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Discussion

Reply to the paper entitled "Comments on thermal shock resistance of yttria stabilized zirconia with Palmqvist indentation cracks by G. Fargas, D. Casellas, L. Llanes, M. Anglada" by F. Tancret [J. Eur. Ceram. Soc. 23 (2003) 107–114]

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We have read with much interest the manuscript of the paper "Comments to thermal shock resistance of yttria-stabilized zirconia with Palmqvist indentation cracks"[1] by Tancret in which the author shows that our results[2] on Y-TZP ceramics are consistent with the theoretical and experimental approach developed previously by Tancret and Osterstock.[3] By using our experimental results, he calculates the critical quenching temperature of our Y-TZP ceramics. Finally, he acknowledges "our general contribution to the understanding of the mechanical behaviour of ceramics".

We appreciate these comments and the use of our results for his calculation, but we would also like to clarify the main point made by Tancret about Hasselman's R'''' thermal shock resistance parameter and which seems to be the fundamental, if not the only one, criticism to our paper.

In our work,[2] it was studied the thermal thermal shock behaviour of two tetragonal zirconia polycrystals stabilised with 2.5% molar yttria and with different fracture toughness. The investigation was concentrated on the analysis of stable crack extension of indentation Palmqvist cracks in the quench indentation test. It was shown that the ratio between the thermal stress intensity factor and the fracture toughness can be easily obtained by measuring the stable crack extension and that deviations from the expected maximum stable crack extension during thermal shock could be accounted for by subcritical crack growth and by a reduction in the level of residual stresses. It was also concluded that for small indentation loads, *R*-curve effects become important and should be considered to explain the experimental results.

Then, the aim of our paper was *not* determining R'''' for Y-TZP with *natural flaws*, as suggested by Tancret,[1] and we never claimed it. In fact, we believe that the approach of Tancret and Osterstock[3] is a simple and excellent approach for finding this parameter for a material with natural flaws, but we restricted our work to the thermal behaviour of two materials with *artificial indentation cracks* and with the same chemical composition, but with different mechanical properties.

It is well known that the thermal shock resistance parameter is defined as,

$$R'''' = \left(\frac{K_{\mathrm{Ic}}}{\sigma_{\mathrm{f}}}\right)^2 (1+\nu) \qquad (1)$$

where K_{Ic} is the fracture toughness, and σ_{f} represents the strength. R'''' is affected by the flaw size that may cause fracture under thermal shock. This depends on many parameters (starting powder, processing, sintering and machining conditions, etc.) as well as on environmental degradation by slow crack growth. In addition, the experimental fracture strength is influenced by the residual stresses, which are left in a surface layer by machining, or, in the case of composites, they might be distributed over all the volume of the body by differences in the expansion coefficients of the constituent phases.

An artificial indentation crack will induce fracture during thermal shock if the combination of crack configuration, length, residual indentation and thermal stresses are such that the fracture condition is obeyed at thermal stresses that are lower than for the natural cracks.

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So, in our paper,[2] we have calculated R'''' when the flaw that causes fracture is a well-defined crack with a well-defined residual stress field. We do not claim that the value of R'''' calculated in this is way is the same as when there are only natural flaws. We only intended to show that R'''' of Eq. (1) depends on the residual stress field and on environmental crack growth, so that an expression was derived for the simple case when the critical flaw is a Pamlqvist indentation crack of total length l_0. Thus, by assuming that the thermal stress intensity factor can be estimated as $Y\sigma_{\mathrm{th}}\sqrt{l_0/2}$where σ_{th} is the maximum thermal stress and Y is a constant, we obtain Eq. (19) of our paper[2]:

$$R'''' = \frac{1}{2} l_0 \left(\frac{Y^2}{R_{0\mathrm{cr}}^2} \right) (1+\nu) = 8\mu^2 l_0 Y^2 (1+\nu) \tag{2}$$

In this way, it is clearly seen that R'''' depends also on l_0 and μ, which is a factor that takes into account environmental indentation crack growth in air.[2] For the same material, microstructure and crack configuration, but with no residual stresses,

$$R'''' = \frac{1}{2} l_0 Y^2 (1+\nu) \tag{3}$$

Since in Y-TZP μ has been estimated to be close to 0.7 (just by measuring crack extension with our approach), R'''' would be as high as about eight times the value of R'''' without residual stresses. This emphasizes the importance residual surface stress fields on the thermal behaviour. So, conclusion (i) of the paper by Tancret,[1] that is, that R'''' is proportional to the size of the artificial (indentation) crack, it was already clearly established in our paper.

In fact, R'''' for Y-TZP with only *natural flaws*, would be much smaller than for artificial *indentation cracks*. This is because critical cracks in our materials are of about 20 μm, while artificial indentation cracks are around an order of magnitude larger (and also there are under a tensile residual stress).

Our approach allows us emphasising the role of the residual stresses and environmental crack growth, and, at the same time, allows us to calculate the level of reduction in the residual stress field because of slow crack growth.

On the other hand, in our analysis, we defined a parameter R_0 as,

$$R_0 = \frac{K_{\mathrm{th}}}{K_{\mathrm{Ic}}} = \frac{Y\sigma_{\mathrm{th}}\sqrt{l_0/2}}{K_{\mathrm{c}}} \tag{4}$$

It represents the ratio between the thermal stress intensity factor that a surface crack will undergo and the fracture toughness. The analysis of the fracture behaviour under thermal shock by using this ratio is straightforward when the critical crack is known.

By the contrary, Tancret[1] uses the parameter R_{th}, which is defined as,

$$R_{\mathrm{th}} \equiv \left(\frac{K_{\mathrm{Ic}}}{\sigma_{\mathrm{th}}} \right)^2 \tag{5}$$

where σ_{th} is the thermal stress. In fact, it can be shown that both values are related by,

$$R_{\mathrm{th}} = \frac{Y^2 l_0}{2R_0^2} \tag{6}$$

The physical meaning of R_{th} is that it measures the severity of the thermal shock by using the thermal stress, while R_0 measures it by using the thermal stress intensity factor. Since this last quantity is well known for indentation cracks, it is very useful for the analysis of these artificial cracks during thermal shock. On the contrary, when applied to materials without well known artificial cracks, since the distribution of natural flaws usually is not known with enough precision, then R_{th} is more convenient.

For indentation cracks, Eq. (11) of Tancret[1] can be compared with our previous Eq. (6) with $Y = (\Omega\pi)$, $z = (l/l_0)^{1/2}$, using the expression for R_0 of Eq. (15) derived in Fargas et al.,[2] that is,

$$R_0 = \frac{z-\mu}{z^2} \tag{7}$$

We obtain

$$R_{\mathrm{th}} = \frac{z^4 Y^2 l_0}{2(z-\mu)^2} \tag{8}$$

Similarly, by using Eqs. (4) and (7), we have

$$\sigma_{\mathrm{th}} = \frac{z^2}{z-\mu} \frac{K_{\mathrm{Ic}}}{Y\sqrt{l_0/2}} \tag{9}$$

which is equivalent to Eq. (12) of Tancret.[1] If we restrict to the case where no relaxation of indentation residual stresses by slow crack growth occurs, then $\mu = 1$ and these equations are identical to the corresponding equations derived by Tancret.[1]

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