<td>Ultimate Tensile Strength oₚₕ</td>
<td>(MPa)</td>
<td>250</td>
<td>—</td>
<td>—</td>
<td>—</td>
</tr>
</tbody>
</table>

Weibull Parameters (4-point-bending):

| Weibull Strength b (σₖ) | (MPa) | 500 | 300 | 350 | 600 |
| Weibull Modulus m (σₚₖ) | — | 20 | 11 | 8 | 10 |

Table B — Welding Parameters

| Speed of Rotation | (rpm) | 1500-5000 |
| Friction Pressure | (MPa) | 20-80 |
| Forge Pressure | (MPa) | 30-150 |
| Friction Time | (s) | 0.5-1.5 |