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Cross-Shear Implementation in Sliding-Distance-Coupled Finite Element Analysis of Wear in Metal-on-Polyethylene Total Joint Arthroplasty: Intervertebral Total Disc Replacement as an Illustrative Application

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Abstract

Computational simulations of wear of orthopaedic total joint replacement implants have proven to valuably complement laboratory physical simulators, for pre-clinical estimation of abrasive/adhesive wear propensity. This class of numerical formulations has primarily involved implementation of the Archard/Lancaster relationship, with local wear computed as the product of (finite element) contact stress, sliding speed, and a bearing-couple-dependent wear factor. The present study introduces an augmentation, whereby the influence of interface cross-shearing motion transverse to the prevailing molecular orientation of the polyethylene articular surface is taken into account in assigning the instantaneous local wear factor. The formulation augment is implemented within a widely-utilized commercial finite element software environment (ABAQUS). Using a contemporary metal-on-polyethylene total disc replacement (ProDisc-L) as an illustrative implant, physically validated computational results are presented to document the role of cross-shearing effects in alternative laboratory consensus testing protocols. Going forward, this formulation permits systematically accounting for cross-shear effects in parametric computational wear studies of metal-on-polyethylene joint replacements, heretofore a substantial limitation of such analyses.

Introduction

Osteolytic reaction to submicron-size abrasive/adhesive wear debris is a longstanding concern in orthopaedic total joint replacement. Enormous effort has gone into reducing such wear, through modification of materials (Harris et al., 2005) and surface treatments (Santavirta, 2003), implant design improvements (Huskies and Verdonshot, 2007), and refinements of implantation technique (Wan et al., 2008). Although different implant designs and anatomical sites have specific considerations, a common issue pre-clinically is the need to reliably estimate *in vivo* wear performance. Historically, laboratory physical simulators have been the primary

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vehicle for that purpose. Physical simulators have evolved to incorporate most of the major factors thought to influence wear in the *in-vivo* setting (Clarke, 1982; Ortega-Saenz et al., 2007; Kaddick and Wimmer, 2001). However, the equipment involved is expensive and time-consuming, data variance can be appreciable even between stations on a single simulator, and flexibility to address novel influence factors is quite limited.

As a complementary vehicle to bypass these disadvantages, computational wear simulations have come into increasing usage. Early effort in that direction was led by Maxian et al. (Maxian et al., 1996a), who reported a finite element implementation of the classic Archard/Lancaster relationship (Archard, 1953), for application to the situation of metal-on-polyethylene total hip replacement. Subsequent extensions of that formulation, summarized in Table 1, have addressed adaptive changes of bearing surface geometry (Maxian et al., 1996b), material nonlinearities (Teoh et al., 2002), boundary element methodology (Sfantos and Aliabadi, 2007b), frictional heating effects (Fialho et al., 2007), regional variations of interface tribology (Brown et al., 2002), and heterogeneous duty cycles (Lundberg et al., 2007). Applications to other designs have included total knees (Zhao et al., 2008), total shoulders (Hopkins et al., 2007), total spinal disc replacements (Rawlinson et al., 2007) and hip resurfacing prostheses (Liu et al., 2008), plus various types of (non-implant) machine bearings (Pödra and Andersson, 1999).

One limitation of these computational wear simulations has been that they seldom have addressed cross-shear. Ultra-high molecular weight polyethylene (UHMWPE) consists of ethylene monomers (C_2H_4) which, during processing, lose the double bond between carbons and connect to form long molecular chains with a carbon backbone. During physical articulation, these initially isotropic chains acquire a preferential molecular orientation at the bearing surface, due to adhesive/abrasive material draw-out in the habitual directional motion of the apposing counterface (Wang, 2001). When the articular motion is in the direction of the preferential orientation, wear particles are less likely to be generated than when the motion is transverse to this orientation. Recognition of this fact has stimulated extensive efforts to replicate physiologic motion patterns in contemporary laboratory wear simulation designs (Bragdon et al., 1995).

In principle, cross-shearing effects can be incorporated in computational wear simulations, by basing the local wear factor on the direction of counterface motion relative to the locally prevailing principal molecular orientation (PMO). Doing so of course requires experimental information as to the cross-shear dependence of polyethylene wear rate. Data to that effect have recently been reported by Kang et al. (Kang et al., 2008b), for polyethylene pins under combined reciprocal/rotational motion against cobalt-chrome plates, with the motion parameters chosen so as to achieve selected levels of cross-shear. From this, the experimentally measured relationship between wear factor and cross-shear was regressed in terms of a power law. In subsequent studies (Kang et al., 2008a, 2009), this same group invoked cross-shear dependence of the wear factor in a simplified computational model of total hip wear, with the contact stress distribution determined from a constrained-column analysis. Strickland and Taylor (2009) and Willing and Kim (2009) have since incorporated sliding direction dependence into rigid body and finite element models, respectively. The present paper reports implementation of cross-shear dependence in an especially versatile computational framework of an adaptively re-meshed finite element model that incorporates changes of PMO direction and cross-shear during evolution of the wear process. Physical validation of the computational wear simulation is performed for the case of a contemporary total disc replacement (TDR) implant (ProDisc-L) under two different loading regimes. Computational wear data are then reported for the ProDisc subjected to duty cycles involving appreciably different cross-shear regimes.

Methods and Materials

A finite element mesh for a Prodisc lumbar TDR was generated in ABAQUS (Figure 1). The dome of the ultra-high molecular weight polyethylene (UHMWPE) insert was modeled with 8-node quadrilateral elements with a (linearly elastic) modulus of 1,400 MPa and a Poisson's ratio of 0.3. (As the remainder of the implant is not subjected to direct loads or wear, it was not included). The (CoCr) upper endplate was modeled as a rigid analytical surface. The inferior side of the UHMWPE insert was also treated as a rigid surface. Forces and displacements were applied to the upper endplate and inferior surface of the UHMWPE insert through reference points located at the implant's center of rotation. The contact surface was assigned a friction coefficient of 0.08, typical of CoCr-on-UHMWPE bearing surfaces (Otto et al., 2001; Wang et al., 2001).

Rotation and loading inputs (based on ISO Standard 18192-1, see below) were applied via time-dependent amplitudes within a single step. The implant was inclined 10 degrees, as recommended for lumbar prostheses. Rotational inputs were applied to the insert, while the axial load was applied to the upper endplate. Translational degrees of freedom for the undersurface of the UHMWPE insert were constrained, as were rotational degrees of freedom for the upper endplate. Translational degrees of freedom for the upper endplate were unconstrained.

The loading, cross-shear and wear depth calculations, and subsequent mesh updates were incorporated into a single ABAQUS job, using the program's adaptive meshing capabilities and the user subroutine UMESHMOTION. ABAQUS' adaptive meshing allows the user to specify a region of elements in which ABAQUS will automatically adjust interior nodes to maintain element (geometric) quality. The user can also select exterior regions (surfaces, edges) on which to define mesh constraints, which specify node motion independent of stresses and strains. Thus, material erosion can be simulated by moving surface nodes in a direction normal to the surface, while ABAQUS automatically adjusts interior nodes to prevent element collapse.

In this study, as in previous studies (Lundberg et al., 2007), linear wear depths for the UHMWPE bearing surface nodes were calculated for a single motion cycle, then scaled by the number of cycles in an update interval, after which the surface topography was adjusted accordingly. The entire insert was designated as an adaptive mesh domain, with a user-defined adaptive mesh constraint defined on the polyethylene bearing surface. At the end of each step, the cross-shear and incremental wear depth were computed, the per cycle wear depth was scaled by the update interval, and nodes were adjusted accordingly. Simulations were run for 10 million cycles, at 250,000 cycle update intervals (based on a convergence evaluation of the effect of update interval on resulting wear rate), thus requiring 40 steps per simulation.

Local per-duty-cycle wear was simulated by implementing the discretized Archard/Lancaster wear formulation used by Maxian et al. (1996b) in their study of hip arthroplasty wear, but augmented by usage of a cross-shear dependent wear coefficient:

$$w = \sum_{i=1}^{25} k(CSR) \cdot \sigma_i \cdot |\vec{u}_i|, \quad (1)$$

where w is the local wear depth per duty cycle, k and CSR are the instantaneous local wear coefficient and cross-shear ratio, and σ_i and u_i are the local contact pressure and sliding distance vector for increment i (the duty cycle being divided in 25 increments), respectively. The

dependence of the wear factor on cross-shear was based on the power-law relationship reported by Kang et al. (2008b),

$$k=8 \times 10^{-7} \cdot CSR^{0.2071} \text{ mm}^3 \text{ N}^{-1} \text{ m}^{-1}. \quad (2)$$

Due to articulation couple tribologic differences (e.g., contact pressure, surface roughness, lubrication, etc.) between the pin-on-disk studies of Kang et al. versus TDRs, the relationship of Equation 2 was multiplied by a scalar so that computational ProDisc-L wear rates were comparable to the physical results reported by Rawlinson et al. (2007). Furthermore, literature indicates that wear factors on the order of $1 \times 10^{-8} \text{ mm}^3 \text{ N}^{-1} \text{ m}^{-1}$ are expected for purely linear motion of polyethylene-on-metal couplings (Saikko and Ahlroos, 1999). Therefore, a slight modification of the Kang et al. curve was performed, such that $k(0)=1 \times 10^{-8} \text{ mm}^3 \text{ N}^{-1} \text{ m}^{-1}$. The final wear factor curve, incorporating both of these changes, is:

$$k=2.8 \times 10^{-6} \cdot (CSR+1.53 \times 10^{-12})^{0.2071} \text{ mm}^3 \text{ N}^{-1} \text{ m}^{-1}. \quad (3)$$

Cross-shear was quantified as described by Kang et al. (2008b), utilizing a cross-shear ratio (CSR) equated to the fraction of frictional work performed in a direction perpendicular to the principal molecular orientation (PMO) of the polyethylene chains. It is assumed that the polymer chains preferentially align in the direction of the dominant frictional work. Therefore, the PMO is defined as the orientation which would minimize the amount of transverse frictional work per duty cycle.

To implement this concept, the contact pressure (σ) and differential tangential slip vectors (u_1, u_2) of the UHMWPE relative to the metal counterface were collected for each node, over the entire motion cycle. An illustrative nodal motion locus, weighted with contact pressures, is shown in Figure 2.

The direction (ϕ_f) of incremental motion (relative to an internal reference frame that rotates with the UHMWPE bearing surface) can be calculated from the tangential slip vectors:

$$\phi_f = \tan^{-1} \left(\frac{u_2}{u_1} \right). \quad (4)$$

Taking into account the coefficient of friction (μ) and the area (ΔA) associated with each node (each surface node can be considered associated with $1/4^{\text{th}}$ of each of the four element surface faces it is connected to, so each node has an associated area approximately equal to the area of an element surface face), the magnitude of the local frictional (shear) work vector for each segment of the duty cycle can be calculated as

$$W_f = |u| \cdot \mu \cdot \sigma \cdot \Delta A, \quad (5)$$

where $|u|$ is the magnitude of the incremental tangential slip vector. Frictional work vectors for a typical node are plotted in polar form in Figure 3.

Given a test axis such as the dashed line shown in Figure 3, at angle ϕ , frictional force and incremental sliding distance components parallel to and perpendicular to that axis can be calculated for each increment:

$$\begin{aligned} F_{\parallel} &= -\sigma \cdot \Delta A \cdot \mu \cdot \cos(\varphi_f - \phi) \\ F_{\perp} &= -\sigma \cdot \Delta A \cdot \mu \cdot \sin(\varphi_f - \phi) \\ u_{\parallel} &= |u| \cdot \cos(\varphi_f - \phi) \\ u_{\perp} &= |u| \cdot \sin(\varphi_f - \phi) \end{aligned} \quad (6)$$

From these values, the frictional work parallel and perpendicular to the test axis can be calculated:

$$\begin{aligned} W_{\parallel} &= |F_{\parallel} \cdot u_{\parallel}| \\ W_{\perp} &= |F_{\perp} \cdot u_{\perp}| \end{aligned} \quad (7)$$

As stated above, the PMO can be defined as that axis along which most of the frictional work occurs. Therefore, the PMO over a given motion cycle can be defined as coincident with the test axis that results in the lowest cumulative ratio of $W_{\perp}$ to W_f throughout the entire motion cycle. Practically, this is implemented by rotating a test axis through 180 degrees and computing the ratio of $W_{\perp}$ to W_f throughout the motion cycle at each axis position. In the illustration shown in Figure 4, the function minimum is 75 degrees, the orientation of the green line shown in Figure 3.

With the PMO thus defined, the cross-shear over one motion cycle can be calculated. The cross-shear ratio (CSR) is defined as the fraction of the total frictional work that is perpendicular to the PMO, expressed mathematically as:

$$CSR = \frac{\sum_{inc} W_{\perp}}{\sum_{inc} (W_{\perp} + W_{\parallel})} \quad (8)$$

In this study the PMO for a given motion cycle was dynamically adjusted, based upon output of the previous motion cycle. With this formulation, a CSR of 0 indicates purely uniaxial motion along the PMO, while a CSR ratio of 1 indicates that all frictional work throughout the motion cycle is performed in a direction perpendicular to the PMO.

The incorporation of all aspects of the wear simulation into a single ABAQUS job proved to be efficient. Run times with Linux on a 3 GHz Intel Xeon processor with 16 GB RAM were approximately 3.5 hours. Furthermore, outputs specific to wear simulation, such as local wear depth, PMO, and cross-shear, were easily calculated and stored as the analysis progressed, in terms of user-defined field variables via the ABAQUS subroutine UFIELD. The volumetric wear of the UHMWPE insert was reported by the ABAQUS history variable VOLC, which was differentiated in time to provide volumetric wear rates, and converted to mass wear rate by multiplication by the density of UHMWPE (0.94 mg/mm³ (McKellop et al., 1995)).

For physical validation purposes, six ProDisc TDRs were tested in a 5 degree-of-freedom wear-testing machine. For 4 million cycles, the loading profile of ISO Standard 18192-1 was applied

(Figure 5a), followed by 2 million cycles under a reduced cross-shear protocol in which the lateral bending waveform was shifted to be in phase with the flexion-extension curve. Gravimetric assessment of wear loss was performed every 1 million cycles, with load-soak controls maintained. A corresponding finite element wear model was subjected to the same loads and boundary conditions as the physical test. In both the physical and finite element tests, the reduced-cross-shear loading immediately followed the high cross-shear test, on the same specimens.

Following validation, three variants of loading (Figure 5) were applied to the FE implant model to demonstrate the importance of cross-shear in long-term wear simulations. Two of these variants used the rotations and loadings of ISO standard 18192-1. In one variant (High CS), the combined rotations were applied per the standard, for 10 million cycles, resulting in a crossing motion that generated appreciable cross-shear. In a second variant (Uniaxial*3), the same set of rotational inputs were used, but applied individually (a testing option included in the current ASTM guidance document), each for 10 million cycles, resulting in three concatenated sequences of much more unidirectional local motion, even though encompassing the same amount of “aggregate” motion as for the combined/concurrent sequence. Finally, a third simulation (Low CS) was run approximating the loading used by Nechtow et al. (2006) to deliberately produce low cross-shear, by eliminating lateral bending. (This particular input set included a single-peak axial load profile and a slightly altered axial rotation profile, compared to the current ISO standard.)

Results

The physical validation showed good wear rate agreement at both cross-shear regimes (Figure 6). The average difference in wear rates between the experimental results and the computational results was 2.3 mg/mc, or 22%. The error percentage was lower in the first four million cycles (13%) than in the last two million cycles (40%), although the absolute error was comparable for both loading protocols (2.2 mg/mc and 2.382 mg/mc, respectively). The spatially averaged cross-shear ratio (CSR) for the first four million cycles (0.15) is twice that of the final two million cycles (0.071).

The three variations in loading input resulted in substantial variations in average CSR. The High CS case (ISO standards as specified) resulted in a spatially averaged CSR of 0.211 (this is higher than in the previous paragraph because the 10-degree incline was not included in that case, in order to match the setup used experimentally). The spatial average CSR generated when these inputs were separated were significantly lower: 0.0005 during flexion/extension, 0.0097 during lateral bending, and 0.0086 during axial rotation. The Low CS case also had significantly reduced spatial average CSR of 0.0645.

As expected given the variation in average cross-shear, the volumetric wear and gravimetric wear rate varied dramatically between the three input conditions. The total volumetric wear in the Low CS case (86.9 mm³) was 40% lower than that for the High CS case (145.6 mm³, Figure 7), with appropriately similar proportionality for the wear rate (8.1 mg/mc vs. 13.6 mg/mc) and maximum linear wear depth (0.49 mm vs. 0.8 mm, Figure 8). Under the Uniaxial rotation inputs, the total volumetric wear (60.1 mm³) was 60% lower than that for the High CS inputs, but this occurred over 30 million rather than 10 million cycles, therefore corresponding to substantially lower wear rates. Very distinct wear rates and wear distributions were demonstrated for each of the three individually applied uniaxial rotations (flex/ext = 1.88 mg/mc, lat. bending = 2.52 mg/mc, axial rot. = 0.96 mg/mc), with axial rotation resulting in the lowest wear rate, a 93% reduction from that of the High CS input. The pronounced spike in wear rate between flexion/extension and lateral bending uniaxial rotation inputs is a result of

temporarily high cross-shear as the direction of motion changes, prior to adaptation of the PMO.

Discussion

Incorporation of cross-shear effects in numerical simulations of wear of metal-on-polyethylene total joint replacement implants is an important step toward enhanced clinical realism for this class of models. The preceding (and pioneering) work on this concept by Kang et al. (2008a) and by Strickland and Taylor (2009) was based on somewhat narrower models that restricted the geometry that could be analyzed (the constrained column analysis of Kang et al.) or that did not allow for updating surface topology (the rigid body analysis of Strickland and Taylor). Willing and Kim (2009) have recently incorporated sliding direction dependence into a finite element model with evolving surface topology, using a direction dependence that is based on similar concepts to Kang's, but implemented in a very different, less analyst-friendly form. The present formulation is a highly efficient implementation, run solely within ABAQUS, which greatly reduces the amount of custom programming required in previous methodologies. With little to no modification, it is broadly applicable to a wide range of implant geometries, material types, and contact interface treatments. Potential applications include studies of implant design parameters, of component surgical placement and patient activity variants, and of alternative specifications for consensus standards for laboratory wear testing. Although the introduction of highly cross-linked polyethylenes has dramatically reduced the severity of the abrasive/adhesive wear problem in total hips, various considerations currently limit application of these new materials to other important sites of total joint arthroplasty such as knees, intervertebral discs, and ankles. Therefore, need exists for ongoing improvement in pre-clinical wear testing methodology.

Numerical simulation of abrasive/adhesive wear for complex geometries has been an area where the biomechanics community has been at the forefront of the broader mechanics community. Commercially available finite element programs have played an important role in terms of the necessary contact stress computations, but wear simulation *per se* has historically been the province of purpose written user post-processing routines (Table 1). As this area has continued to develop, however, utility routines implementing these post-processing procedures have begun to find their way into widely used commercial finite element programs. To our knowledge, the first such instance was in ABAQUS v 6.5, which was introduced in 2004, ten years after the first conference presentation (Brown et al., 1994) of the Maxian et al. formulation. Another major commercial program, MSC Software, has subsequently followed suit. Addressing cross-shear effects from within a commercial software package holds the advantage of straightforward availability to large numbers of potential users. Additional attractions of conventional codes include convenience and versatility of the modeling (i.e., availability of extensive element and material libraries, alternative interface treatments, etc.) and highly-evolved user interfaces.

As is always the case in computational simulation, physically accurate input data are key to model realism. For cross-shear dependency of wear factors, that information base is presently in its infancy. The pin-on-plate data from Kang et al. (2008b) were for different bearing couple tribology than for ProDisc-L, so it was necessary to invoke a scaling step on the wear coefficient curve to link in with the existing laboratory simulator results from Rawlinson et al. (2007). Nevertheless, the resulting curve for wear factor as a function of cross-shear led to computational correspondence with independent simulator experimental data (within 2.3 mg/mc) at two different cross-shear levels. Certainly, as more experimental data involving varying cross-shear levels under both pin-on-plate and full-joint tribologic conditions become available, even better computational models can be implemented. Indeed, in preliminary tests we have found that slight modifications to the wear factor curve can bring even closer

agreement in the physical validation presented here, particularly reducing the error of the low cross-shear protocol. The dependence of wear factor on cross-shear reported by Kang et al. (2008b) shows a near-plateau relationship, whereby the wear factor increases rapidly as the cross-shear increases to 0.1 and then levels off to a relatively constant value. This relationship may explain the generally good realism of previous isotropic computational wear simulations (Table 1): Other than for situations where conditions of low cross-shear predominate, an isotropic computational wear factor derived from a moderate or high cross-shear regime experiment has yielded very reasonable first approximations of global wear rate and wear distribution.

Among various open questions regarding numerical modeling of cross-shear, two unresolved issues particularly merit mention. First, the concept of principal molecular orientation is not purely a directional issue, but also involves the *intensity* of that orientation, i.e. what proportion of the polymer chains are actually aligned along the PMO. In the present computational implementation, intensity of PMO was not addressed, in large part because no corresponding experimental data were available. A second unresolved issue involves the rapidity of the development of the PMO, and the persistence of an existing PMO under conditions of changed articulation kinematics/kinetics. In the present study, based on preliminary experimental data from Sambasivan et al. (2007) showing rapid PMO development and short persistence, it was assumed that the PMO was fully “burned in” immediately after the first ABAQUS solution step, and that it completely changed immediately (again after the first ABAQUS step) when the input duty cycle or (adaptive) mesh geometry changed. If/when more definitive experimental data become available, latency or relaxation effects could be introduced to incorporate PMO “inertia.”

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Figure 1.
(a) CAD model, and (b) finite element model of the Prodisc-L TDR

Figure 2.

A contact pressure-weighted motion locus of a typical node on the UHMWPE insert, relative to the Co-Cr endplate.

Figure 3.

Polar representation of frictional work vectors for an illustrative node during one motion cycle. The radial coordinate indicates the magnitude of the work vector (units of mJ), and the angular coordinate indicates the relative direction of the work vectors to one another.

Figure 4.

The fraction of the frictional work perpendicular to a test axis, as a function of test axis orientation. The test axis that minimizes perpendicular frictional work is selected as the PMO.

Figure 5. Loading profiles for (a) High CS - ISO 18192, (b) Uniaxial*3 - ISO inputs separated/concatenated, and (c) Low CS - input per Nechtow et al. (2006)

Figure 6.

Experimental and finite element wear rate comparison under ISO Standard 18192-1 (first 4 million cycles) and a lower-cross-shear perturbation (final 2 million cycles). The error bars on the experimental data represent the range of wear rates from all 6 testing stations.

Figure 7.

(a) Volumetric wear, and (b) gravimetric wear rate for the Prodisc-L TDR under each of the three loading conditions.

Figure 8.

Peak FE-computed wear depths (after 10 million cycles) for models subjected to (a) High CS, (b) Low CS, (c) uniaxial flexion/extension, (d) uniaxial lateral bending, and (e) uniaxial axial rotation. Peak linear wear is shown in parentheses for each case. Contours in (c-e) represent the linear wear accumulated during that loading input only.

Table 1

Summary of computational wear studies in biomechanics. 'None' indicates that wear calculations were based on a single contact analysis, without evolution of surface topography. 'Not reported' indicates that the study included evolving surface topography, but did not specify method of implementation.

Year	Author	Study Design	Wear Factor ($\text{mm}^3/(\text{N}\cdot\text{m})$)	Implementation of Contact / Geometry Updates
1996c	Maxian et al.	FEA of THR	1.07×10^{-6}	ABAQUS / Custom Script
1996a	Maxian et al.	FEA of THR	1.07×10^{-6}	ABAQUS / None
1996b	Maxian et al.	FEA of THR	1.07×10^{-6}	ABAQUS / Gawk Script
1997	Maxian et al.	FEA of THR	1.53×10^{-6}	ABAQUS / None
1998	Pietrabissa et al.	Analytical solution of THR with wear dependent on surface roughness (Ra)	$(0.235 \times 10^{-4}) \times \text{Ra}^{(2.03)}$	Custom Script / Custom Script
1999	Kurtz et al.	FEA of frontside and backside wear in THR	1.07×10^{-6}	DYNA3D / ANSYS Script
2001	Raimondi et al.	Analytical solution of THR with wear dependent on surface roughness (Ra)	$(8.68 \times 10^{-6}) \times \text{Ra} + (1.51 \times 10^{-6})$	Custom Script / Custom Script
2002	Teoh et al.	FEA of THR with elasto-plastic UHMWPE properties	1.07×10^{-6}	ABAQUS / Not reported
2002	Brown et al.	FEA of THR with localized roughening	1.07×10^{-6}	ABAQUS / Custom Script
2003	Patil et al.	FEA of THR with nonlinear elastic UHMWPE properties	1.07×10^{-6}	Marc / Not reported
2003	Wu et al.	FEA of THR	8×10^{-7}	Fortran / Fortran
2005	Bevill et al.	FEA of THR with creep	1.07×10^{-7}	ABAQUS / Python Scripting
2005	Lundberg et al.	FEA of THR with localized roughening	1.07×10^{-6} where smooth	ABAQUS / Custom Script
2005	Fregly et al.	Multibody dynamic/ Elastic foundation simulation of TKR with creep	2.2×10^{-7}	ProMECHANICA Motion / Custom Script
2006	Penmetsa et al.	FEA of THR with creep	1.07×10^{-7}	ABAQUS / Custom Script
2006	Kang et al.	Simplified elasticity contact model of THR with standard and cross-linked UHMWPE	1.07×10^{-6} (standard) 2.00×10^{-7} (cross-linked)	Custom Script / Custom Script
2007	Lundberg et al.	FEA of THR with localized roughening and outlier duty cycles	1.07×10^{-6} where smooth	ABAQUS / Custom Script
2007	Rawlinson et al.	FEA of TDR with experimental calibration	1.98×10^{-6}	ABAQUS / Custom Matlab
2007	Hopkins et al.	FEA of total shoulder prosthetic	1.53×10^{-6}	PAMCRASH / None
2007	Fialho et al.	FEA of THR with heat effects	1.07×10^{-6}	ANSYS / None
2007	Knight et al.	Rigid body FEA of TKR and experimental verification	2.64×10^{-7}	ABAQUS / Python Scripting

Year	Author	Study Design	Wear Factor ($\text{mm}^3/(\text{N}\cdot\text{m})$)	Implementation of Contact / Geometry Updates
2007a	Sfantos and Aliabadi	Boundary Element Method (BEM) of THR	8×10^{-7}	Custom Script / Custom Script
2007b	Sfantos and Aliabadi	BEM of THR	1.5×10^{-6} to 1.8×10^{-6}	Custom Script / Custom Script
2008	Pal et al.	Probabilistic, Explicit, FEA of TKR	2.63×10^{-10}	ABAQUS / Not reported
2008	Liu et al.	FEM of Metal-on-Metal hip resurfacing prosthesis	1.13×10^{-8} (bedding-in phase) 1.2×10^{-9} (steady-state phase)	ABAQUS / Custom Script
2008	Zhao et al.	Multibody dynamic/ Elastic foundation simulation of TKR with creep	2.59×10^{-7}	ProMECHANICA Motion / C++ Script
2009	Kang et al.	Simplified elasticity contact model of THR with cross-shear	Dependent on cross-shear and contact pressure	Custom Script / Custom Script
2009	Willing et al.	FEA of TKR with creep and direction dependence	Dependent on sliding direction and creep	ANSYS/ Matlab Script
2009	Strickland and Taylor	Retrospective rigid-body analysis of wear tests in literature	Dependent on cross-shear	MSC.ADAMS/ None