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Morphology Based Cohesive Zone Modeling of the Cement-Bone Interface from Postmortem Retrievals

Daan Waanders¹, Dennis Janssen¹, Kenneth A. Mann², and Nico Verdonschot^{1,3}

¹ Orthopaedic Research Laboratory, Radboud University Nijmegen Medical Centre, Nijmegen, The Netherlands ² Department of Orthopaedic Surgery, SUNY Upstate Medical University, Syracuse, NY, USA ³ Laboratory for Biomechanical Engineering, University of Twente, Enschede, The Netherlands

Abstract

In cemented total hip arthroplasty, the cement-bone interface can be considerably degenerated after less than one year in-vivo service; this makes the interface much weaker relative to the direct post-operative situation. It is, however, still unknown how these degenerated interfaces behave under mixed-mode loading and how this is related to the morphology of the interface. In this study, we used a finite element approach to analyze the mixed-mode response of the cement-bone interface taken from postmortem retrievals and we investigated whether it was feasible to generate a fully elastic and a failure cohesive model based on only morphological input parameters.

Computed tomography-based finite element analysis models of the postmortem cement-bone interface were generated and the interface morphology was determined. The models were loaded until failure in multiple directions by allowing cracking of the bone and cement components and including periodic boundary conditions. The resulting stiffness was related to the interface morphology. A closed form mixed-mode cohesive model that included failure was determined and related to the interface morphology.

The responses of the finite element simulations compare satisfactorily with experimental observations, albeit the magnitude of the strength and stiffness are somewhat overestimated. Surprisingly, the finite element simulations predict no failure under shear loading and a considerable normal compression is generated which prevents dilation of the interface. The obtained mixed-mode stiffness response could subsequently be related to the interface morphology and subsequently be formulated into an elastic cohesive zone model. Finally, the acquired data could be used as an input for a cohesive model that also includes interface failure.

Keywords

finite element; bone; bone cement; interface; cohesive modeling

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Correspondence: Daan Waanders, Orthopaedic Research Lab, Radboud University Nijmegen Medical Centre, P.O. Box 9101, 6500 HB Nijmegen, The Netherlands, T: +31 24 3617461, F: +31 24 3540555, d.waanders@orthop.umcn.nl.

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1. INTRODUCTION

In cemented total hip arthroplasty, the fixation at the cement-bone interface is one of the critical factors in the longevity of the cemented hip reconstruction. Since bone cement is not osteoconductive, physicochemical bonding cannot be expected (Oonishi et al., 2008), and therefore, interface fixation relies on cement interdigitation into the bone (Lucksanasombool et al., 2003). However, recent research has indicated that after less than one year *in-vivo* service the cement-bone interface can be considerably degenerated due to bone resorption (Venesmaa et al., 2003; Mann et al., 2010). This subsequently makes the interface considerably weaker relative to the immediate post-operative situation (Miller et al., 2010).

The implementation of the mechanical behavior of the cement-bone interface into Finite Element Analysis (FEA) of cemented hip reconstructions has often been oversimplified. Over the years, the cement-bone interface has frequently been implemented either as an infinitely stiff interface (Stolk et al., 2007; Hung et al., 2004; Katoozian and Davy, 2000), a uniform layer of soft tissue elements (Colombi, 2002; Verdonschot and Huiskes, 1997) or as a frictional contact layer (Lewis and Duggineni, 2006). The main limitation of these methods is that the interface behavior may not capture the true physics of the cement-bone interface.

Recently, the mechanical mixed-mode behavior of the cement-bone interface has been modeled utilizing sophisticated cohesive zone models (Moreo et al., 2006; Perez et al., 2009). The mechanical characteristics of these cohesive models were based on experimental data acquired from lab-prepared cement-bone interfaces that represent the immediate postoperative situation. It has recently also been reported that the increased compliance of these lab-prepared interfaces, relative to an assumed infinitely stiff interface also increases the overall cement mantle damage (Waanders et al., 2011b). However, it is still unknown how increased compliance and reduced strength of the cement-bone interface following *in vivo* service influences the mixed-mode loading response and subsequent cement mantle damage.

In order to understand the behavior of the cement-bone interface derived from postmortem retrievals, from which cohesive models could be generated, information on the mixed-mode failure response of the interface is necessary. A possible method to obtain this information could be to load multiple postmortem specimens to failure under different directions, like has previously been done with lab-prepared specimens (Wang et al., 2010; Mann et al., 2001). However, postmortem retrievals can be highly variable in terms of the amount of interdigitation due to biological changes at the interface, so that a consistent failure response as a function of different loading directions would be difficult to develop using an experimental approach. We have previously shown that micromechanical finite element models can be used to predict the failure response of lab-prepared specimens (Waanders et al., 2011a). Here we propose to extend this concept to postmortem retrievals using an *in silico* approach; specimens will be loaded to failure in multiple loading directions and the mechanical response quantified.

Around the cement mantle, the degenerated cement-bone interface does not exhibit a homogenous morphology (Bishop et al., 2009). To account for variable morphology in cohesive zone modeling of the cement-bone interface, interface properties have previously been based on the quantity of cement interdigitation into the bone (Perez and Palacios, 2010). However, the quantity of cement interdigitation into the bone has been based on lab-prepared cement-bone interfaces (Mann et al., 2001) and it is therefore unknown whether it can also be applied to postmortem retrievals. Moreover, the quantity of cement

interdigitation into the bone does not provide insight into the micromechanics that cause the mechanical properties of the interface.

The goal of this study is to investigate the mechanical mixed-mode response of the cement-bone interface from radiographically well-fixed postmortem retrievals and relate this response to their morphology. A subsequent goal is to generate an elastic and failure cohesive model with only morphological factors as an input. From quantitative computed tomography (CT) data of postmortem retrievals, micromechanical FEA models were generated using a multi-scale approach. These models were subsequently loaded in multiple directions while failure could occur by allowing both the bone and cement components to crack. Using this approach, we addressed three research questions: (1) what are the mixed-mode characteristics of the postmortem cement-bone interface and how do these simulations compare to experimental findings?; (2) can the initial elastic stiffness in multiple directions be related to the interfacial morphology?; and (3) Can a (I) fully linear elastic or (II) non-linear elastic cohesive failure model reproduce the mechanical response of the *in silico* experiments using the interface morphology as the input parameters?

2. METHODS

2.1 Specimen preparation

Four cement-bone interface specimens were retrieved from the proximal femurs of two donors with cemented hip components at autopsy (Table 1) through the anatomical donor programs at SUNY Upstate Medical University and the University of Alabama at Birmingham. Donations to the anatomical donor programs were made between 1 and 2 days of death and frozen at -20°C prior to tissue harvest. The specimen size, age, gender, cause of death and number of years in service were documented. Specimen source locations were documented including distance from the calcar and anterior/posterior-medial/lateral quadrant. By observation of the cement mantle porosity it was assessed whether the utilized bone cement was vacuum mixed prior to insertion. All specimens were micro-CT scanned at 12-micron isotropic resolution (Scanco 40, SCANCO Medical AG, Brüttisellen, Switzerland). Based on planar x-rays of the cemented femur construct, the quality of the cement-bone interface fixation was assessed and specified as radiographically '*definitely loose*', '*possibly loose*', or '*not loose*'.

2.2 FEA modeling

From each specimen a FEA model was generated. Each model consisted of two parts: bone and cement. The FEA meshes were created from the micro-CT data of the cement-bone interface using MIMICS 11.0 (Materialize, Leuven, Belgium). After segmentation of the micro-CT data, the 3D voxel meshes of the bone and cement were transformed to triangular surface meshes, using a $6\times 6\times 6$ voxel interpolation with smoothing. Next, these surface meshes were remeshed to reduce the number of triangles and to remove low quality triangles. To avoid intersecting elements, the mesh of the cement was subtracted from the bone mesh. The resulting low quality elements of the bone were subsequently remeshed. Finally, an erosion ($2.0\text{ }\mu\text{m}$) was applied to the bone interface to ensure that the two meshes were not intersecting (Waanders et al., 2009). Next, both surface meshes were meshed as a tetrahedral 3D solid mesh (Patran 2005r2, MSC Software Corporation, Santa Ana, CA, USA). The solid mesh of the bone was mapped back into the micro-CT data set, after which the weighted average of the grayscale was calculated for each solid element using MIMICS.

In order to avoid off axis tractions during loading in normal directions and to apply periodic boundary conditions (see section 2.3), the initial meshes were mirrored (Waanders et al., 2011a; Pahr and Zysset, 2008; Mullins et al., 2007). First, the initial mesh was mirrored in

the y-z plane (Figure 1a). Subsequently, this mirrored mesh was mirrored in the x-z plane. The resulting models contained on average 384,000 elements and 88,000 nodes (Table 1; Figure 1b). Contact between the bone and cement was modeled using a double-sided node to surface contact algorithm (MSC.MARC 2007r1, MSC Software Corporation, Santa Ana, CA, USA) with a friction coefficient of 0.3 (Janssen et al., 2008b).

2.3 Material properties

Both bone and cement were initially modeled as isotropic linear elastic materials. Since the exact material properties of the cement were unknown, the Young's modulus (E) and Poisson's ratio (ν) of the cement were taken as 3000 MPa and 0.3, respectively (Janssen et al., 2008b; Harper and Bonfield, 2000; Lewis, 1997). The bone elastic properties were based upon micro-CT grayscale values, which were converted to equivalent HA-densities using a calibration phantom. The Young's modulus was assumed to be linearly dependent on the HA-density (Janssen et al., 2009), resulting in bone modulus values ranging from 0.1 to 20,000 MPa.

Previous experiments showed that when the cement-bone interface was loaded until failure, cracks initiated in both the cement and bone (Mann et al., 2008). Therefore, crack formation in the bulk bone and cement was simulated using an adapted custom-written FEA algorithm to simulate static failure (Stolk et al., 2004). Static failure in either bone or cement occurred when the local principal tensile stress exceeded the material strength. The strength of the cement was taken as 40 MPa (Harper and Bonfield, 2000; Lewis, 1997), while the strength of the bone was derived from the Young's modulus (Keyak et al., 2005; Waanders et al., 2010):

$$S = 102 \cdot \left(\frac{E}{14,900} \right)^{\frac{1.80}{1.86}} \quad (1)$$

Cracks were simulated by setting the Young's modulus to 0.1 MPa in the direction perpendicular to the corresponding principal stress direction.

2.4 Boundary conditions

In order to establish a multi-scale representation of the cement-bone interface, periodic boundary conditions were applied. In this way, one single model of the cement-bone interface was considered as an infinite series of periodic micro structures (Kadir et al., 2010). The periodic boundary conditions were implemented by constructing nodal links between nodes periodically located on the left ($y=0$) and right side ($y=w$) of the model (Figure 2) (Salomonsson and Andersson, 2008) and were defined as:

$$u_i(x, 0, z) = u_i(x, w, z) \quad (2)$$

In this equation, ' u_i ' represents the displacement in x, y and z-direction. As a result of the mirroring of the meshes, no bone-to-cement links at the boundary between two different periodic cells occurred.

While all nodes in the bottom plane ($z=0$) were fixed in all directions (Figure 2), the nodes in the top plane ($z=h$) were uniformly displaced until failure. Seven different angles (α) were considered: 0° (pure tension), 30°, 60°, 90° (pure shear), 120°, 150° and 180° (pure compression) (Figure 2). The incremental displacement in normal and tangential direction, Δ_N and Δ_T , could hence be calculated as $\Delta_N = \Delta \cos(\alpha)$ and $\Delta_T = \Delta \sin(\alpha)$, respectively. The

resultant nodal reaction force [N] was calculated and subsequently decomposed and

converted to tractions $\left[\frac{\text{MPa}}{\text{mm}} \right]$ in normal and tangential direction, T_N and T_T respectively.

2.5 Morphology

In order to capture the morphology of the cement-bone interface, a CT-based stereology approach was used (Miller et al., 2010). A grid (0.3×0.3 mm spacing) was placed over the micro-CT data and lines were projected vertically through the specimen (Figure 3). At the points where the projection lines crossed the interface, the status was designated as either apposition or proximity (gap < 0.25 mm). From this, the *Contact Index* (CI) was calculated as the number of points of apposition divided by the total number of projection lines. The *Intersection Index* (II) was calculated as the total number of points in apposition and proximity divided by the total number of projection lines. Finally, the gap between the cement and bone was measured for each projection line. Subsequently, the average of all the local gaps was determined what resulted in the *Gap Thickness* (GT).

2.6 Cohesive modeling

The interface stiffness as a result of multi-axial loading was, like in physical experiments, determined in the direction of the interface displacement. In physical experiments, where off axis loads usually do not occur as a result of the experimental setup such as use of linear sliders or a universal joint, the load vector ($\mathbf{T}$) always points in the same direction as the displacement vector (Δ) (Miller et al., 2011; Mann et al., 2008). However, as a result of the boundary conditions as applied in the current study, the load vector ($\mathbf{T}$) and the displacement vector (Δ) do not necessarily point in the same direction (Figure 4) (Waanders et al., 2011a). Therefore, the orthogonal projection of $\mathbf{T}$ onto Δ was determined ($\mathbf{T}'$) to obtain the stiffness in the direction of the displacement (Figure 4). The vector $\mathbf{z}$ was defined as the component of $\mathbf{T}$ perpendicular to Δ . From this, the stiffness in the direction of the applied displacement

$\left(\frac{\partial \mathbf{T}'}{\partial \Delta} \right)$ and its perpendicular stiffness $\left(\frac{\partial \mathbf{z}}{\partial \Delta} \right)$ could be determined.

2.6.1 Elastic cohesive model—In order to generate an elastic model for postmortem cement-bone interfaces, a step-wise regression model was used to determine the relationship

between $\frac{\partial \mathbf{T}'}{\partial \Delta}$ and the three morphological factors (CI, II and GT) as well as the loading angle (α). In order to incorporate the number of variables in the correlation, the adjusted

correlation coefficient, $\bar{r}$, was used. It was assumed that $\frac{\partial \mathbf{z}}{\partial \Delta}$ could be determined as:

$$\frac{\partial \mathbf{z}}{\partial \Delta} = A \cdot \sin(\alpha) \cdot \frac{\partial \mathbf{T}'}{\partial \Delta} \quad (3)$$

in which 'A' is a constant to be determined and $\frac{\partial \mathbf{T}'}{\partial \Delta}$ as predicted by the morphological ∂ factors and the loading angle. Linear regression analysis was used to assess the correlation

between the predicted $\frac{\partial \mathbf{T}'}{\partial \Delta}$ and $\frac{\partial \mathbf{z}}{\partial \Delta}$.

2.6.2 Failure cohesive model—A mixed-mode model which also included interfacial failure was determined utilizing the following cohesive model (Wei and Hutchinson, 2008):

$$T_N(\Delta_N, \Delta_T) = \frac{\Gamma_0}{\delta_N} \left[\frac{\Delta_N}{\delta_N} - \left((1+\beta) \frac{\Delta_N}{\delta_N} - \beta \right) f(\Delta_T) \right] \exp \left(-\frac{\Delta_N}{\delta_N} \right) \quad (4a)$$

$$T_T(\Delta_N, \Delta_T) = \Gamma_0 \frac{df(\Delta_T)}{d\Delta_T} \left[1 + (1+\beta) \frac{\Delta_N}{\delta_N} \right] \exp \left(-\frac{\Delta_N}{\delta_N} \right) \quad (4b)$$

In this set of equations, the normal and tangential tractions (T_N and T_T) were defined as a function of the normal and tangential displacements (Δ_N and Δ_T) and four parameters which can partly be linked to the morphology. The parameter Γ_0 denotes the total fracture energy in pure tension ($\Delta_T=0$), which was therefore calculated from equation 4a as:

$$\Gamma_0 = T_{N,ult} \cdot \delta_N \cdot \exp(1) \quad (5)$$

where $T_{N,ult}$ is the tensile strength of the cement-bone interface. Since for postmortem retrievals there is a positive relationship ($r^2=0.57$) between the tensile strength and stiffness, $T_{N,ult}$ was determined as (Miller et al., 2010; Waanders et al., 2010):

$$T_{N,ult} = 0.0069 \frac{\partial T_N}{\partial \Delta_N} + 0.093 \quad (6)$$

where the normal stiffness $\left(\frac{\partial T_N}{\partial \Delta_N} \right)$ was determined from the morphological based stiffness

$\frac{\partial T'}{\partial \Delta}$ from section 2.5.1. The variable δ_N was defined as the displacement at the tensile

strength and was determined as a function of $T_{N,ult}$ and $\frac{\partial T_N}{\partial \Delta_N}$ utilizing the data of Mann et al. (2008) and was therefore also morphology dependent. The function $f(\Delta_T)$ was used to define the response in pure shear:

$$T_T(0, \Delta_T) = \Gamma_0 \frac{df(\Delta_T)}{d\Delta_T} \quad (7)$$

Since a previous mixed-mode study suggested a linear relationship between T_T and Δ_T (Waanders et al., 2011a), this function resulted in:

$$f(\Delta_T) = \frac{1}{2\Gamma_0} \frac{\partial T_T}{\partial \Delta_T} \Delta_T^2 \quad (8)$$

The parameter $\frac{\partial T_T}{\partial \Delta_T}$ was defined as the tangential stiffness in pure shear, which was also

determined from the morphological based stiffness $\frac{\partial T'}{\partial \Delta}$. Finally, the fitting coefficient β was set to -0.8 (Waanders et al., 2011b). This value had to be negative in order to reproduce normal compression when loaded in pure shear.

The generated elastic and failure cohesive models were subsequently compared with the responses as found by the mixed-mode simulations. From this it was assessed whether the generated cohesive fit was satisfactory.

3. RESULTS

3.1 Mixed-mode response

A similar mixed-mode response was found for all four models. In pure tension ($\alpha=0^\circ$), the response showed a traction-displacement response with an initial stiffness followed by yielding and softening (Figure 5a). The ultimate tensile strengths ($T_{N,ult}$) ranged from 0.10

to 0.81 MPa and the normal stiffness $\left(\frac{\partial T_N}{\partial \Delta_N}\right)$ from 5.4 to 93.0 MPa/mm (Table 2). In pure compression ($\alpha=180^\circ$), a linear $T_N-\Delta_N$ relationship was found with stiffness ranging from 21.4 to 441.2 MPa/mm (Table 2). In pure tension and compression, the tangential tractions (T_T) were found to be negligible (Figure 5d).

In pure shear ($\alpha=90^\circ$), the tangential traction (T_T) increased linearly with the applied tangential displacement (Δ_T) (Figure 5c) and none of the models reached failure. The four

models had a tangential stiffness $\left(\frac{\partial T_T}{\partial \Delta_T}\right)$ ranging from 10.1 to 184.3 MPa/mm (Table 2). Remarkably, a considerable compressive traction was needed to prevent dilation of the interface (Figure 5b).

For all the intermediate values of α , all the responses showed a smooth transition between the aforementioned three ‘principal’ responses. It is interesting to note that although interfaces are loaded in mixed-mode tension (30° and 60° cases), normal compression occurred in the softening phase (Figure 5a–b).

3.2 Cohesive model

When the logarithmic value of the interfacial stiffness in the direction of the applied

displacement $\left(\log\left(\frac{\partial T'}{\partial \Delta}\right)\right)$ was used in the regression model, the morphological variables the *Contact Index*, *Gap Thickness* and $\cos(\alpha)$ showed a significant contribution to the predicted stiffness ($r^2=0.91$, $p<0.0001$; Table 3; Regression model 1). The *Intersection Index* did not significantly contribute to the regression ($p=0.72$). With this regression model, a negative estimate for the *Contact Index* coefficient was found. A physical interpretation would be that increasing the contact index would result in a lower interface stiffness (holding all other parameters constant). This conflicts with previous experimental findings, but can be attributed to the fact that *Contact Index* and *Gap Thickness* are not independent; there is an inversely proportional relationship between the *Contact Index* and the *Gap Thickness* ($r^2=0.63$). Since the *Contact Index* was the least significant estimate, it was, like the *Intersection Index*, ignored in the second regression model (Table 3; Regression model 2).

Considering only the *Gap Thickness* and $\cos(\alpha)$, the predicted stiffness $\log\left(\frac{\partial T'}{\partial \Delta}\right)$ still showed a strong correlation with the estimates ($r^2=0.81$, $p<0.0001$) (Figure 6a). There was

also a strong correlation ($r^2=0.73$, $p<0.0001$) between $\frac{\partial z}{\partial \Delta}$ and $A \cdot \sin(\alpha) \cdot \frac{\partial T'}{\partial \Delta}$ (equation 3), in

which ‘A’ was estimated to be 0.316 and $\frac{\partial T'}{\partial \Delta}$ the aforementioned predicted stiffness from Regression model 2 (Figure 6b).

3.2.1 Elastic cohesive model—Hence, using the predictions as mentioned above, the interfacial normal and tangential tractions (T_N and T_T) could be determined:

$$\begin{bmatrix} T_N \\ T_T \end{bmatrix} = \frac{\partial T'}{\partial \Delta} \cdot \Delta \cdot \begin{bmatrix} \cos(\alpha) \\ \sin(\alpha) \end{bmatrix} + 0.316 \cdot \sin(\alpha) \cdot \frac{\partial T'}{\partial \Delta} \cdot \Delta \cdot \begin{bmatrix} -\sin(\alpha) \\ \cos(\alpha) \end{bmatrix} \quad (9)$$

Knowing that $\cos(\alpha) = \frac{\Delta_N}{\Delta}$, $\sin(\alpha) = \frac{\Delta_T}{\Delta}$, and $\frac{\partial T'}{\partial \Delta} = 10^{-6.369GT - 0.289 \frac{\Delta_N}{\Delta} + 2.439}$ the previous equation can be rewritten in terms of Δ , Δ_N and Δ_T , in which $\Delta = \sqrt{\Delta_N^2 + \Delta_T^2}$:

$$\begin{bmatrix} T_N \\ T_T \end{bmatrix} = 10^{-6.369GT - 0.289 \frac{\Delta_N}{\Delta} + 2.439} \begin{bmatrix} \Delta_N - 0.316 \cdot \frac{\Delta_T^2}{\Delta} \\ \Delta_T \left(1 + 0.316 \cdot \frac{\Delta_N}{\Delta} \right) \end{bmatrix} \quad (10)$$

This model resulted in a generally satisfactory fit between the simulated and the predicted elastic responses for all four models (Figure 7). Of note for the T_N - Δ_N responses was that when the tensile or compressive direction was predicted nicely, the opposite direction was rather under or over predicted. The tangential elastic stiffness for model 2 and 3 was predicted with good fidelity, while the stiffness of model 1 and 4 were a rather under and over predicted, respectively.

3.2.1 Failure cohesive model—When the mixed-mode model as proposed by Wei and Hutchinson (2008) was considered, the responses showed some artifacts in the normal direction (Figure 8; upper row), particularly for the post-yield response. The mixed-mode model had some difficulties with predicting the ultimate tensile strength ($T_{N,ult}$) and its corresponding displacement (δ_N). The RMS difference between $T_{N,ult}$ as determined by the simulations and the predicted mixed-mode model was 0.28 MPa. For δ_N the RMS difference was 64 μ m. However, the responses in normal compression were predicted with fewer artifacts than the elastic cohesive model. In the tangential direction, the mixed-mode responses were predicted satisfactorily for model 2 and 3 (Figure 8; lower row). As was found in the elastic cohesive model (Figure 7), the stiffness of model 1 and 4 was slightly under and over predicted, respectively.

4. DISCUSSION

The aim of this study was to investigate the mixed-mode behavior of cement-bone interfaces from postmortem retrievals utilizing micromechanical FEA models and, subsequently, generate an elastic and failure cohesive model based on the determined mixed-mode response and the interfacial morphology. This study distinguishes itself from physical experiments because one single interface morphology could be loaded until failure in multiple directions instead of one single direction.

The results show that the tensile strength ($T_{N,ult}$) and stiffness $\left(\frac{\partial T_N}{\partial \Delta_N} \right)$ as obtained by the FEA

models compare well with experimental observations. However, the normal stiffness $\left(\frac{\partial T_N}{\partial \Delta_N} \right)$ at 0° is rather stiff compared to the mean normal stiffness as obtained by experimental findings (Table 2). This overestimation can be explained by the origin of the specimens; specimens 1 and 4 were harvested from the stiffest donor as used in the study of Mann et al., 2010 (model K). Although it is known that in compression the interface is stiffer than in

tension (Mann et al., 2008; Miller et al., 2011) the compressive stiffness as determined with the FEA models seems to be overestimated. In pure shear, a considerable compressive traction is needed to prevent dilation of the interface. This phenomenon has been described before in other interface studies (Waanders et al., 2011a; Salomonsson, 2008). All mixed-mode responses show a gradual transition between the three ‘principal’ responses (tension, shear and compression).

The boundary conditions as applied in the current study are different from the boundary conditions that occur in physical experiments. In the current study, no relative motions between the top and bottom plane were possible, whereas they can occur in experiments as a result of the experimental setup (Miller et al., 2011). As a result of this restriction, the mechanical stiffness can increase since the “path of least resistance” cannot be followed. This could also be an explanation of the overestimated stiffness in pure compression. Furthermore, the applied periodic boundary conditions result in a multi-scale representation of the interface which is not possible to validate experimentally. In order to assess the effect of the periodic boundary conditions, additional simulations have been executed in which the periodic boundary conditions were removed. It appears that the interface with periodic boundary conditions is stronger and stiffer relative to the interface without, but the differences are small (Figure 9). However, a considerable difference can be observed in the distribution of the Von Mises stresses; the interface with periodic boundary conditions presents a smooth transition of the stresses over the two sides, where the interface without periodic boundary conditions does not (Figure 9).

In order to generate an elastic mixed-mode cohesive model, the stiffness in line with the

applied displacement, Δ , was determined $\left(\frac{\partial T'}{\partial \Delta}\right)$. This stiffness was successfully related to the *Contact Index* (CI), *Gap Thickness* (GT) and loading angle ($\cos(\alpha)$) (Table 3; Regression model 1). However, the estimate of CI was found to be negative. This is counterintuitive, since it implies that a decrease of CI increases the interface stiffness. This conflicting finding can be attributed to the inversely proportional relationship between CI and GT for the four models ($r^2=0.63$); an increase of gaps as a result of bone resorption decreases the amount of cement-bone contact. Considering 50 postmortem specimens, Miller et al. (2010) also found an inverse proportional relationship between GT and CI ($r^2=0.42$). Since CI was the least significant estimate in the fit, CI was disregarded in further stepwise regression in

generating a morphological based model to describe $\frac{\partial T'}{\partial \Delta}$. This resulted in a fit for $\frac{\partial T'}{\partial \Delta}$ which only considered GT as a morphological input and the loading angle, $\cos(\alpha)$ ($\bar{r}^2=0.81$, $p<0.0001$) (Table 3; Regression model 2). The estimate of GT was found to be negative, indicating that an increase of the *Gap Thickness* decreases the interface stiffness. Surprisingly, the estimate of $\cos(\alpha)$ was the same for both Regression model 1 and 2. When

CI would have been used in the fit to describe $\frac{\partial T'}{\partial \Delta}$ instead of GT, the estimate of CI would be positive (CI=1.174, $p=0.008$). This is consistent with what has been found previously (Miller et al., 2010). However, a fit which only includes CI, $\cos(\alpha)$ and a constant reduces the correlation considerably relative to a fit with GT ($\bar{r}^2=0.30$, $p=0.0005$).

A mixed-mode cohesive model which also includes failure was determined based on the model as proposed by Wei and Hutchinson (2008). This model has, besides Δ_N and Δ_T , four different input parameters: the normal strength ($T_{N,ult}$) and its corresponding displacement

(δ_c), the tangential stiffness in pure shear $\left(\frac{\partial T_T}{\partial \Delta_T}\right)$ and a fitting coefficient (β). The normal

strength ($T_{N,ult}$) is calculated based on a positive linear relationship between $T_{N,ult}$ and $\frac{\partial T_N}{\partial \Delta_N}$. It would also be possible to calculate $T_{N,ult}$ according to a morphological fit as proposed by Miller et al. (2010). However, when we applied this relationship we found the same RMS difference between $T_{N,ult}$ of the simulations and the predicted mixed-mode model, but the relative difference, especially for the weak model 2, was much higher. Additionally, the RMS difference for δ_c was increased by a factor of 2.5 when the model of Miller et al. (2010) was considered. The variable δ_c was calculated based on the data of Mann et al. (2008) where the interface displacements at yield and the ultimate strength were determined relative to their corresponding stresses. Using this approach, δ_c could be determined as a

function of $T_{N,ult}$ and $\frac{\partial T_N}{\partial \Delta_N}$. Although the data reported by Mann et al. (2008) was from lab-

prepared specimens, we believe the same function of $T_{N,ult}$ and $\frac{\partial T_N}{\partial \Delta_N}$ will be found for post-mortem responses. Finally, we believe it is more accurate than simply assuming δ_c to be the

division of $T_{N,ult}$ with $\frac{\partial T_N}{\partial \Delta_N}$, although the T_N - Δ_N responses of model 2 and 3 might presume so.

The main limitation of this study is the absence of a direct quantitative validation. However, the methodology used to model the micromechanical FEA models has previously been used in studies in which the FEA models were successfully validated to experimental results (Waanders et al., 2009; Janssen et al., 2009; Janssen et al., 2008a). Although these studies involved lab-prepared specimens, we believe the same methodology was applicable for postmortem specimens. Furthermore, a direct validation was not possible since within physical experiments the specimens could be tested until failure in one direction only and not in multiple directions. We were therefore forced to do a quantitative validation of previously published data. Unfortunately, the amount of quantitative mechanical data of postmortem cement-bone interface specimens is, in contrast to lab-prepared data, also limited (Miller et al., 2011; Mann et al., 2010; Miller et al., 2010). In the past, studies to post-mortem cement-bone interfaces mainly focused on the histology rather than the mechanical behavior of the interface (Clauss et al., 2010; Bishop et al., 2009; Schmalzried et al., 1993; Jasty et al., 1991). Finally, the FEA models used in this study do not directly match the geometry of the postmortem specimens as a result of the mirroring. However, the mirroring was necessary in order to avoid off-axis tractions in the cohesive model. It is more than likely that these off-axis tractions occur in experiments in which asymmetric postmortem specimens are used.

Another big limitation is the small sample that was used. The step-wise regression analysis

in which the stiffness $\frac{\partial T'}{\partial \Delta}$ was related to the *Contact Index*, *Intersection Index*, *Gap Thickness* and $\cos(\alpha)$ could be improved through use of a larger sample. However, it is unknown whether a larger number of samples would change the outcome of the significance of each morphological parameter. Consistent with the present study it has recently been shown that the *Contact Index*, and thus the *Gap Thickness*, influences the interface stiffness and the *Intersection Index* does not (Miller et al., 2011). It is questionable whether a larger sample would disapprove this finding.

The penetration depth of the cement into the bone was not considered as a morphological factor. Although a larger penetration depth correlates nicely with the interface strength in unidirectional loading (Waanders et al., 2010), no correlation was found when considering the strength under multiple loading directions (Wang et al., 2010). Moreover, two other

studies have shown that there is no correlation between the amount of cement penetration and the interface stiffness in mixed-mode loading (Waanders et al., 2011a; Miller et al., 2011).

From a clinical perspective, the results of the current study implicate that minimizing the gap between the cement and the bone enhances the mechanical properties of the cement-bone interface. From a surgical point of view the gap could be minimized by the degree of cement pressurization (Gozzard et al., 2005), although over pressurizing of the femoral canal can lead to fat and bone-marrow embolism syndrome, which can sometimes even be fatal (Sierra et al., 2009). However, gaps could also be created due to heat necrosis as a result of cement polymerization (Mjoberg, 1986), bone remodeling (Goodman et al., 1997) or cement shrinkage (Roques et al., 2004).

The cohesive models as derived in this study are applicable to multiple applications in the research to cemented hip implants. One could even consider whether this method could be applicable to any cemented orthopaedic device, such as tibial trays or glenoid components. A possible application of the derived cohesive model involves FEA simulations for macroscopic pre-clinical testing of newly developed orthopaedic implants or cement-bone interface optimization. Even *in-vivo* studies in which bone degeneration at the cement-bone interface is monitored on a micro scale might be considered. The cohesive models could possibly indicate what the mechanical consequences of such remodeling are or explain the cause of remodeling based on local stress intensities.

We conclude that the simulated mixed-mode behavior of the cement-bone interface from postmortem retrieved cemented hip replacements satisfactorily match experimental findings with similar specimens, albeit the stiffness is somewhat overestimated in compression. The obtained mixed-mode stiffness response can subsequently be related to the interface morphology and can be formulated in an elastic cohesive model. Finally, the acquired data can be used as an input for a cohesive model which also includes interface failure.

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a.

Model 1

Model 2

Model 3

Model 4

b.

Figure 1.

a. The initial mesh was first mirrored in the y-z plane and subsequently mirrored in the x-z plane.

b. The four models as used in this study. The dimensions of the four models were on average $\sim 4.2 \times 7.2 \times 3.0 \text{ mm}^3$.

Figure 2.

The boundary conditions as applied in this study: All nodes at $z=0$ were fixated in all degrees of freedom, while all nodes at $z=h$ were uniformly displaced in seven different directions. Periodic boundary conditions were applied on the model's left and right side using nodal ties and were defined as: $u_i(x,0,z) = u_i(x,w,z)$. In this equation, ' u_i ' represents the displacement in x, y and z-direction. Tangential displacements were consequently displaced in positive y-direction.

Figure 3.

The micro CT data set consisted of the bone (top) and cement (bottom). At various locations, there was cement penetration into the cavities of the bone. The white spots in the bulk cement can be attributed to the presence of BaSO₄ particles. A grid (0.3 × 0.3 mm spacing) was placed over the micro-CT data and lines were projected vertically through the specimen. At those locations where a projection line crossed the interface, the interface status was designated as either apposition (triangle) or proximity (circle). Also, the gap thickness between the bone and cement (white lines) was measured for projection line. From this, the *Contact Index* (CI), *Intersection Index* (II) and *Gap Thickness* (GT) could be determined. It has previously been shown that these parameters can be used to clarify the cement-bone interface response (Miller et al., 2010; Miller et al., 2011).

Figure 4.

The load vector ($\mathbf{T}$) was decomposed into $\mathbf{T}'$ and $\mathbf{z}$. The vector $\mathbf{T}'$ was defined as the orthogonal projection of $\mathbf{T}$ onto Δ . The vector $\mathbf{z}$ was defined as the component of $\mathbf{T}$ orthogonal to Δ .

Figure 5. The traction-displacement relationships between (a) T_N and Δ_N , (b) T_N and Δ_T , (c) T_T and Δ_T , and (d) T_T and Δ_N of model 4. In pure tension ($\alpha = 0^\circ$), the complete traction-displacement response is captured in subfigure (a). As a result of the symmetry, no tangential tractions occurred in pure tension (d). In pure shear ($\alpha = 90^\circ$), no ultimate strength was found (c) and a considerable normal compression was needed to prevent dilation of the interface (b).

Figure 6.

a. There was a strong correlation ($\bar{r}^2=0.81$, $p<0.0001$) between the logarithmic value of the

stiffness $\frac{\partial T'}{\partial \Delta}$ and the *Gap Thickness* and $\cos(\alpha)$: $\log\left(\frac{\partial T'}{\partial \Delta}\right) = a_3 GT + a_4 \cos(\alpha) + a_5$ (Table 3; Regression model 2).

b. A strong correlation ($r^2=0.72$, $p<0.0001$) was found between $\frac{\partial z}{\partial \Delta}$ as measured from the

simulations and $A \cdot \sin(\alpha) \cdot \frac{\partial T'}{\partial \Delta}$, in which $\frac{\partial T'}{\partial \Delta}$ was the aforementioned predicted stiffness by Regression model 2. The variable 'A' was estimated to be 0.316.

Figure 7. Comparison between the finite element simulated responses (solid lines) to mixed-mode loading and the predicted elastic responses (dashed lines) using the Regression Model 2 for the four finite element models. The upper and bottom rows show the responses in the normal and shear directions, respectively. For each model, 0°, 90°, and 180° indicate loading in tension, shear, and compression, respectively.

Figure 8.

Comparison between the finite element simulations (solid lines) and the responses using the mixed-mode model (dashed lines) as proposed by Wei and Hutchinson (2008). The upper and bottom rows show the responses in normal and shear directions, respectively. For each model, 0°, 90°, and 180° indicates loading in tension, shear, and compression, respectively..

Figure 9.

- a. The mixed-mode response of Model 1 with and without periodic boundary conditions. When periodic boundary are considered, the interface was about 6% stronger in tension. The periodic boundary conditions also make the interface stiffer; 18%, 4% and 9% in tension, compression and shear, respectively.
- b. Von Mises stress in Model 1 after a tangential displacement, Δ_T , of $15\mu m$. When periodic boundary conditions are considered, there is a smooth stress distribution between the left and right side of the model. Without periodic boundary conditions, the stresses are truncated.

Table 1

Donor and FEA model information for the four models

	Specimen 1	Specimen 2	Specimen 3	Specimen 4
Specimen size (l×b×h mm ³)	3.98×2.06×2.70	3.41×2.42×3.00	4.04×2.00×3.20	3.65×2.06×3.40
Age	67	93	93	67
Gender	Female	Female	Female	Female
Cause of death	Alzheimer's disease	Renal insufficiency	Renal insufficiency	Alzheimer's disease
Years in service	14	Not available	Not available	14
Distance from calcar [mm]	100	70	70	110
A/P-M/L quadrant	Medial	Medial	Lateral	Lateral
Vacuum-mixed	Yes	Yes	Yes	Yes
Fixation quality	Not loose	Possibly loose	Not loose	Not loose
	Model 1	Model 2	Model 3	Model 4
FEA model size (l×b×h mm ³)	7.97×4.13×2.70	6.82×4.85×3.00	8.08×4.01×3.20	7.30×4.13×3.40
Number of elements	343,988	357,236	468,308	367,160
Number of nodes	80,804	83,589	106,473	81,078

Mechanical and morphological parameters of the four models and experimental findings utilizing postmortem retrievals (Miller et al., 2010; Miller et al., 2011). Unfortunately, no experimental postmortem retrieval data in pure shear was available

Table 2

Model	0°	0°	$\frac{\partial T_N}{\partial \Delta_N} \left[\frac{\text{MPa}}{\text{mm}} \right]$	$\frac{\partial T_N}{\partial \Delta_N} \left[\frac{\text{MPa}}{\text{mm}} \right]$	180°	90°	CI [-]	II [-]	GT [mm]
				$\frac{\partial T_T}{\partial \Delta_T} \left[\frac{\text{MPa}}{\text{mm}} \right]$					
1	0.55	21.7		154.4	248.1	154.4	0.18	1.01	0.101
2	0.10	5.4			21.4	10.1	0.13	1.34	0.231
3	0.81	93.0			258.6	139.4	0.29	1.26	0.076
4	0.56	37.7			441.2	184.3	0.74	1.20	0.015
Mean (SD)	0.51 (0.30)	39.5 (38.1)		122.1 (76.9)	242.3 (171.9)		0.34 (0.28)	1.20 (0.14)	0.11 (0.09)
Miller et al., 2010 Mean (SD)	0.21 (0.32)	16 (35)		47 (61)		-	0.11 (0.17)	0.72 (0.62)	0.33 (0.28)
Miller et al., 2011 Mean (SD)	-	19.2 (25.6)		22.7 (35.7)		-	0.57 (0.31)	1.89 (0.69)	-

Table 3

The logarithmic value of the interfacial stiffness in the direction of the applied displacement $\left(\log\left(\frac{\partial T'}{\partial \Delta}\right)\right)$ was linearly related to the *Contact Index* (CI), *Intersection Index* (II), *Gap Thickness* (GT) and the cosine of the loading angle ($\cos(\alpha)$) ($\bar{r}^2=0.91$, $p<0.0001$) (Regression model 1):

$$\log\left(\frac{\partial T'}{\partial \Delta}\right)=a_1CI+a_2II+a_3GT+a_4\cos(\alpha)+a_5.$$

With GT as the only morphology parameter (Regression model 2), there is still a strong correlation between $\log\left(\frac{\partial T'}{\partial \Delta}\right)$ and the predicted estimates ($\bar{r}^2=0.81$, $p<0.0001$).

Regression model 1			Regression model 2			
Regression Model Term	Estimate	SE	p-Value	Estimate	SE	p-Value
CI [-] (a_1)	-1.346	0.242	<0.0001	-	-	-
II [-] (a_2)	-0.140	0.390	0.72	-	-	-
GT [mm] (a_3)	-9.646	0.735	<0.0001	-6.369	0.649	<0.0001
cos(α) [-] (a_4)	-0.289	0.046	<0.0001	-0.289	0.068	<0.0001
Constant [-] (a_5)	3.233	0.154	<0.0001	2.439	0.086	<0.0001