FINITE ELEMENT ANALYSIS OF MOAT WALL POUNDING IN BASE-ISOLATED BUILDINGS

P. J. Hughes¹, A. Sarebanha², and G. Mosqueda³

ABSTRACT

Seismic isolation is an effective strategy to improve the performance of structures under ground shaking, but requires large displacement demands at the isolation level. Under extreme seismic loading conditions, there exists a potential for the superstructure base to exceed the provided clearance and impact the surrounding moat wall. Previous studies of this phenomenon were primarily analytical, using various simplified approaches to simulate pounding, but efforts to capture the impact force-displacement response using a high-fidelity finite element model are more limited. Furthermore, very few large-scale experiments have been conducted to support the numerical results. This paper summarizes a series of experiments performed at the University of Buffalo’s Structural Engineering and Earthquake Engineering Simulation Laboratory, as well as a simplified impact element used to capture the pounding force-displacement response. A 3-D finite element model of the experimental pounding setup is presented, and impact simulation results are compared with the experiment and previous numerical studies. Additionally, different levels of complexity for the finite element model are examined to study the effect on pounding response.

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Finite Element Analysis of Moat Wall Pounding in Base-Isolated Buildings

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ABSTRACT

Seismic isolation is an effective strategy to improve the performance of structures under ground shaking, but requires large displacement demands at the isolation level. Under extreme seismic loading conditions, there exists a potential for the superstructure base to exceed the provided clearance and impact the surrounding moat wall. Previous studies of this phenomenon were primarily analytical, using various simplified approaches to simulate pounding, but attempts to capture the impact force-displacement response using a high-fidelity finite element model are more limited. Furthermore, very few large-scale experiments have been conducted to support the numerical results. This paper summarizes a series of experiments performed at the University of Buffalo’s Structural Engineering and Earthquake Engineering Simulation Laboratory, as well as a simplified impact element used to capture the pounding force-displacement response. A 3-D finite element model of the experimental pounding setup is presented, and impact simulation results are compared with the experiment and previous numerical studies. Additionally, different levels of complexity for the finite element model are examined to study the effect on pounding response.

Introduction

Base isolation reduces floor acceleration and interstory drift demands on a building during an earthquake, but allows potentially excessive displacements at the isolation level [1]. If the shaking is strong enough, the base floor displacement may exceed the allowed clearance and impact the moat wall, inducing high-amplitude forces over a short time span.

The phenomenon of moat wall impact has been sparsely studied in the literature, with most impact studies focusing on fixed-base building-to-building impact [2,3,4] or pounding between bridge segments [5,6]. Studies that analyze moat wall pounding [7,8,9] rely on variations of simple impact elements [10,11] to generate the contact forces. While useful in producing overall impact forces, these so-called “macro” elements can overlook potentially important aspects of the pounding event, such as instantaneous contact area and spatial distribution of contact forces. Moreover, their accuracy depends on the assumed stiffness, mass, and damping of the system, which can vary significantly depending on the model employed [12].

High-fidelity finite element models of pounding, as opposed to the previously described macro models, seek to capture the entire physical picture of the impact event. The basis of these models is a detailed finite element mesh of each impacting structure, allowing for a complete
description of the instantaneous displacement field throughout the collision time history. Whereas the simplified models only capture overall force and displacement, full-field models must completely represent the spatial variation of these quantities. Detailed models also inherently include the contribution of higher-mode vibrations through element-wise distribution of mass and stiffness.

Experiments conducted at the University at Buffalo’s Structural Engineering and Earthquake Simulation Laboratory (UB-SEESL) in 2011 generated a wealth of data on the response of base-isolated buildings subject to earthquake-induced moat wall pounding [8]. The experimental results, including the local force-displacement response at the point of impact, were recreated in OpenSees [13] using a simplified impact element consisting of springs and dampers in series.

This paper contrasts the Masroor-Mosqueda macro model impact element [8] with a new, full-field finite element model constructed in LS-DYNA [14], and compares them both to the recorded experimental data. Additionally, the overall complexity of the new LS-DYNA model is varied to demonstrate its effect on important impact response variables such as dissipated energy, impulse, and coefficient of restitution.

Description of UB-SEESL Experiments

A single-bay, three-story, base-isolated intermediate moment frame (IMF) was tested at UB-SEESL to determine the structural response under earthquake-induced pounding. The structure was built at quarter scale to satisfy the limitations of the testing facilities at UB-SEESL. The IMF consists of structural S shapes, and is attached to a pin-supported gravity frame that provides the structural mass, as seen in Figs. 1 and 2.

The base floor weighs approximately 18.5 kips, and the upper floors each weigh 10.8 kips, for a total superstructure weight of 51 kips. System identification indicated that the natural periods corresponding to the first three translational modes are 0.66 sec (1.5 Hz), 0.18 sec (5.5 Hz), and 0.094 sec (11 Hz), respectively. The corresponding damping ratios are 5.6%, 2.2%, and 3.8%.

Figure 1. Scaled IMF with member sizes [1].
Four single friction pendulum (SFP) isolators were installed below the columns of the IMF, each having an inner radius of 30” and maximum displacement capacity of 8”. Under sinusoidal loading, the isolators reached a peak force of 10.2 kips at about 4” of lateral displacement, and exhibited an average friction coefficient of 0.08. A schematic of a typical SFP isolator and its hysteresis under sinusoidal loading are shown in Fig. 3.

A concrete moat wall with soil backfill and a wedge-shaped steel wall were tested during the 2011 experiments, with the analysis in this work restricted to the steel wall. The steel moat wall was connected to a supporting pedestal made of two W8x58 sections and thirty-six ½” lateral stiffener plates. Four long rods connected the wall to the pedestal, in addition to spot welds around its base, to create a near-rigid impact with the IMF base floor.

Concrete blocks were bolted to each end of the IMF base plate to create a contact surface like that of a mat foundation. Special impact load cells with a sampling frequency of 2,500 Hz
were installed behind the blocks, such that the normal contact force could be accurately measured throughout the entirety of the impact event. Each load cell is connected to the base plate using an L8×8×1/2 shape. Thus, the measured impact force is the force imposed by the block on the cylindrical load cells, and not the direct force from the concrete block onto the steel moat wall.

Analysis of ground motions in this work are limited to one simulation of the NS component of the 1992 Erzincan earthquake record, which was the only motion that produced moat wall impact in the experiments.

**Masroor-Mosqueda Impact Element**

The impact element proposed by Masroor and Mosqueda [8] considers two phases of impact: a local deformation phase where one object indents the other, and a vibration phase where the colliding bodies vibrate in unison according to the modal characteristics of the moat wall. Forces during the deformation phase are influenced by the relative stiffnesses of the colliding bodies, and tend to be larger in magnitude than forces in the vibration phase, which are controlled by the dynamic properties of the moat wall.

Local deformation behavior is considered through a single nonlinear spring that approximates a damped Hertz model [10,11]. In OpenSees, this is achieved with a bilinear curve using the uniaxial impact material [15], as shown in Fig. 4. The bilinear approximation does not necessarily capture the peak force or peak penetration of the damped Hertz model, but approximately satisfies equal hysteretic energy dissipation. Key inputs for this model are the Hertz stiffness $K_h = 8,000 \text{ kip/in}^{3/2}$, coefficient of restitution $e = 0.7$, maximum expected penetration $\delta_m = 0.025 \text{ in}$, and yield penetration $\delta_y = 0.0025 \text{ in}$ [8].

![Hertz damped model and OpenSees ImpactMaterial](image)

**Figure 4.** Hertz damped model and OpenSees ImpactMaterial.

Vibrational behavior is approximated using a damped single degree-of-freedom (SDOF) system consisting of the moat wall’s first modal mass $M_1 = 0.0010 \text{ kip} \cdot \text{s}^2/\text{in}$, first modal stiffness $K_1 = 100 \text{ kip/in}$, and impact damping ratio $\zeta_{imp} = 0.4$. Modal parameters $M_1$ and $K_1$ are computed from physical properties of the moat wall, and $\zeta_{imp}$ is determined from experiments. A schematic of the complete Masroor-Mosqueda impact element is shown in Fig. 5. The damping coefficient is computed as $C_{imp} = 2\zeta_{imp} \sqrt{K_1 M_1} = 0.26 \text{ kip} \cdot \text{s/in}$. 
High-fidelity Impact Model

Fig. 6 shows the LS-DYNA finite element model used to simulate moat wall impact, which consists of a 144” × 108” × 1.5” steel base plate, 48” × 14.5” × 6” concrete impact block, two L8×8×1/2 support angles with ten ½” stiffener plates, two 4.5”-diameter load cells, a 60” × 36” × 36” triangular steel moat wall composed of 2” plates, and a support pedestal made of two W8x58 shapes and thirty-six ½” stiffener plates. The model does not include the effect of upper stories or the SFP isolators. The base plate assumes the 18.5 kip weight of the bottom floor only. Each part is modeled using 0.5” cubic hexahedrons, for a total of 379,560 solid elements. The base plate is constrained in the vertical direction, and the plate beneath the support pedestal is fully fixed.

Figure 5.  Schematic of the Masroor-Mosqueda impact element [1].

Figure 6.  Detailed finite element model of base plate and moat wall created in LS-DYNA; (a) isometric view, (b) side view.
Component parts are connected by steel bolts modeled as beam elements, with rigid spider connections at their ends to simulate the clamping effect. Bolts are 1” in diameter for the wall base connection and ½” for all other connections. Flexural and transverse reinforcement in the concrete impact block is modeled explicitly with steel beam elements that have their translational degrees of freedom tied to the surrounding solid elements. A detailed view of the impact block’s reinforcement and bolt elements is shown in Fig. 7.

![Figure 7. Close-up view of impact block (a) with support angles and load cells, (b) with steel reinforcement, bolts, and rigid spider connections.](image)

All steel elements assume a bilinear hysteresis and kinematic hardening, with a Young’s modulus of 29,000 ksi, post-yielding modulus of 580 ksi, Poisson’s ratio of 0.3, and unit weight of 495 lb/ft³. The yield stress is 36 ksi for angles and plates (including the moat wall), 50 ksi for the W shapes, 150 ksi for bolts, and 60 ksi for reinforcing bars. The concrete block utilizes the Winfrith concrete material model [16], which is elastoplastic in compression, and elastic with post-peak softening in tension. The concrete model assumes a Young’s modulus of 3,800 ksi, uniaxial compressive strength of 5,000 psi, uniaxial tensile strength of 440 psi, Poisson’s ratio of 0.2, and unit weight of 150 lb/ft³.

Contact is modeled at all interfaces, including sliding between solid elements and bolt shaft friction. Fastener sliding assumes the static and dynamic coefficients of friction to be 0.75 and 0.6, respectively, while concrete-to-steel contacts assumes 0.6 and 0.4. Instantaneous contact boundary conditions are achieved using node-to-surface and surface-to-surface search algorithms coupled with the classical penalty method [17].

Simulations are carried out by applying an initial velocity to the base plate assembly equal to the pre-impact relative velocity between the base floor and the wall, as determined from the experimental data. For the single test considered here, the pre-impact base plate velocity was +36.4 in/sec and the wall velocity was -0.8 in/sec, yielding a relative impact velocity of 37.2 in/sec.

**Results**

Numerical results for one impact event are compared between the UB-SEESL experiments, the Masroor-Mosqueda impact element in OpenSees, and the high-fidelity LS-DYNA model. OpenSees results are computed using a complete representation of the experimental setup,
including the scaled IMF, SFP isolators, and moat wall impact element [8]. LS-DYNA results are computed using the model shown in Fig. 6, with no consideration of the superstructure or isolation system. Experimental and OpenSees results are extracted from a full earthquake time history simulation, whereas LS-DYNA results are obtained by applying the appropriate relative velocity to the base plate assembly. Impact force-time and force-displacement results are plotted in Fig. 8.

![Figure 8](image_url)

**Figure 8.** (a) Impact force time history and (b) impact hysteresis from experimental data (---), simplified OpenSees model (—), and high-fidelity LS-DYNA model (—).

Force-time variation in Fig. 8(a) shows that the LS-DYNA model overestimates the experimental peak force and underestimates the contact time by a factor of approximately three, whereas the force-displacement response seen in Fig. 8(b) indicates that the detailed model underestimates the peak displacement by about 0.1 in.

Clearly the simplified OpenSees model provides a better estimate of the experimental response compared to the LS-DYNA model in its current state in terms of peak forces and displacements. This can be attributed to the relatively higher stiffness of the LS-DYNA model, which has inter-component reactions and sophisticated boundary conditions that are not present in the OpenSees model. Furthermore, the impact response may be influenced by restoring forces from the superstructure or isolation system, which are not considered in the LS-DYNA model.

Interestingly, the LS-DYNA model exhibits a pounding impulse (area under force-time curve) of 2.56 kip-sec, which is relatively close to the experimental value of 2.99 kip-sec, and nearly identical to the OpenSees value of 2.56 kip-sec. The stiffer impact response seen in the LS-DYNA model induces higher contact forces, but also reduces the contact time. Thus, the impulse is generally independent of stiffness and duration, and is primarily controlled by the masses and velocities of the colliding bodies.

Still, the apparent coefficient of restitution (COR), computed as the ratio of post-impact relative velocity to pre-impact relative velocity is noticeably larger in the LS-DYNA model (0.78) than in the experiment or OpenSees model (0.49 and 0.53, respectively). Impulse, apparent COR, and other response quantities are summarized in Table 1. Impact stiffness is the slope of the line drawn between the origin and the point of maximum displacement on the force-displacement curve.
Table 1. Impact response parameters from experimental data, simplified OpenSees model, and high-fidelity LS-DYNA model.

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<tr>
<td>Experiment</td>
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<td>61</td>
<td>0.16</td>
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<td>LS-DYNA</td>
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<td>144</td>
<td>321</td>
<td>8.79</td>
<td>2.56</td>
<td>0.78</td>
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Four additional model cases are analyzed to study how the LS-DYNA model complexity affects the impact response. Case 1 includes only the impact block and fixed-base moat wall, and consecutive cases marginally increase the model complexity: case 2 adds block reinforcement and replaces the fixed moat wall base with a bolted connection to steel plates, case 3 adds the base plate with rigidly connected support angles, and case 4 changes all rigid connections to bolted connections. The LS-DYNA model with results shown in Fig. 8 is referred to as case 5, and is identical to case 4 with the addition of the support pedestal and all relevant bolted connections. Component masses are controlled such that the initial kinetic energy is the same for all cases. Isometric views of model cases 1-4 are shown in Fig. 9, and simulation results are shown in Fig. 10 and Table 2.
Table 2. Impact response parameters from five different LS-DYNA models.

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<tr>
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<td>8.79</td>
<td>2.56</td>
<td>0.78</td>
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Fig. 10 shows that impact forces increase and displacements decrease as more components are added to the finite element model. The exception to this trend is model case 3, which shows a stiffer response than case 2 due to the rigidly connected support angles. The model returns to lower stiffness in case 4, when all rigid connections are replaced with bolted connections. Overall response parameters in Table 2 confirm that the impulse and apparent COR remain relatively constant as the model stiffness changes. Table 2 also shows that, due to the trend of decreasing forces and increasing displacements, impact stiffness and energy dissipation decrease as more components are added to the model.

Conclusions

Moat wall impact experiments are summarized, along with the simplified impact element used to capture local contact forces. A new high-fidelity finite element model of the experimental setup is constructed in LS-DYNA and compared with previous experimental and numerical results. Impact simulation results indicate that the detailed model is overly stiff, with higher forces and smaller displacements than what is observed experimentally and in the simplified OpenSees model. Variation of model complexity indicates that the inclusion of more components decreases overall stiffness and drives numerical results towards the experiment. Discrepancies in the LS-DYNA model may be due to the absence of other experimental components, or the exclusion of the upper stories and isolator elements.

This work summarizes current progress in detailed finite element modeling of moat wall impact. Future work will adjust the LS-DYNA model such that numerical and experimental results are within an acceptable margin of error, explore the effect of modeling the superstructure and isolation system, and examine other types of moat walls.

Acknowledgments

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