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Statistical failure analysis of adhesive resin cement bonded dental ceramics

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Abstract

The goal of this work is to quantitatively examine the effect of adhesive resin cement on the probability of crack initiation from the internal surface of ceramic dental restorations. The possible crack bridging mechanism and residual stress effect of the resin cement on the ceramic surface are examined. Based on the fracture-mechanics-based failure probability model, we predict the failure probability of glass-ceramic disks bonded to simulated dentin subjected to indentation loads. The theoretical predictions match experimental data suggesting that both resin bridging and shrinkage plays an important role and need to be considered for accurate prognostics to occur.

Keywords

Ceramics; Dental restoration; Adhesive resin cement; Finite element; Failure probability

1. Introduction

The structure of a natural tooth consists of a hard, rigid, translucent ceramic-like outer enamel shell composed primarily of densely packed calcium phosphate crystals which is intimately attached to a softer, more flexible, polymer/crystalline composite structure known as dentin. In their natural unrestored state, teeth are surprisingly durable structures able to withstand repeated loads during mastication without catastrophic failure. Exactly how the enamel is attached to the underlying dentin, and how this intimate attachment to the dentin substrate influences the response of the enamel to external stimuli is still not completely understood.

During service, teeth are subjected to any number of external stimuli that can contribute to their partial or complete degradation. Mechanical, chemical and bacterial stimuli can contribute to the degradation process and lead to some need for repair or replacement of the damaged tissues. Once the defective tooth structure has been removed and the area restored, the tooth can return to normal function. Numerous materials and techniques are currently used to restore

the missing tooth structure. Each presents its own unique set of benefits and risks, however, each can provide the patient with adequate function over some useful lifespan.

It has been estimated that over 60% of all dental treatment performed involves the re-restoration of teeth [1]. It is clear that all restorations have a finite service life. The inevitability of failure for all restorations demands that we study the process to better understand the principal failure mechanisms with the hope that this knowledge can aid to improve the design, performance and even predictability of restorations.

An ideal restorative technique would minimize any unnecessary removal of healthy tissue, replace the missing structure with a material that performs like the tooth structure it replaces, and protects the remaining tooth structure from further degradation or fracture. Over the past decade, new restorative materials and techniques have been introduced that generally fulfill these specifications and have challenged the clinical performance of the more traditional restorative paradigms. Adhesive resin retained ceramic restorations are among the most biomimetic restorative techniques that have emerged from these recent developments and warrant careful evaluation and examination.

Glasses and glass-ceramics can be made to be optically compatible with natural enamel and dentin and are among the most stable, biologically inert materials used in restorative dentistry. In their most common dental application form, they are used as a veneering material sintered directly to a metal substructure. In this form, fractures are minimized by the strengthening effect of the underlying metal support [2], but the optical benefits of the ceramic are often compromised by its presence. The unwanted optical effects of the underlying metal must be hidden with a layer of opaque porcelain that can render the restoration “lifeless” due to its lack of translucency.

The routine clinical application of substantially glassy ceramic materials has been hindered by their high fracture rates and poor clinical longevity when used in conjunction with conventional acid based cementation techniques [3,4]. However, there is significant laboratory and clinical evidence to suggest that the application of adhesive resin cement can act to reinforce ceramic restorations and improve clinical longevity [3–7]. The clinical performance of resin bonded ceramic inlays, onlays, crown and veneers have been well documented [8–15] and in some cases have been shown to compete favorably with cast metal restorations [16]. Additionally, the adhesive nature of some restorations have been shown to reinforce the remaining tooth structure by increasing stiffness and fracture strength [17–19].

There is evidence to suggest that the adhesive resin cements may contribute to the clinical longevity of ceramic restorations by decreasing the probability of crack initiation from the internal surface [3,4,15,20–22]. A similar strengthening effect has been observed in a model trilayer “onlay-like” system (Fig 1) [23]. The fracture mechanics based failure probability model [24] was employed to predict the failure probability distribution of model onlay restorations subjected to indentation load to failure testing. Surface flaw distribution derived from biaxial data was used to predict the theoretical failure probability of the more complex trilayer model. The predicted values for the non-bonded ceramic supported by the simulated dentine substrate fell within the 90% confidence interval of the experimental data. However, the predicted values for the completely bonded case showed a somewhat higher failure probability than the experimentally derived values. It has been suggested that this discrepancy may be attributed to the possible bridging effect by the adhesive resin cement on the interfacial surface defects in the ceramic that was not accounted for in the analytical model [23]

The most common failure mode reported for ceramic restorations of all types is bulk fracture in the ceramic material (Figure 2). It has been demonstrated by evaluation of clinically failed glass-ceramic restorations that a majority of these fractures (> 90%) are initiated from flaws

and stresses originating from the adhesive resin cement interface rather than from the contact surface of the restoration itself [25–27]. This suggests that for this restoration type, Hertzian damage [28] is of less concern.

The goal of this work is to quantitatively investigate the effect of adhesive resin cement on the probability of crack initiation from the internal cemented surface of ceramic dental restorations. We hypothesize that the resin cement on the ceramic surface influences failure probability of adhesive ceramic dental restorations through crack bridging mechanism and residual stress due to its curing process. A simplified trilayer onlay model such as dental ceramic disk bonded to simulated dentin (Figure 1) is used to represent the more complex clinical situation as shown in Figure 2. Distribution of the surface flaws is assumed to be in the form of

$$N(\sigma_{Cr}) = k\sigma_{Cr}^m \quad (1)$$

where σ_{Cr} , N , k and m respectively represents the critical stress, critical stress distribution function, and the scale and shape parameters k and m [29]. The k and m parameters are obtained by curve-fitting the experimental biaxial flexural test data on ceramic disks or plates. Given the same stress level, the crack opening displacement is restrained by the adhesive resin cement layer and the resulting reduction of the stress intensity factor is calculated by the simplified 2D and 3D local models. This bridging mechanism is incorporated into the critical stress distribution function. The curing process is simulated through linearly elastic FEA models with constant 1% volume shrinkage [30]. Based on the experimentally determined parameters and numerical simulations, failure probability distributions for biaxial tests with adhesive resin cement coated ceramic disks and indentation tests on simple trilayer models are theoretically predicted.

2. Experimental methods

2.1 Biaxial test for determination of ceramic flaw population

Biaxial data reported previously [6,23] using borosilicate glass and a new set of data using a dental glass-ceramic were utilized to verify the validity of our analytical model. Briefly, glass disks of about 1 mm thickness were formed by sectioning a 5/8 inch diameter borosilicate glass rod (item # 8849K62 McMaster Carr Supplies, Chicago, IL) with a slow speed diamond wheel saw and water coolant. Similarly, leucite-reinforced glass-ceramic (ProCAD, Ivoclar, Amherst, NY) plates were fabricated by sectioning the preformed 12.5 mm × 14.5 mm rectangular CAD (Computer Aided Design) blocks into 1.6 mm thick sections. All surfaces of the disks were hand ground on a flat glass plate with 600 grit SiC powder slurrie, so as to assure a consistent surface finish. One surface of each disk was etched with 5% hydrofluoric acid-etching gel (IPS Ceramic Etching Gel, Ivoclar USA, Amherst NY) for 60 seconds, rinsed with water and air dried with an air/water syringe. The dental ceramic disks were arbitrarily separated into two sub groups: one was tested as is while the other group was treated with a silane coupling agent (Silane Primer; SDS Kerr, Orange, CA) and coated with an approximately 100 μm resin cement film [23].

Failure behavior of the treated disks was characterized by the ball-on-ring biaxial flexure test. A schematic of the test apparatus is shown in Fig. 3. The specimens were centered on a 12.5mm diameter support ring with the treated surface placed face down. A vertical load was applied to the center of the disks with a spherical indenter load at a crosshead speed of 0.5 mm/min. Detailed dimensions are summarized in Table 1a. There was no observed indentation of the ring into the surface of the cement during the test.

To study the fracture behavior of ceramic restorations bonded to a compliant substructure such as dentin, a simplified trilayer onlay model was used. As shown in Fig. 1, the dental glass-

ceramic plates and glass disks were bonded onto the top of dentin-like substrate using standard dental bonding techniques and resin cement (Nexus 2, Kerr Dental, Orange CA). The dentin-like substrate consisted of a glass fiber reinforced polymer (Garolite G10, McMaster Carr Supplies, Chicago IL) that has an elastic modulus similar to dentin. The bonded surfaces were treated in a manner similar to those used to cement dental ceramic restoration to dentin. The ceramic surface was etched and silanated as described previously. The dentin-like substrate was etched with 35% phosphoric acid for 30 sec to clean the surface, rinsed with water and lightly air dried prior to a 15 second application of the adhesive (Optibond Solo Plus, Kerr, Orange, CA). The adhesive was then air thinned to drive off the volatiles and light cured for 20 sec prior to cement application and light curing (Nexus 2 dual cure cement, Kerr Dental, Orange CA). The adhesive was applied to assure good wetting between fiber glass/epoxy substrate and the resin cement.

The completed trilayer specimens were loaded from the top surface with a spherical WC indenter using a universal testing machine. Detailed dimension is summarized in Table 1b. In order to protect the ceramic surfaces from contact damage and increase the probability of interface initiated cracks, the indenter was covered with a thin adhesive polyethylene tape. Loading and observation of the crack initiation at the interface were achieved using two slightly different methods. For the transparent glass specimens, crack initiation could be viewed directly from the side of the specimen through a 10 x binocular microscope with transillumination of the specimen during continual loading. For the translucent dental ceramic materials, this could not be achieved. Therefore, the specimens were step loaded at increasing 50 N increments and observations were periodically made from the top surface using the 13.5 x binocular dental loupes and transillumination to detect cracks. This was considered to be analogous to a clinical exam. The breaking loads for the cracks initiating at the bottom surfaces were recorded.

2.2 Material property measurement

The elastic moduli of some of the composite materials were obtained by ultrasonic

measurements of longitudinal $v_l = \sqrt{\frac{\lambda + 2\mu}{\rho}}$ and shear $v_t = \sqrt{\frac{\mu}{\rho}}$ wave velocities, where ρ is the density, λ is the Lamé's parameter and μ is the shear modulus. The Young's modulus $E = \frac{\mu(3\lambda + 2\mu)}{\lambda + \mu}$ and Poisson ratio $\nu = \left(\frac{v_l^2}{2v_t^2} - 1\right) / \left(\frac{v_l^2}{v_t^2} - 1\right)$ were calculated from measured longitudinal and shear wave velocities. The density was determined by Archimedes's method. The ultrasonic velocity measurements were performed at 10MHz by the pulse-echo method using both immersion and contact techniques for longitudinal wave velocity and by the contact method for shear wave velocity. A Panametrics 5073 PR pulser/receiver and a Hewlett Packard 54504-A 400 MHz digital oscilloscope were used for time delay measurements by the signal overlapping technique. The sample thickness was measured by a micrometer. The precision of the measurement is limited by the flatness and parallelism of the sample surfaces and by the couplant effect for the shear wave velocity measurement. We estimate at least three correct digits in the determination of the Young's and shear moduli on our samples. The results are summarized in Table 2.

Quantitative assessment of the surface roughness was accomplished using a WYCO optical profilometer (Veeco NT3300, Veeco Metrology, Tucson, AZ). All scans were performed utilizing a 10X objective under vertical scanning interferometry (VSI) corrected for curvature and tilt. Ten scans in pre-determined areas were taken from select prepared glass and glass-ceramic surfaces to provide measurements of arithmetic mean roughness ($R_a = 0.49 \pm 0.09 \mu m$), and root-mean square roughness ($R_q = 0.64 \pm 0.10 \mu m$).

3. Theoretical analysis

3.1 Critical flaw distribution and local crack stress intensity model

3.1.1 Critical flaw distribution—In both the biaxial test (Fig. 3) and the trilayer indentation tests (Fig. 1), the fracture initiates from a flaw located on the bottom surface of the top layer in contact with the indenter. We define as the critical stress σ_{Cr} the given stress state σ at which a crack with critical length a_{Cr} begins to propagate. The surface flaws from which fracture initiates are the result of sample preparation and surface treatment prior to bonding and can be related to surface roughness. In analyzing the stress distribution at surface flaws it is reasonable to use the following assumptions: (1) the critical flaw distribution is isotropic; (2) each critical flaw is considered as an equivalent small surface crack; (3) critical flaws are sufficiently far apart (the spacing between critical flaws is much larger than the depth) and thus the interaction between them is neglected. The hand ground 600 grit SiC slurry produces an average surface Ra of about $0.5 \mu\text{m}$ and the roughness is found to be similar in two directions. Assumption (1), therefore, can be justified. Since stress state depends on radius we subdivide the top layer on regions where the stress assumed to be approximately constant and therefore the corresponding critical flaw size is different for different regions. If there exists a region containing a flaw which is equal to the critical flaw size, the failure starts from that region. Therefore, assumption (3) that the critical flaws are far apart is reasonable. A distribution $N(\sigma_{Cr})$ gives the number of critical flaws per unit area for which the critical stress varies between zero and σ_{Cr} , and can be interpreted as the crack density function. As in Chao and Shetty [29] and Wang et al [23,24], the critical stress density distribution N is assumed to be in the form of Equation (1).

3.1.2 Local model for determination of stress intensity factor for system with and without adhesive resin cement—Since the critical flaws are non-interacting, one may obtain critical stress levels in distribution (1) by an analysis of stress intensity factors of isolated cracks. To compute crack stress intensity factors at a given level of indentation load in the models of Fig. 1 and Fig. 3, one needs to perform 3D finite element analysis of these models which is computationally expensive. For this reason we have employed the method of local crack analysis by calculating the stress distribution in the models (Figs. 1 and 3) without a crack and taking the stress distribution obtained in the vicinity of an isolated crack to determine the stress intensity factor. This approach was shown to be sufficiently accurate by comparison with parametric study performed by 3D finite element analysis, which will be described elsewhere. In general, the variation of normal stress along the crack depth is found to be negligible if the crack size is small. Therefore, only tension loading is used in local models. Deviation from this assumption will be reported in detail elsewhere.

In order to quantitatively address the possible bridging effect of the adhesive resin cement, we introduce 2D and 3D local crack models with and without resin cement as shown in Figs. 4 and 5. It is assumed that only mode I failure occurs. The system without resin cement is considered first. In the 2D plane-strain local model without resin cement, a single edge crack inside a semi-infinite body is introduced (Fig. 4a). The body is under a constant tensile stress σ . Similarly, in the 3D local model without resin cement, a half-penny crack inside a semi-infinite body is subjected to a constant tensile stress σ normal to the crack face as shown in Fig. 5a. For both local models without resin cement, the crack intensity factor K_I at the crack tip (point A) can be represented as:

$$K_I = f \times (\sigma \sqrt{\pi a}), \quad (2)$$

where a represents the radius of a half-penny crack in the 3D model, and the crack length in the 2D model. It is well known that the value of the parameter f is $1.025 \times 2/\pi$ for the 3D model and 1.12 for the 2D model [31].

When a thin layer of adhesive resin cement is bonded to the ceramic surface, the crack opening displacement of a surface flaw is constrained by the resin. Therefore, given the same constant tensile stress σ normal to the crack surface, the stress intensity factor will be reduced. This effect of resin cement on the stress intensity factor in our local model leads to modification of factor f in Eq. (2). In order to simulate this effect, a perfectly bonded semi-infinite body representing the resin cement is included in the 2D and 3D local models as shown in Figs. 4b and 5b. The far tensile stress σ_g in the resin cement region is chosen to satisfy $\sigma/E = \sigma_g/E_g$ so that the far interfacial displacement field is compatible. For these local models the values of factor f for both 2D and 3D models are not readily available. Hence, we employ the FEA model to calculate the value of f . The results are summarized in Table 3 where f and f' represent the value without and with resin cement respectively.

3.1.3 Determination of parameter k in critical flaw distribution—The critical stress σ_{Cr} in distribution (1) for a given flaw size, a , is determined from Eq. (2):

$$K_{IC} = f \times (\sigma_{Cr} \sqrt{\pi a}) \quad \text{without adhesive,} \quad (3)$$

where K_{IC} is the critical stress intensity factor (fracture toughness) which is a material property. Therefore, after substitution of Eq. (3) into Eq. (1) it describes the flaw size distribution for the surface without resin cement. With use of the local model, the corresponding equation for the crack bridged by resin cement is:

$$K_{IC} = f' \times (\sigma'_{Cr} \sqrt{\pi a}) \quad \text{with adhesive,} \quad (4)$$

where fracture toughness K_{IC} and crack size a remain unchanged. Eqs. (3) and (4) lead to the relation:

$$\sigma_{Cr} = \frac{f'}{f} \sigma'_{Cr}, \quad (5)$$

where f'/f is less than 1 as shown in Table 3. Thus σ_{Cr} is less than σ'_{Cr} , which means that the resin cement layer constrains the crack opening and high level of critical stress is required for the sample failure.

Inserting Eq. (5) into Eq. (1), we obtain the effective critical stress density distribution N for the surface with resin cement:

$$N(\sigma'_{Cr}) = k' \sigma'^m_{Cr}, \quad (6)$$

where

$$k' = k \left(\frac{f'}{f} \right)^m. \quad (7)$$

In the analysis below, Eq. (1) is used to predict the failure probability for the surface without resin cement, while Eq. (6) is used for the surface with resin cement.

3.1.4 Determination of parameters m and k in Eq. (1) from biaxial indentation experiments—In order to determine experimentally the parameters m and k in Eq. (1), we employ the fracture-mechanics-based statistical method developed by Wang et al. [24]. First,

experimental data points of cumulative failure probability distribution are obtained, and second they are curve-fitted with the theoretical failure distribution equation with appropriate value of m and k . In extracting physically meaningful data from actual experimental data, we take into account of failure load variation due to thickness variation. For this purpose, we introduce the effective fracture initiation load P_i of specimen i given by

$$P_i = P_i^o \left(\frac{t}{t_i} \right)^2, \quad (8)$$

where t_i is the thickness of specimen i , t is the average thickness, and P_i^o is the experimental obtained fracture initiation load of specimen i . Eq. (8) is based on the assumption of linear elasticity where the maximum stress σ_{Max} is proportional to $P_i^o / \pi t_i^2$ [32]. By using P_i instead of P_i^o , we eliminate the thickness variation of individual specimens from the raw experimental data. In this work, the effective failure load P_i is sorted by order of magnitude and the cumulative failure probability $P_f(i)$ of crack initiation at the i -th fracture load P_i is assumed to be

$$P_f(i) = i / (1 + n) \quad (9)$$

where n is the number of specimens.

The parameters m and k are determined by curve-fitting the data from the biaxial flexure tests by the following equations [6,23,24]:

$$\ln [-\ln(1 - P_f)] = m \ln(P) + B \quad (10)$$

and

$$B = \ln \left[\frac{R^2 m k l_D}{(\pi t^2)^m} \right], \quad (11)$$

where P and P_f represent the effective load and cumulative failure probability respectively and R is the radius of the support ring. The definition of I_D and derivation of Eq. (11) are summarized in the Appendix. As shown in the appendix, I_D is independent of the indentation load for the biaxial test, since within the limits of linear elasticity the stress distribution is proportional to the indentation load (Eq. (VI)). The I_D value for the biaxial tests on ceramic plates or disks without resin cement is determined for a stress distribution induced by a unit indentation load obtained using a linear-elastic axisymmetric FEA model. In the computations, quadratic axisymmetric stress elements are used (100 elements along the radius direction and 20 elements along the thickness of the glass-ceramics); the spherical indenter and supporting ring are assumed to be rigid, and the contact surfaces are assumed to be friction free. The specimen dimension and relevant material properties used in the computation are summarized in Tables 1a and 2, respectively.

3.2 Effect of resin-cement layer shrinkage on crack bridging

The curing process of bonding resin is a complicated polymerization process with rate and temperature dependent deformation and material properties [33–35] and volumetric shrinkage. The strengthening effect of the resin layer on ceramics, resulting in crack bridging, may be increased due to shrinkage of the resin layer during curing on the stiff substrate. The shrinkage leads to residual tensile stresses in resin cement and compressive stresses in the substrate, which

results in additional strengthening. Since the residual stress is influenced by the rheological properties and curing kinetics of the resin cement [36], it is difficult to simulate accurately.

The curing of dental composites is usually accompanied by a volumetric shrinkage in the range from 1.5 to 5% [33]. Sakaguchi et al. [30] developed a strain gage method to measure the shrinkage and isolated the net post-gel shrinkage, which was within the range from 0.66% to 0.87% for commonly used dental composites.

To examine the residual stress distribution due to resin shrinkage, axisymmetric FEA computations are performed for the biaxial and the trilayer models without mechanical load. Since the volumetric shrinkage measured by Sakaguchi et al. [30] is performed in the post-gel phase, the elastic modulus of resin cement with a reasonable approximation may be assumed to be constant. The volume fraction of the filler for the resin cement used in this work is much less than that of the composites used by Sakaguchi et al. [30]. Because of this, the volumetric shrinkage rate is expected to be larger than those measured by Sakaguchi et al. [30]. We employ a 1% volumetric shrinkage as an approximation for the resin cement layer. To simulate such a level of shrinkage a thermal expansion coefficient of $0.0033\text{ }^{\circ}\text{C}^{-1}$ and decrease of $1\text{ }^{\circ}\text{C}$ in temperature are assigned for the resin cement in the FEA simulation so that 1% volumetric shrinkage will be produced.

There are two competing factors governing the volume shrinkage. One is the increase of residual stresses due to higher volume shrinkage. In general, a cement material is less filled, so it flows easily during the cementation process thus can exhibit 2–3 times as great a shrinkage as a restorative composite. Therefore, instead of only 1% of the cement volume shrinkage, 2~3% is possible. The competing factor is the residual stresses reduction caused by the viscoelastic behavior of the cement. This effect is changing during the curing process since the material viscosity/stiffness is a function of curing time (the cement shrinkage in the beginning of the curing process does not result in the residual stresses). These two competing factors should be theoretically examined in order to make an accurate residual stress analysis. The current study, based on the linearly elastic material assumption, is limited in this regard and provides only an approximate analysis.

3.3 Prediction of failure probability distributions for trilayer model

The failure probability distribution for biaxial tests of ceramic disk and plate samples coated with a thin resin cement layer (Fig. 3b) is predicted theoretically using biaxial test data on uncoated samples (Fig. 3a). For analyses, the fracture mechanics based failure probability model [24] (see Eq. (IV) in Appendix) is used. In order to examine the effects of crack bridging by the resin layer on the failure probability, four different cases were investigated. First, the resin layer effect was determined with direct use of the experimentally determined parameters m and k . Curing residual stress was not included and only the strengthening effect of the resin cement layer is accounted for but without its effect on reduction of the crack stress intensity factor. The second simulation includes the curing residual stress. Third, to account for the modification of the crack stress intensity factor in the 3D local model, the value of parameter k was replaced by k' (Eq. (7)) to include the resin layer bridging in addition to curing residual stress. Fourth, the parameter k was replaced by k' which was modified by a 2D local model and curing residual stress was included.

To evaluate the stress fields in biaxial tests of ceramic specimens coated with resin cement, an axisymmetric linearly elastic finite element (ABAQUS Version 6.5) analysis was employed. Quadratic axisymmetric stress elements (100 elements along the radius direction and 20 elements along the thickness of the glass-ceramics, 6 elements along the thickness of the resin cement) were used. The spherical indenter and supporting ring were assumed to be rigid and the contact interface between indenter and sample was assumed to be friction free. Tied

constraint is employed in the interface between the glass-ceramic and resin cement layers. The dimension and material properties are summarized in Table 1a and Table 2 respectively.

The same method is employed to predict the failure probability distribution for the indentation tests on the trilayer model (ceramic/glass bonded to simulated dentin, Fig. 1). In the axisymmetric finite element model to simulate the indentation tests on the bonded trilayer systems, 40 and 80 elements along the radial direction, respectively, are used for glass-ceramics and for glass. Along the thickness direction, 20 and 6 elements are respectively used in the top/substrate layers and the resin cement layer for both glass-ceramics and glass. The dimension and material properties are summarized in Table 1b and Table 2 respectively.

4. Results

4.1 Critical stress density distribution function for biaxial tests

Experimentally obtained failure distributions of ball-on-ring biaxial tests without resin cement are shown in Fig. 6 for both glass and glass-ceramics. The experimental data are least-square-fitted and the fitting results are shown in Figure 6. The parameters m and k of the critical stress density distribution function N , obtained based on the method of section 3.1.4, are shown in Figure 6.

The parameter k' of the effective critical stress density distribution evaluated from Eq. (7) and Table 3 is summarized in Table 4. The resulting critical stress density distribution functions are plotted in Fig. 7. The dotted line represents the critical stress density distribution curve-fitted from the biaxial test without a resin cement layer. The dashed line and solid line, respectively, represent the effective critical stress density distribution modified by the 3D and 2D local models. The differences, among these three critical stress density distribution functions, are attributed to the differences in the parameter k' as summarized in Table 4.

For all of these curves, the critical stress density distribution N rapidly increases with the critical stress, indicating that the results are dominated by small flaws. Since large critical stresses correspond to small cracks, the experimentally determined curve shows that the number of small cracks per unit area is much larger than that of large cracks. Modified distributions (dashed and solid curves) are lower than the experimentally determined distribution (dotted curve); high critical stress is required for failure initiated from the same flaw size. As shown in Fig. 7, the resin cement layer provides stronger bridging effect in the 2D case than in the 3D case.

4.2 FEA simulation of Residual Curing Stresses

As discussed in Section 3.3, the residual stress distribution at the bottom surface of the ceramic layer due to curing of the resin cement coating has been simulated for both the biaxial test model (Fig. 3b) and the trilayer onlay indentation model (Fig. 1). The normal residual stress components σ_{rr} and $\sigma_{\theta\theta}$ are found to be nearly constant for most of the region. The average values of the residual stress components for 1mm radius concentric area (the critical central area at the bottom of the ceramic layer below the indenter) are summarized in Table 5. The calculated level of residual compressive stress is very small compared to the 122 MPa Weibull characteristic fracture stress for glass-ceramics [6].

4.3 Prediction of failure probability distribution for biaxial tests of ceramic coated with resin cement layer - comparison with experimental data

Experimentally obtained failure probability distribution of glass-ceramic disks subjected to biaxial tests are plotted in Fig. 8. Stars and circles represent biaxial test data with and without resin cement, respectively. The biaxial test data of the glass-ceramic disks with and without

the resin cement coating were processed as discussed above. The 95% confidence interval [37] of the experimental data with resin cement is also shown as thin solid lines. The fitted curve for tests of uncoated samples is also shown in Fig. 8.

Four distinct theoretical predictions of failure probability distribution for the biaxial test with resin cement are plotted by dotted, dot-dashed, dashed and solid lines, respectively. Dot-dashed and dotted lines represent the theoretical failure probability distribution prediction based on the stress calculation from the axisymmetric FEA model with and without curing residual stresses respectively. For these two theoretical predictions, the curve-fitted parameters m and k are used. Solid and dashed lines represent the theoretical failure probability prediction with values of the parameter k' modified by the 3D and 2D local models respectively.

Inclusion of the residual stress due to the curing process significantly shifts the theoretical prediction towards higher loads. Inclusion of both 3D and 2D bridging effects also shifts the failure probability prediction towards higher loads. The dot-dashed curve (the prediction based on the stress reduction and the curing residual stress) falls within the 95% confidence interval for the biaxial test data, and the dashed curve (the prediction based on the stress reduction, the residual stress and the 3D local model) is mostly within this interval. However, the solid curve (the prediction based on stress reduction, residual stress and the 2D local model) and especially the dotted line (the prediction based on the stress reduction by the resin cement layer) mostly fall outside the 95% confidence interval.

4.4 Prediction of failure probability distribution for indentation tests on the bonded trilayer onlay model

4.4.1 Glass-ceramics/resin cement/simulated dentin trilayer model—The experimentally obtained failure probability distribution for the indentation tests on the trilayer model (glass-ceramic/resin cement/dentin) is shown by triangle points in Fig. 9. The 95% confidence intervals [37] are represented by thin solid lines. As in the previous theoretical failure probability prediction of the biaxial test for ceramic coated by a resin cement layer, four distinct theoretical predictions of the failure probability distribution for the indentation tests on the three layer model are included in Fig. 9.

The predictions are performed using the stress calculation for the indentation tests of this system using the axisymmetric FEA model. Dot-dashed and dotted lines are obtained with and without accounting for residual stresses respectively. For these two simulations, the experimentally determined values of parameters m and k are used. Inclusion of the residual stress in the theoretical prediction shifts the failure probability distribution towards higher loads. However, the shift is almost negligible compared to the significant shift seen in the biaxial tests. The prediction based on the stress reduction by the resin cement layer (dotted line) and the prediction based on the stress reduction and the residual stress (dot-dashed line) are inside the 95% confidence interval for the lower fracture initiation load region.

Solid and dashed lines represent theoretical predictions with parameter k' modified by the 2D and 3D local models respectively, thus accounting for change of crack stress intensity factors. Inclusion of both the 3D and 2D bridging effects significantly shifts the failure probability towards higher loads. When compared to the results in biaxial tests, this shift did not help to improve failure probability predictions.

The predicted cumulative failure probability curve based on the stress reduction, the residual stress and the 3D local model (dashed line) falls within the 95 % confidence interval of the experimental curve for the lower fracture initiation load region. The predicted curve based on the stress reduction, the residual stress and the 2D local model (solid line) falls within the 95% confidence interval only for a small region of lower loads.

4.4.2 Glass/resin cement/simulated dentin trilayer model—The glass/resin cement/simulated dentin system experimental data and the corresponding theoretical predictions are shown in Fig. 10. Following the same procedure as above computer simulations of the indentation tests of the system have been performed. The prediction based on the stress reduction (dotted line) and the prediction based on the stress reduction and the residual stress (dot-dashed line) fall mostly outside of the 95% confidence interval of the experimental data. The predicted probability curve based on the stress reduction, the residual stress and the 3D local model (dashed line) mostly falls within the 95 % confidence interval. The prediction based on the stress reduction, the residual stress and the 2D local model (solid line) falls within or very close to the 95% confidence interval for the lower load region. The analysis corresponding to the dotted line (far left; prediction based on the stress reduction) which is outside the 95% confidence interval, was also previously performed by Wang et al. [23].

5. Discussion

5.1 Determination of parameters m and k

For the glass/resin cement/simulated dentin system, the failure probability distribution has already been examined by Wang et al. [23]. In this work, we consider the bridging effect and the residual shrinkage stress of the resin cement layer to improve the predictive capability of the analysis as shown Fig. 10.

Parameters m and k for the critical stress density distribution shown in Figure 6 are different than those by Wang et al. [23] although the same experimental data were used for the analysis. The difference is partly attributed to the modification of the experimental fracture initiation load by considering the variation of thickness of each specimen based on Eq. (8). In addition, the stress distribution is more accurately calculated by the FEA model, rather than using the approximate analytical equations [32] employed by Wang et al. [23]. Therefore, in this work, the same experimental data was used to determine more accurately the model parameters.

5.2 Curing residual stress versus crack bridging as strengthening mechanisms

In this work, we quantitatively examined the strengthening mechanism attributed to the influence of the resin cement layer by both crack bridging and curing residual stress. In the biaxial tests (Fig. 8), both the bridging effect and the residual stress appear to be important factors. For the trilayer ‘simulated onlay’ model, however, curing residual stress appears to play an insignificant role as shown in Figs. 9 and 10. This difference can be attributed to the difference in the calculated residual stress level as shown in Table 5. In the trilayer model, the average residual stress at the bottom glass-ceramics surface is 1.9 MPa, which is much smaller than the 12.4 MPa for the biaxial model.

In order to consider this difference, the deformation due to resin cement shrinkage for the biaxial test and the trilayer onlay models is shown in Fig. 11. With enlargement of displacement by a factor of 20, a bending deformation can be observed in the biaxial test specimen, while there is no such deformation in the ceramic bonded to a thick substrate. The substrate in the trilayer model restricts the contraction of the resin cement, while in the biaxial test model there is no similar constraint on the ceramic. As a result, larger compressive stresses are produced due to the bending deformation at the bottom surface of the ceramics in the biaxial test model.

5.3 2D and 3D local crack models

The surface flaws vary in their shapes and sizes, and they are different from specimen to specimen. In order to simulate the surface flaws, we developed 2D and 3D local crack models assuming the crack shape as shown in Figs. 4 and 5. If the surface flaw is much longer than its depth, it can be locally approximated by a 2D surface or interface crack. If the flaw length and

depth are comparable, it can be approximated by a 3D local crack model. The shape of an actual flaw can be between these two limiting cases described approximately by our 2D and 3D local crack models. Our results show that combination of 2D and 3D local models results in reasonable agreement with experimental data.

5.4 Discrepancies between the test data and the models

Despite generally good agreement between the test data and the model predictions as shown in Figures 8–10, there are some discrepancies. For both trilayer onlay systems as shown in Figures 9 and 10, the slopes for experimental data with 95% confidence interval curves are steeper than those for theoretical predictions while in biaxial tests, the difference in the slope between experimental data curves and theoretical predictions are small. In addition, in the glass ceramics/resin cement/simulated dentin system, better agreement between experimental data and theoretical predictions are obtained for the lower load portion of the curve, while in glass/resin cement/simulated dentin system, better agreement are obtained for the higher load range portion of the curve. Possible causes of these discrepancies may be attributed to simplified model assumptions such as uniform 1% volume shrinkage without modeling dynamic curing process and idealized surface flaw geometries. In addition to these simplified assumptions, some physical phenomena which are not included in our current modeling effort may play an important role. For example, during the curing process, the resin cement may partially fill up the surface flaws in the direction of their depth and enhance the bridging effect. In addition to purely mechanical factors, differences in the nature of chemical bonding at the interface may limit predictive capabilities of the current model.

6. Conclusion

In this work, the influence of the resin cement layer on the strengthening mechanism and fracture behavior of dental ceramics has been quantitatively examined by employing the fracture mechanics based failure probability model proposed by Wang et al. [24]. To account for the bridging effect of the resin cement layer we have introduced the notion of a critical stress density distribution function based on 2D and 3D local crack models. The residual stress induced in the system due to curing of the resin cement layer is simulated by FEA assuming 1% volumetric shrinkage.

Good agreement has been obtained between the prediction of the failure probability distribution and experimental data for both the biaxial tests with a resin cement layer and the indentation tests on the bonded trilayer onlay model.

The model developed has not only improved the predictive capability but also provided physical insight as to the relative importance of different strengthening mechanisms. In the biaxial test, both the curing residual stress and the crack bridging effects play important roles in strengthening mechanisms. However, in the bonded trilayer onlay model, the bridging effect plays a crucial role while the contribution of the curing residual stress is negligible. This difference in the strengthening mechanisms is attributed to the constraint from the bottom substrate which results in relatively small magnitudes of curing residual stress in the top onlay layer.

The strengthening mechanism for the bonded trilayer onlay model has important implications in the clinical application: the strengthening mechanism for the complex clinical restoration structures may be attributed to the bridging effect. Although our analysis is limited to simplified structure, it is expected that the same mechanism exists for more complicated clinical dental restoration structures. Therefore, in order to improve clinical performance, it is more important to increase the resin cement modulus and assure good bonding.

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Appendix

a) Statistical theory for failures of brittle materials

In order to analyze strength variation of brittle materials, Batdorf et al. [38] introduced the notion of solid angle Ω containing the normals to all directions for which the normal stress is larger than the critical stress σ_{Cr} . The solid angle Ω varies from zero to 2π for surface cracks based on the definition. For $\Omega/2\pi < 1$, propagation of a crack depends on its orientation and there exists a range of orientation angle where a crack does not propagate. For this case, the failure probability of a single surface crack can be expressed as [38]

$$P_f = \Omega(\sigma, \sigma_{Cr})/2\pi, \quad (I)$$

where σ is the applied stress. For $\Omega/2\pi = 1$, propagation of a crack is independent of its orientation and it is solely determined by the size of the crack [24].

Combining the two cases ($\Omega(\sigma, \sigma_{Cr}) < 2\pi$ and $\Omega(\sigma, \sigma_{Cr}) = 2\pi$), Wang et al. [24] derived the general expression of failure probability for surface cracks as follows.

$$\begin{aligned} P_f &= 1 - (1 - P_f^I)(1 - P_f^{II}) \\ P_f^I &= 1 - \exp \left\{ \int_A \int_{\sigma_{Cr}^{Min}}^{\sigma_{Cr}^{Max}} \ln[1 - \Omega(\sigma, \sigma_{Cr})/2\pi] \frac{dN(\sigma_{Cr})}{d\sigma_{Cr}} d\sigma_{Cr} dA \right\}, \quad \Omega < 2\pi \\ P_f^{II} &= 1 - \exp \left\{ - \int_A \int_0^{\sigma_{Cr}^M} \frac{dN(\sigma_{Cr})}{d\sigma_{Cr}} d\sigma_{Cr} dA \right\}, \quad \Omega = 2\pi \end{aligned} \quad (II)$$

Where σ_{Cr}^{Max} and σ_{Cr}^{Min} , respectively, represent the values of the maximum and minimum critical stress and $N(\sigma_{Cr})$ represents the critical stress density distribution function.

The critical stress density distribution function can be assumed to have the form proposed by Chao et al. [29].

$$N(\sigma_{Cr}) = k\sigma_{Cr}^m \quad (III)$$

Substituting Eq. (III) into Eq. (II), the failure probability for surface cracks can be expressed as

$$P_f = 1 - \exp \left\{ - \int_0^R 2\pi m k r \left[\int_0^{\sigma_2} \sigma_{Cr}^{m-1} d\sigma_{Cr} - \int_{\sigma_2}^{\sigma_1} \ln \left(1 - \frac{\Omega}{2\pi} \right) \sigma_{Cr}^{m-1} d\sigma_{Cr} \right] dr \right\}, \quad (IV)$$

where R is the radius of the effective area (support ring radius for ball-on-ring test, and sample radius for indentation test) and

$$\Omega = 2\cos^{-1} \left[\frac{2\sigma_{Cr} - \sigma_1 - \sigma_2}{\sigma_1 - \sigma_2} \right]. \quad (V)$$

b) Calculation of parameters m and k from biaxial experimental data

The statistical parameters m and k for the critical stress density distribution function $N(\sigma_{Cr}) = k\sigma_{Cr}^m$ (Eq.(III)) can be calculated by curve-fitting the biaxial experimental data. In biaxial flexural tests, the stress distribution at the bottom of the disk is proportional to $P / \pi t^2$ within the linearly elastic theory, where P and t, respectively, represent an applied load and thickness. If we introduce a non-dimensional factor I_D as

$$\begin{aligned} I_D &= \int_{r/R=0}^1 2\pi \left(\frac{r}{R} \right) \left[\int_{\frac{\sigma_{Cr}}{P/\pi t^2}=0}^{\frac{\sigma_2}{P/\pi t^2}} \left(\frac{\sigma_{Cr}}{P/\pi t^2} \right)^{m-1} d \left(\frac{\sigma_{Cr}}{P/\pi t^2} \right) \right. \\ &\quad \left. - \int_{\frac{\sigma_{Cr}}{P/\pi t^2}=\frac{\sigma_2}{P/\pi t^2}}^{\frac{\sigma_1}{P/\pi t^2}} \ln \left(1 - \frac{\Omega}{2\pi} \right) \left(\frac{\sigma_{Cr}}{P/\pi t^2} \right)^{m-1} d \left(\frac{\sigma_{Cr}}{P/\pi t^2} \right) \right] d \left(\frac{r}{R} \right), \end{aligned} \quad (VI)$$

and B defined by

$$B = \ln \left[\frac{R^2 m k I_D}{(\pi t^2)^m} \right], \quad (VII)$$

then I_D and B are independent of the load P for biaxial test. They can be calculated if the dimensions of the samples are known.

Correspondingly, the failure probability in Eq. (IV) can be expressed as

$$P_f = 1 - \exp\left(-e^B(P)^m\right), \quad (\text{VIII})$$

or

$$\ln\left[-\ln(1 - P_f)\right] = m\ln(P) + B. \quad (\text{IX})$$

If we plot the biaxial experimental data using $\ln[-\ln(1 - P_f)]$ and $\ln(P)$, m and B can be determined by curve-fitting the data point to a straight line for Eq. (IX), and k can be calculated by Eq. (VII).

Fig. 1. Indentation tests on the bonded trilayer onlay model. Average dimension is listed in Table 1b.

Fig. 2. Fractured resin retained ceramic onlay after 3.5 years of service on a maxillary 1st premolar on a patient with a severe bruxing problem. Arrows point to the fractured ceramic surface. The buccal half of the onlay remains bonded to the tooth structure while the lingual portion is missing and the underlying dentin is exposed.

Fig. 3. Ball-on-ring biaxial experimental tests. Average dimension of samples used for biaxial tests is listed in Table 1a.

(a) single edge crack with length a in a semi-infinite body (E, ν)

(b) single edge crack with length a in a semi-infinite body (E, ν) reinforced by a dissimilar semi-infinite body (E_g, ν_g)

Fig. 4.
2D Plane Strain Local Model.

Fig. 5.
3D Local Model.

Fig. 6.

Ball-on-ring biaxial experimental data without resin cement (see Fig. 3a) for two materials: glass and glass-ceramics. The straight lines ($Y=5.21X-23.81$ for glass and $Y=5.92X-32.55$ for glass-ceramics), are obtained by fitting the experimental data based on the least-square method. They are used to determine the parameters k and m of the critical stress density distribution function $N(\sigma_{cr}) = k\sigma_{cr}^m$. (glass: $m=5.21$, $k=1.75E-12 \times mm^{-2}Mpa^{-5.21}$; glass-ceramic: $m=5.92$, $k=2.25E-14 \times mm^{-2}Mpa^{-5.92}$)

Fig. 7.

Critical stress density distribution N as a function of σ_{Cr} , $N(\sigma_{Cr}) = k\sigma_{Cr}^m$, at the bottom surface of glass-ceramic disks.

Fig. 8.

Experimental data and predicted failure probability distributions of glass-ceramic disks with and without resin cement layer under biaxial tests (Fig. 3). The critical stress density distribution (obtained from $Y=5.92X-32.55$ in Fig. 6) was used to theoretically predict the failure probability of disks with resin cement.

Fig. 9.

Experimental data and predicted failure probability distributions of indentation tests on bonded trilayer onlay model (Fig. 1), (glass-ceramics/resin cement/simulated dentin trilayer system).

Fig. 10.

Experimental data and predicted failure probability distributions of indentation tests on bonded trilayer onlay model (Fig. 1), (glass/resin cement/simulated dentin trilayer system).

Fig. 11.

Deformation caused by shrinkage of the resin cement layer in biaxial tests with resin cement and the trilayer onlay model. The deformations are calculated by AbaqusTM, and 1% volumetric shrinkage of resin cement layer is included. No external force is included.

Table 1a

Average dimension of laboratory samples used for biaxial tests (Fig. 3).

sample material	glass-ceramics	glass
diameter: D (mm)	14.8 ⁽¹⁾	15
thickness of disk: h (mm)	1.6	1.1
thickness of resin cement layer: h _{adhesive} (mm)	0.1	NA
radius of spherical indentation ball: R (mm)	12.5	5
diameter of support ring (mm)	12.5	12.5

⁽¹⁾ The diameter is the equivalent diameter calculated according to ASTM standard C1499-05 [39].

Table 1b

Average dimension of laboratory trilayer onlay models used for indentation tests (Fig. 1).

top layer material	glass-ceramics	Glass
substrate layer material	simulated dentin	simulated dentin
diameter: D (mm)	12.5	13
thickness of top disk: h_{top} (mm)	1.6	1.1
thickness of resin cement layer: h_{adhesive} (mm)	0.1	0.1
thickness of substrate: $h_{\text{substrate}}$ (mm)	5	4
radius of spherical indentation ball: R (mm)	6.25	20

Table 2

Experimentally measured materials properties.

	Young's modulus E (GPa)	Poisson's ratio ν
glass	62.5	0.19
glass-ceramics ⁽¹⁾	62	0.25
adhesive resin cement	10.21	0.33
simulated dentin	12.6	0.35

⁽¹⁾ material property obtained from reference [6,40].

Table 3

Numerically evaluated values of f' for 2D and 3D perfectly bonded local models (Figs. 4b and 5b). Recalling that

$f = 1.025 \times \frac{2}{\pi}$ for the 3D local model (Fig. 5a) and $f = 1.12$ for the 2D local model (Fig. 4a), note that $\frac{f'}{f} < 1$ for both glass/resin cement and glass-ceramic/resin cement systems with 3D and 2D assumptions. This increases the critical failure stress as shown in Eq. (5).

	f' for glass/resin cement system	f' for glass-ceramics/resin cement system
3D local model	$0.971 \times 2/\pi$	$0.978 \times 2/\pi$
2D local model	0.951	0.958

Table 4

Numerical values of parameter k' of effective critical stress density distribution $N(\sigma'_{cr}) = k' \sigma'_{cr}{}^m$ calculated from $k' = k \left(\frac{f'}{f} \right)^m$ and Table 3 and experimentally determined parameters m and k (glass: $m=5.21$, $k=1.75\text{E-}12 \times \text{mm}^{-2} \text{MPa}^{-5.21}$; glass-ceramics: $m=5.92$, $k=2.25\text{E-}14 \times \text{mm}^{-2} \text{MPa}^{-5.92}$)

materials	local models employed for modification	k'
glass	3D	$1.32\text{E-}12 \times \text{mm}^{-2} \text{MPa}^{-5.21}$
	2D	$7.46\text{E-}13 \times \text{mm}^{-2} \text{MPa}^{-5.21}$
glass-ceramics	3D	$1.70\text{E-}14 \times \text{mm}^{-2} \text{MPa}^{-5.92}$
	2D	$0.89\text{E-}14 \times \text{mm}^{-2} \text{MPa}^{-5.92}$

Table 5

Average values of residual stress components σ_{rr} and $\sigma_{\theta\theta}$ at the bottom critical surface of glass-ceramics/glass (1mm radius concentric critical area) due to curing process (1% volume shrinkage of resin cement). The results are calculated by axisymmetric FEA (Biaxial and Trilayer) models. σ_{rr} and $\sigma_{\theta\theta}$, respectively, represent the normal stresses along the radial and tangential directions.

materials and structure	σ_{rr} (MPa)	$\sigma_{\theta\theta}$ (MPa)
glass-ceramics/resin cement	-12.4	-12.4
glass-ceramics/resin cement/simulated dentin	-1.9	-1.9
glass/resin cement/simulated dentin	-2.5	-2.5