Full-Scale Testing of a Viscoelastic Coupling Damper for High-Rise Building Applications and Comparative Evaluation of Different Numerical Models

Deepak R. Pant, Ph.D.1; Michael Montgomery, Ph.D.2; and Constantin Christopoulos, M.ASCE3

Abstract: Viscoelastic coupling dampers (VCDs) are used for the seismic and wind protection of tall buildings. In the past, several tests have been carried out on VCDs under severe loading conditions for concept validation purposes and different numerical models have been used for evaluating the performance of tall buildings with VCDs. Nonetheless, for practical applications understanding the performance of VCDs over a full range of demands including extremely small amplitudes of vibration, long-duration loading, and realistic well-defined design level seismic and wind events, as well as a comparative evaluation of different numerical models are needed. To address this research gap, a systematic study was carried out to understand the performance of VCDs based on full-scale tests and to assess the suitability of different numerical models in predicting their response. A range of displacement-controlled tests was carried out on a full-scale VCD specimen. Seismic loading was derived by considering both far-fault long-period long-duration ground motions as well as near-fault pulse-like ground motions that were developed based on a site-specific study and were scaled to represent the design earthquake (DE) and the risk-targeted maximum considered earthquake (MCEg) levels for a real project. Long-duration wind loading of 6-h with 1-, 10-, 50-, and 500-year mean recurrence intervals (per Canadian practice) developed based on wind tunnel testing of another real project were used. The temperature rise in the specimen during the tests was measured using high-precision thermocouples embedded in the viscoelastic (VE) layers as well as an external thermal camera. The test results indicated well-defined force-deformation hystereses of the specimen at all levels of strain amplitudes including those at extremely small deformation amplitudes up to 2.5 μm of deformation. The temperature rise of the specimen was less than 1°C and 4°C, respectively for the earthquake and wind loadings representative of the real projects that were considered in this study. This temperature rise was found to be lower when compared with previous generations of VE materials, which were tested under loading representative of shorter buildings with higher fundamental frequencies. Finally, the accuracy of four different macroscopic numerical models with different degrees of complexities in simulating the test results was investigated. Different numerical models were found to be suitable for different loading conditions and recommendations are provided for practical nonlinear modeling of tall buildings with VE dampers. DOI: 10.1061/(ASCE)ST.1943-541X.0002246. © 2018 American Society of Civil Engineers.

Author keywords: Full-scale testing; Numerical modeling; Viscoelastic damper; Wind and seismic loadings.

Introduction

Viscoelastic (VE) dampers have been used in wind and seismic protection of buildings for nearly five decades (Aiken et al. 1990; Soong and Dargush 1997; Housner et al. 1997; Christopoulos and Filipatrault 2006). The application of VE dampers to tall (greater than about 50 m in height) to megatall (greater than about 600 m in height) buildings is particularly appealing because of their ability to provide stiffness and energy dissipation to control drifts and forces from very small to very large deformations. Commercial VE dampers were first used in 1969 when more than 11,000 VE dampers manufactured by the 3M Company (referred to as 3M VE dampers) were installed in each of the two World Trade Center towers (100+ stories) in New York City. The 3M VE dampers have been used in more than 250 buildings including a number of tall buildings (Mahmood and Kee 1986; Mahmoodi et al. 1987; Samali and Kwok 1995; Montgomery et al. 2014). In the past, VE dampers have been installed to control seismic and wind vibrations primarily in bracing configurations or as wall panels in frame-type structures. These traditional VE damper configurations are less effective for use in modern tall buildings in which concrete core walls with or without outriggers constitute the main lateral load-resisting system. Nonetheless, because of the recent advances in VE damper technologies such as the viscoelastic coupling damper (VCD), it is now possible to apply VE dampers more effectively to modern tall buildings (MacKay-Lyons et al. 2012; Pant et al. 2017b). As a result, VE dampers are now being considered for some of the tallest buildings in the world for both high seismic and wind loading (Christopoulos et al. 2017). Therefore, understanding the response of VE dampers in the context of high-rise buildings has gained renewed attention.

Since the characteristics of the VE material play an important role in determining the effectiveness of VE dampers, a brief description of 3M VE materials used in structural applications is presented here. The ISD series of 3M VE materials are the most widely used VE materials for structural applications. ISD-109
was the first generation of the 3M VE material, which was first used in the World Trade Center towers in New York City in 1969. ISD-110 was the second generation of the VE material, which was used in a number of tall buildings including the 76-story Columbia Seafirst building in Seattle in 1982 and the 56-story Two Union Square building in Seattle in 1989. Dampers made of ISD-109 and ISD-110 3M VE materials required a bonding agent between the layers of VE materials (referred to as VE slabs) and the alternating steel plates, with special considerations to ensure the long-term performance of the bonding agent. The third and the fourth generations of the 3M VE materials, i.e., ISD-111 and ISD-111H, respectively, were developed based on the experience of testing and monitoring the first two generations of the VE materials. The ISD-111 and ISD-111H VE materials are self-adhering (i.e., they do not require a bonding agent like their predecessors) and the bond between the VE slabs and the steel plates is much stronger than the bond between the VE slabs themselves. In particular, the fourth generation of the VE material, i.e., ISD-111H, was specifically designed for high-rise building applications. This was achieved by making it stiffer and with higher damping compared to the ISD-111, and less temperature sensitive and more durable compared to all the previous generations of the 3M VE materials.

Previous experimental studies in the literature have mainly used earlier generations of the 3M VE materials, i.e., ISD-109, ISD-110, and ISD-111. Most of these studies report on component-level dynamic tests as well as shaking table tests of scaled building models with VE dampers under seismic loading conditions (e.g., Chang et al. 1991; Lin et al. 1991; Higgins et al. 1996; Asano et al. 2000; Vasques et al. 2010; Sasso et al. 2011; Zheng et al. 2015; Gong and Zhou 2017; Xu et al. 2016). On the other hand, for wind loading, previous studies have primarily relied on harmonic tests for durations lasting up to about 1 h (Mahmoodi and Keel 1986; Sato et al. 2007). While harmonic tests give a general idea of wind loading effects, they might not provide insights into realistic performance of dampers under real wind storms, which are random in nature, contain static as well as dynamic loading components, and can last for much longer than 1 h. Moreover, apart from the study of Asano et al. (2000), other studies have only used small-scale dampers for tests and only a handful of studies have considered the loading frequencies and protocols that are of interest in the analysis and design of tall buildings.

The ISD-111H dampers, on the other hand, have been a focus of only very recent studies. Daniel et al. (2014) conducted tests on a full-scale VCD made of ISD-111H under a variety of seismic and wind loadings representative of dampers configured in tall buildings. This study was focused on evaluating the performance of the dampers under extreme loading conditions by subjecting them to rather severe loading protocols that are not representative of real design targets. The work of Daniel et al. (2014) was a continuation of the earlier work by Christopoulos and Montgomery (2013) and Montgomery and Christopoulos (2015), where full-scale tests on VCDs were carried out primarily for concept validation purposes. None of these previous studies on VCDs have investigated the response of a full-scale specimen at very low deformation amplitudes (in the range of micrometers of deformations), which are relevant for service-level dynamic loading on buildings or under long-term realistic wind storms as well as project-specific seismic loading.

Self-heating of VE materials under external loading is an important parameter that affects their mechanical properties (Aprile et al. 1997; Housner et al. 1997; Gopalakrishna and Lai 1998; Guo et al. 2016; Kasai et al. 2017). Previous studies have paid little attention to the accuracy of capturing temperature rise in VE dampers during tests. Some of the previously mentioned studies have used thermocouples attached to the outside of the steel plates or to the VE layers and some studies have employed thermal cameras, which also measure the surface temperature of the VE layers as well as the steel plates. Ideally, thermocouples embedded in the VE layers are needed to get an accurate estimate of the VE temperature rise during tests. It is also not well established how this internal VE layer temperature differs from the surface temperature measurements.

While VCDs have previously been tested in a configuration similar to the one considered in this study, none of the previous studies considered (1) capturing the response of VCDs under very small deformations, which required careful selection of the loading machine as well the measuring instruments; (2) accuracy of the temperature measurements, which required use of thermocouples embedded in the VE layers and their comparison with the results from a thermal camera; and (3) realistic well-defined design-level loading protocols including six hours-long wind loadings as well site-specific seismic loading, which required significant prior analyses using three-dimensional numerical models of two real buildings.

Another important aspect is the numerical modeling of VE dampers, which is complex because the response of 3M VE dampers is dependent on frequency and temperature. Among these, simulating the effects of temperature rise due to self-heating is the most challenging. Different numerical models with different degrees of complexities, ranging from classical rheological models to fractional derivative models (FDMs) and coupled thermal-mechanical models, have been developed. Nonetheless, comparative studies on different available numerical models are quite limited. There have been several fundamental studies that have compared different numerical models from a viewpoint of capturing material-level frequency dependency (Park 2001; Lewandowski and Lasecka-Plura 2016). In addition, several studies have compared the seismic response of structures using different numerical models by considering frequency dependency (Singh and Chang 2009). Nonetheless, none of these studies were focused on both seismic and wind loading and they did not investigate coupled thermal-mechanical models. Daniel et al. (2014) provided some preliminary results of comparative studies considering simple coupled thermal-mechanical models, but since then more advanced numerical models have been developed.

This paper presents findings of a systematic study focused on the testing and numerical modeling of VCDs made of ISD-111H for tall building applications. Dynamic tests were carried out on a full-scale VCD specimen under realistic seismic and wind loading conditions. Based on two real recent projects, seismic loading and wind loading were obtained from a site-specific ground motion study and comprehensive wind tunnel testing, respectively, combined with three-dimensional numerical models of the buildings. The considered ground motions consisted of far-fault long-period long-duration ground motions as well as near-fault pulse-like ground motions. Unlike most previous studies, wind loading of up to 6 h in duration was considered. The specimen was also tested under very small levels of deformations representative of service-level dynamic loading, which is required to assess damping that is provided for mitigating vibration perception under service-level wind loading. The temperature rise in the specimen was accurately characterized using high-precision thermocouples embedded in the VE layers and the measurements were compared with the surface temperature measured using an external thermal camera. Finally, capabilities of different mechanical as well as coupled thermal-mechanical models in predicting the test results was assessed.
Test Program

Test Specimen and Setup

The VCD specimen consisted of two panels of nine layers (per panel) of 5-mm-thick, 560 × 430 mm 3M ISD-111H material bonded sequentially to steel plates, manufactured by Nippon Steel Engineering, Japan (Fig. 1). The specimen was a full-scale prototype of the VCDs designed for two tall RC buildings as discussed in more detail subsequently. Note, however, that the same damper can be used for any tall building. Usually a number of such dampers are installed in a real building. Type T thermocouples were embedded in each VE layer by the manufacturer. The 2,700-kN MTS (Eden Prairie, Minnesota) testing machine at the University of Toronto was used for the tests. Stiff steel plates were used to connect the specimen to the testing machine using 3.18 cm (1.25 in.). ASTM F3125 (ASTM 2015) high-strength bolts. Class B surface preparation was used on both sides of the connecting plates. The bolted connection was then pretensioned with a minimum pretensioning force of 316 kN per bolt following CSA S16 (CSA 2014) to achieve a slip-critical connection. The slip resistance of the assembly of 12 bolts along each connecting end computed as 3,288 kN per CISC (2017) was well above the maximum force of about 1,600 kN that was exerted to the specimen during the tests. The specimen was tested using a displacement-controlled loading protocol.

Instrumentation

Forces were measured using a load cell mounted on the MTS testing machine and deformations across the VE layers were measured using four LVDTs, two of which were mounted on each face of the specimen as shown in Fig. 1. The LVDTs used in this study were appropriate for the range of displacement amplitudes considered in this study, i.e., from 2.5 μm to 7.5 mm. LVDTs can record infinitesimal change in displacement, with their capability only being limited by the noise in the signal conditioner and the resolution of the output display (Sattler 2011). The minimum displacement that the MTS testing machine could apply was 1 μm. For the test results, average VCD deformation recorded using four LVDTs is reported. The Type T thermocouples embedded in the VE layer, which possess excellent readability, had an accuracy of ±1°C. Two different thermocouple card readers were used to record the temperature measured by the thermocouples from each of the VE panels. For reporting and analysis purposes, the temperature was taken as the average of the temperature recorded from all the thermocouples. Since four thermocouples out of the 18 malfunctioned, the average temperature reported here is the average of 14 thermocouples (seven embedded in each panel of the specimen). The defective thermocouples were detected as malfunctioning before the tests were started. Since the thermocouples were embedded in the VE layers, it was difficult to inspect and identify the cause of this malfunction. It is likely that the thermocouples may have been damaged during manufacturing. We did not investigate this issue further because we had 14 thermocouples that were correctly working to adequately capture the temperature rise in the specimen. In addition, the VE layer surface temperature was also frequently monitored using a FLIR ONE thermal camera set up on a tripod at a distance of 1.5 m from the specimen (distance of 1.5 m is approximate and is recommended by the manufacturer). Handheld thermocouples were also used but they were found to be very sensitive to human operation and were considered inappropriate for the tests.

Loading Protocols and Test Results

The specimen was subjected to a range of different loading protocols as discussed in more detail subsequently. In order to investigate changes in the properties of the VE material over the course of the entire testing program, which spanned over 1 month, at the

FIG. 1. Test setup and instrumentation of the test specimen. The thermal camera also used to monitor the temperature is not shown.
beginning of each test day the specimen was subjected to three cycles of sinusoidal displacements to approximately 50% peak shear strain (i.e., 2.5-mm peak displacement) at 0.15 Hz. These tests revealed essentially no changes in the properties of the VE material in terms of the storage modulus ($G'$) or the loss factor ($\eta$) over the course of the entire testing program, which confirmed the ability of the VE material to undergo multiple realistic loading conditions without any deterioration of its properties. The following paragraphs describe various tests and corresponding results.

**Harmonic Tests**

The specimen was first subjected to 10 cycles of sinusoidal displacements at various frequencies and strain amplitudes to characterize its properties. The tests were carried out at various frequencies, i.e., 0.075, 0.1, 0.15, 0.2, 0.3, and 0.5 Hz, which cover the range of first mode and higher mode frequencies for tall to megatall buildings (see, for example, Fig. 2). Four levels of peak input shear strains were used: 10%, 50%, 100%, and 150%. The actual strains computed using the LVDT measurements were slightly smaller than the input strains because of losses in the test system. The storage modulus and the loss factor obtained from the tests (computed using the second and fourth half cycles per standard practice) were found to be within 11% of the manufacturer-specified properties. The values of both the storage modulus and the loss factor were found to be within the production variability tolerances. The force-deformation hysteresis loops shown in Fig. 2 represent a typical response of VCDs, where stiffening of the VCDs at higher frequencies of loading is evident, highlighting the frequency dependency of the VE material.

**Small- to Large-Amplitude Tests**

In order to investigate the response of the VCD under very small to very large deformation amplitudes, harmonic tests were carried out at monotonically increasing peak strain amplitude levels, i.e., 0.05%, 0.1%, 0.2%, 0.3%, 0.5%, 1%, 2%, 5%, 10%, 20%, 30%, 50%, 100%, and 150%, at various frequencies, i.e., 0.05, 0.1, 0.15, 0.2, 0.3, and 0.5 Hz. Three cycles were applied at each strain level. A typical deformation time history is shown in Fig. 3. The test results presented in Fig. 4 show a well-defined hysteresis of the response even at extremely small strain amplitudes such as 0.05% strain (i.e., 2.5 $\mu$m of deformation). This distinguishes VE dampers that utilize solid damping from other types of dampers such as hysteretic dampers (e.g., steel yielding dampers or friction dampers) or fluid dampers, which may be ineffective at such small levels of deformations.

**Earthquake Tests**

In order to determine realistic earthquake loading protocols for the VCD specimen, a 40-story RC building, which was designed with the VCDs in Seattle, was considered. The building was designed and peer reviewed using the performance-based design following the Pacific Earthquake Engineering Research Center (PEER) tall buildings initiative (TBI) (PEER 2017) and LATBSDC (2017) guidelines (Pant et al. 2015). Soil-structure interaction (SSI) effects were not explicitly considered in the design of the building according to a common practice allowed by these guidelines, although the guidelines do encourage explicit modeling of SSI. First, nonlinear response history analyses (RHAs) of the building under seven spectrally matched design earthquake (DE)–level and the risk-targeted maximum considered earthquake (MCER)–level ground motions were carried out using a three-dimensional nonlinear model of the building with the VCDs in Perform-3D. For RHAs, VCDs were modeled using a generalized Maxwell model (GMM) discussed in more detail subsequently in the “Numerical Modeling” section to
Table 1. Earthquake ground motions used in the analysis of the 40-story building

<table>
<thead>
<tr>
<th>Earthquake number</th>
<th>Event</th>
<th>Year</th>
<th>Station</th>
<th>(M_w)</th>
<th>Closest distance (km)</th>
<th>Fault mechanism</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Chile</td>
<td>2010</td>
<td>CCSP</td>
<td>8.8</td>
<td>36.2</td>
<td>Interface subduction</td>
</tr>
<tr>
<td>2</td>
<td>Tohoku</td>
<td>2011</td>
<td>Ujje, TCGH12</td>
<td>9.0</td>
<td>103.8</td>
<td>Interface subduction</td>
</tr>
<tr>
<td>3</td>
<td>Tohoku</td>
<td>2011</td>
<td>Oyama, TCGG12</td>
<td>9.0</td>
<td>119.4</td>
<td>Interface subduction</td>
</tr>
<tr>
<td>4</td>
<td>Tokachi-Oki</td>
<td>2003</td>
<td>HDK084</td>
<td>8.3</td>
<td>146.8</td>
<td>Interface subduction</td>
</tr>
<tr>
<td>5</td>
<td>Tabas, Iran</td>
<td>1978</td>
<td>Tabas</td>
<td>7.35</td>
<td>2.1</td>
<td>Crustal; reverse subduction</td>
</tr>
<tr>
<td>6</td>
<td>Loma Prieta</td>
<td>1989</td>
<td>LGPC</td>
<td>6.93</td>
<td>3.9</td>
<td>Crustal; reverse oblique</td>
</tr>
<tr>
<td>7</td>
<td>Loma Prieta</td>
<td>1989</td>
<td>WVC</td>
<td>6.93</td>
<td>9.3</td>
<td>Crustal; reverse oblique</td>
</tr>
</tbody>
</table>

The ground motions and the building can be found elsewhere (Pant et al. 2015). Note, however, that due to limitations in the capacity of the damper force under this ground motion exceeds the 2,700-kN capacity of the machine.

Selected force-deformation hysteresis results from the earthquake loading protocols are shown in Fig. 5 and the time histories of the temperature rises are shown in Fig. 6. Near-fault ground motions clearly show a pulse-like response [Figs. 5(c and f)] and larger forces when compared with the far-fault ground motions [Figs. 5(a and b) and (d and e)], although peak deformation demands under near-fault motions are not always larger than those under the far-fault motions [compare Figs. 5(d and f)]. In contrast, the total energy dissipated under the far-fault motions (area under the force-deformation curve) is larger because of their longer duration. Unlike the distinctive differences in the peak forces for the near-fault and the far-fault ground motions, the peak temperature rises were similar for these two types of ground motions. Nonetheless, the variation of the temperature rise was quite different, where near-fault motions result in a sudden increase of the temperature as opposed to the far-fault motions where the temperature rise is more gradual. The far-fault motions also lead to nearly the same levels of peak temperature as the near-fault motions but at a much slower rate (Fig. 6). Overall, the maximum temperature rise was found to be less than 1°C for the DE- as well as MCE\(R\)-level ground motions, which is significantly smaller than what has been observed in the past for some other types of VE materials and loading conditions in low-rise building applications (Aprile et al. 1997). The lower temperature rises observed in this study are attributed to the reduced temperature sensitivity of the ISD-111H material used in this study as well as to the dominant frequency of excitation.

Fig. 5. Force-deformation hysteresis from earthquake tests: DE-level ground motions for (a) EQ #1, (b) EQ #3, and (c) EQ #7; and MCE\(R\)-level ground motions for (d) EQ #1, (e) EQ #3, and (f) EQ #6.
which is smaller for tall buildings when compared to shorter buildings. Since the temperature used in the analyses was nearly the same as the average starting VE temperature during the tests and since the temperature rise in the tests was very small (less than 1°C), which only changes the $G’$ and $\eta$ values by less than 5%, iterations between the tests and numerical analyses were not carried out. Ideally, the tests can be carried out in an iterative manner until the maximum temperature for each of the earthquake tests becomes exactly the same as the one used in the numerical analyses, but such an exercise is unlikely to change the test results much in this case.

**Wind Tests**

In order to determine realistic wind loading for the specimen, a 60-story RC building, which was designed with the VCDs in Toronto, was considered. RWDI, which was the wind engineering consultant on the project, provided wind loading time histories up the height of the building corresponding to 1-, 10-, 50-, and 500-year mean recurrence intervals (MRIs) of the wind storm based on a pressure test on the building in a wind tunnel [Fig. 7(a)]. The 500-year MRI is the basis of ultimate wind loading for this building in Canada, which is comparable to the 700-year MRI in the United States. Then fast nonlinear analyses (FNAs) of the building were carried out using a three-dimensional elastic model of the building with the VCDs in ETABS. For FNAs, VCDs were also modeled using a GMM whose parameters were calculated using an upper-bound temperature of 28°C. Then the deformation time histories of the VCD that sustained the maximum deformation under these wind storms in ETABS were used as input signals in the tests. The wind tunnel engineers provided time histories for approximately 1 h of the wind storm, which is typical for most applications in which mean hourly wind speeds are commonly used in design. In addition, the wind engineering consultant provided average scale factors that can be applied to this 1-h loading to generate a 12-h-long wind storm [Fig. 7(b)]. Past recorded wind storm data in Toronto in which recordings of several hours before and after storm peaks were used and the scale profile was generated based on statistical methods. Since the response of the VE dampers depends on the temperature rise due to self-heating, which in turn significantly depends on the duration of the loading, longer duration loadings were considered for testing the specimen. In order to generate this long-duration loading, the 1-h storm VCD deformation time histories were first obtained from the ETABS analyses and then extended to 12 h using the scale-factor profile shown in Fig. 7(b).

Representative force-deformation hystereses under wind loading are shown in Fig. 8. As expected, the force levels under the wind loading are notably smaller compared with those under the earthquake loading (Fig. 5). The hysteretic response of the VCDs under small as well as large levels of deformations was evident in these tests. The time histories of temperature rises are plotted in Fig. 9, where the maximum temperature rise under each of the 1-, 10-, 50-, and 500-year wind storms was 0.39°C, 0.97°C, 1.97°C, and 3.65°C, respectively. The temperature first increases...
Numerical Modeling

In general, the response of VE materials is frequency, temperature, and strain dependent. The temperature change in the VE material can occur due to ambient temperature fluctuations or internal temperature changes due to self-heating of the VE material under external loading. In most practical applications, VE dampers are located inside the building in a temperature-controlled environment and thus the effects of ambient temperature changes can be neglected in a numerical model. Accidental extreme variations in external temperatures are taken into account through a bounding analysis. Since strain does not have a significant influence on the properties of the ISD-111H VE material for the strain levels typically considered in design, the discussion herein is focused on capturing the frequency and temperature dependencies that characterize the response. This limited strain dependence especially at low strain amplitude levels is one of the main advantages of the 3M VE materials.

Mechanical models of VE dampers can be broadly classified into classical rheological models (also referred to as integer derivative models) and fractional derivative models (Lewandowski and Lasecka-Plura 2016). While the classical rheological models can be easily implemented in common structural analysis programs such as ETABS, Perform-3D, and OpenSees, the fractional derivative models are not available in these programs. This is because for most practical structural engineering applications, fractional derivative models are not required. In addition, as demonstrated in this paper subsequently, classical rheological models with more parameters can provide nearly identical results to those obtained using fractional derivative models with fewer parameters. Mechanical models with different numbers of parameters can be built, and if calibrated properly to the material-level test results, can simulate the frequency-dependent behavior of the VE dampers. Although mechanical models, via consideration of additional parameters, can be easily modified to simulate the temperature dependency through frequency-temperature equivalence (Ferry 1980), such modifications are also not available in common structural analysis programs. On the other hand, internal temperature change due to self-heating cannot be simulated using mechanical models, and accordingly numerical models that can estimate temperature rise in the VE material are needed. In order to capture the entire response of the VE dampers considering frequency as well as temperature dependency, a combination of the previously noted two numerical models referred to as coupled thermal-mechanical models are required. Unfortunately, such coupled thermal-mechanical models are also not available in common structural analysis programs. The discussion in this paper is focused on macroscopic models that can be efficiently used to assess the response of large structures such as high-rise buildings with many VE dampers. The following

Fig. 9. Temperature-rise time histories under wind storms with different return periods.

Fig. 10. (a) Temperature recordings obtained from thermal camera for the overall specimen; comparison of temperatures recorded from thermocouples embedded inside VE layers and surface temperatures measured by thermal camera for (b) 50-year; and (c) 500-year wind storms.
is a description of the previously mentioned models considering their capabilities in simulating the test results presented in this paper.

**Classical Rheological and Fractional Derivative Models**

Classical rheological models can be represented by a combination of springs and dashpots. These models are mathematically simple and can be constructed using simple elements in most finite-element programs. The Kelvin-Voigt model (KVM) represented by a spring and a dashpot in parallel and the Maxwell model represented by a spring and a dashpot in series are the most common classical rheological models. The KVM and the Maxwell model are not able to capture the frequency dependency of the VE material. In contrast, a GMM represented by a number of Maxwell elements in parallel can be used to capture the frequency dependency. A number of other variations of the classical rheological models can also be found in the literature [see, for example, Zhou et al. (2016) for a review of different models]. In this paper, a particular form of the GMM, in which a spring element is used in parallel with a number of Maxwell elements as shown in Fig. 11(a), was adopted (Fan GMM, in which a spring element is used in parallel with a number of Maxwell elements). In this paper, a particular form of the GMM is adopted. The Kelvin-Voigt model (KVM) represented by a spring and a dashpot in series are the most common classical rheological models. The parameters of the four Maxwell elements model for the ISD-111H VE material. Two frequency ranges, 0.05–0.5 Hz and 0.05–3 Hz, and three ambient temperatures, 20°C, 30°C, and 40°C, were considered. Since material-level characterization tests at a range of different frequencies were not carried out in this study, manufacturer-specified $G'$ and $\eta$ values were used. This is because the results of the representative characterization tests conducted in this study discussed previously were found to be similar to the manufacturer-specified properties. While the former frequency range (i.e., 0.1–0.5 Hz) can be used for seismic analysis of tall buildings, the latter frequency range (i.e., 0.05–3 Hz) is much wider and is more appropriate for wind analysis of tall buildings, where static as well as dynamic components of loading are involved. The parameters of the models are usually determined in the frequency domain by calibration to available test data at a range of frequencies. A number of methods have been used in the literature to determine the parameters of such numerical models (see Fan 1998; Park 2001). In this paper, the constrained optimization algorithm available in MATLAB 8.5 was used. The calibration errors reported as the maximum of the errors in the prediction of $G'$ and $\eta$ for different numbers of Maxwell elements are plotted in Fig. 11(b) for 30°C. Nearly identical results were obtained for 20°C and 40°C as well. It is clear from Fig. 11(b) that with the increasing number of Maxwell elements, the calibration error exponentially reduces and then becomes nearly constant with further increase in the number of Maxwell elements. Fig. 11(b) also shows that while at least four Maxwell elements might be needed to accurately simulate the response of the VE dampers under the wider frequency range (i.e., 0.05–3 Hz), as few as two Maxwell elements could be sufficient for the shorter frequency range (i.e., 0.1–0.5 Hz). The sample calibration results shown in Fig. 12 show an excellent correlation between the test and the predicted values of the parameters using four Maxwell elements. The parameters of the four Maxwell elements model for various temperatures are shown in Table 2.

Unlike the GMM, the FDM is mathematically more complex. Although fractional derivative models with a number of parameters can be built, a 4-parameter model represented a good balance between accuracy and computational efficiency. In subsequent sections of this paper, the results from this 4-parameter model are compared with the results from the GMM with four Maxwell elements. The stress-strain ($\tau$-$\gamma$) relationship of the FDM at any time step $t$ during the analysis is given as

$$\tau(t) + aD^\alpha \tau(t) = G(\gamma(t) + bD^\alpha(\gamma(t)))$$  \hspace{1cm} (1)

where $a$ and $b = \text{dimensionless parameters; } G = \text{elastic parameter; } 0 < \alpha < 1$ is the fractional derivative order; and $D^\alpha = \text{fractional derivative operator.}$ The model converges to an integer derivative model for $\alpha = 1$. The fractional derivatives can be calculated using different definitions such as the Riemann-Liouville definition or the Grunwald-Letnikov definition. The latter definition was adopted in this study and for any function $f$, i.e., $\tau$ or $\gamma$, the fractional derivative using this definition can be numerically evaluated at the $i$th step of analysis as

$$\left(D^\alpha f_{i}\right) = \frac{1}{\Delta t^\alpha} \sum_{m=0}^{N} (-1)^m \frac{\Gamma(\alpha + 1)}{m!(\alpha - m + 1)} f_{i-m} \hspace{1cm} (2)$$

![Fig. 11](a) GMM; and (b) influence of number of Maxwell elements in the numerical model on the calibration error for the ISD-111H at 30°C.

![Fig. 12](a) Calibration results for the GMM using four Maxwell elements for a frequency range of 0.05–3 Hz at 30°C.
Table 2. Calibrated parameters for the GMM using four Maxwell elements for 0.05–3 Hz

<table>
<thead>
<tr>
<th>Parameter</th>
<th>θ = 20°C</th>
<th>θ = 30°C</th>
<th>θ = 40°C</th>
</tr>
</thead>
<tbody>
<tr>
<td>K_F (kN/mm)</td>
<td>0.00</td>
<td>1.7</td>
<td>0.00</td>
</tr>
<tr>
<td>K_1 (kN/mm)</td>
<td>89.7</td>
<td>39.9</td>
<td>52.3</td>
</tr>
<tr>
<td>C_1 (kN s/mm)</td>
<td>70.7</td>
<td>36.9</td>
<td>7.6</td>
</tr>
<tr>
<td>K_2 (kN/mm)</td>
<td>282.6</td>
<td>107.6</td>
<td>20.6</td>
</tr>
<tr>
<td>C_2 (kN s/mm)</td>
<td>33.0</td>
<td>15.4</td>
<td>20.2</td>
</tr>
<tr>
<td>K_3 (kN/mm)</td>
<td>3,006.9</td>
<td>905.9</td>
<td>399.5</td>
</tr>
<tr>
<td>C_3 (kN s/mm)</td>
<td>25.8</td>
<td>12.1</td>
<td>5.4</td>
</tr>
<tr>
<td>K_4 (kN/mm)</td>
<td>88.6</td>
<td>54.1</td>
<td>39.7</td>
</tr>
<tr>
<td>C_4 (kN s/mm)</td>
<td>924.0</td>
<td>804.6</td>
<td>663.8</td>
</tr>
</tbody>
</table>

where N = windowing parameter that defines the number of previous analysis results to recall; Δt = analysis time step; and Γ is the gamma function. The windowing parameter has an influence on the analysis results as well as the computational efficiency of the model. A value of N ≥ T/(2πΔt) has been recommended (Koh and Kelly 1990), where T is the period of vibration of the structure. In the presented study, T = 10 s was used because it represents a period much longer than the period of the buildings considered herein. The value of N was chosen based on a sensitivity study to achieve a balance between accuracy and speed of the analysis and it varied from the previously recommended minimum value up to 40 times that value. In the present study, the parameters of the FDM were also calibrated using the constrained optimization algorithm in MATLAB. For example, calibration at 30°C for the frequency range of 0.05–3 Hz resulted in a = 0, b = 2.29, α = 0.54, and G = 34.81 kN/m². Similarly, the calibration parameters were also obtained for 20°C and 40°C but are not presented here for brevity. Also, the calibration results were similar to the ones shown in Fig. 11(b). Unlike the GMM, the possibility of using more than four parameters for the FDM was not investigated because the four parameters provided sufficient accuracy for the simulations.

Numerical analyses were carried out using the GMM with four Maxwell elements and the 4-parameter fractional derivative model. OpenSees was used for the analyses using the GMM, while an in-house MATLAB program was used for the FDM because it is not available in OpenSees. The numerical analyses results for only the earthquake and wind loadings that are actually of interest in the simulation of high-rise buildings are discussed in this paper. The parameters of the models were calibrated to the starting temperature recorded during each of the tests. Table 3 shows the difference between the analysis and test results for all the numerical models considered in this paper in terms of the area under the force-deformation curve, which is a measure of the energy dissipation, absolute value of the peak force, and peak temperature. A negative difference in Table 3 means that the numerical model underestimates the test results and vice versa. The plots are presented in terms of the force and temperature time histories of the damper. The test damper deformation time history was used as the input in the numerical models.

The test and analysis results for the DE-level EQ #4, which had the smallest temperature rise during the tests, and the MCEq-level EQ #1, which had the highest temperature rise during the tests, are plotted in Fig. 13. It is clear from this figure as well as Table 3 that for the seismic loading both the GMM and the FDM provide similar results and both of these models simulate the test results very well, with the maximum differences between the test and analysis results being less than 15%. There was no clear trend that indicated that the FDM was superior to the GMM or vice versa. Since the temperature rise during these tests was small (Fig. 6), temperature-dependent effects that were not included in these models did not make much of a difference in the predictions. This is because the maximum temperature rise during the earthquake tests was 0.84°C, which leads to less than 5% reduction in the value of the storage modulus G′ as well as the loss factor η compared with G′ and η at any given temperature. For wind loading, on the other hand, both of these models also produce nearly similar results (Fig. 14 and Table 3), but they simulate the test results well only for the smaller intensity wind loading, i.e., the 1-year wind storm [Figs. 14(a and b)] and the 10-year wind storm, where the maximum difference between the test and analysis results is less than 10%. This can also be attributed to the small temperature rises observed for these loadings. For the higher intensity wind loadings, i.e., the 50-year wind storm, the GMM and the FDM overestimate the energy dissipation with a maximum difference of less than 20%. For the highest intensity wind storm considered in this study, i.e., the 500-year wind storm [Fig. 14(b)], the GMM and the FDM both grossly overestimate the energy dissipation as well as the force demands on the damper. This is because the stiffness

Table 3. Percent difference between analysis and test results

<table>
<thead>
<tr>
<th>Motion</th>
<th>Earthquake number</th>
<th>Difference in area under force-deformation curve</th>
<th>Difference in peak force</th>
<th>Difference in peak temperature</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>GMM</td>
<td>FDM</td>
<td>MT1</td>
<td>MT2</td>
</tr>
<tr>
<td>DE</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
<tr>
<td>MCEq</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
<tr>
<td>1-year wind</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
<tr>
<td>10-year wind</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
<tr>
<td>50-year wind</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
<tr>
<td>500-year wind</td>
<td>5%</td>
<td>6%</td>
<td>7%</td>
<td>8%</td>
</tr>
</tbody>
</table>

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of the specimen, which becomes smaller with self-heating in the damper, is overestimated by these models, which do not consider self-heating.

**Temperature-Frequency Equivalence**

The previously referenced classical rheological and fractional derivative models can be modified to take into account the temperature dependency through temperature-frequency equivalency, according to which properties of most VE materials at any temperature and frequency can be correlated to their properties at a different reference temperature and a different frequency (Ferry 1980). Using this equivalence, a temperature shift function can be calculated as

\[ s = e^{-(\theta - \theta_{ref})/(p_2 + \theta - \theta_{ref})} \]  

where \( p_1 \) and \( p_2 \) = dimensionless coefficients; \( \theta \) = current temperature; and \( \theta_{ref} \) = reference temperature. In the frequency domain, the temperature dependency can be included by simply multiplying

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**Fig. 13.** Test and analysis force-time histories for the earthquake loading cases using the GMM and the FDM: (a and b) DE-level EQ #4; and (c and d) MCER-level EQ #1.

**Fig. 14.** Test and analysis force-time histories for the wind loading cases using the GMM and the FDM: (a and b) 1-year wind storm; and (c and d) 500-year wind storm.
the frequency $\omega$ by the shift function $s$. Thus, the calibration problem to determine the parameters of the model now includes three additional parameters $p_1$, $p_2$, and $\theta_{ref}$. Given the parameters of the GMM or the FDM at the reference temperature, the parameters at any time step during the analysis can be easily calculated if the temperature at that time step is known.

**Coupled Thermal-Mechanical Models**

Coupled thermal-mechanical models can be defined by combining either the GMM or the FDM (including the temperature shift function) with a numerical model that calculates temperature rise in the VE damper. Since the GMM and the FDM are shown to provide similar results, the discussion presented in the following sections is focused on the 4-parameter FDM.

Although different thermal-mechanical models exist (see, for example, Aprile et al. 1997; Gopalakrishna and Lai 1998; Guo et al. 2016; Kasai et al. 2017), the present study was focused on the evaluation of two thermal-mechanical models. The first model, referred to as MT1, was based on the classical heat transfer equation, neglecting the conduction from the VE layers to the steel plates and the convection from the damper to the surrounding air. In this model, the temperature rise using the FDM at an analysis time step $t$ is given as

$$\Delta \theta(t) = \frac{\tau(t) \Delta \gamma(t)}{(\rho c)_V}$$  \hspace{1cm} (4)

where $\Delta \gamma(t) = \gamma(t + 1) - \gamma(t)$, and $(\rho c)_V$ is the product of the density $\rho$ and the specific heat $c$ of the VE material. Once the temperature rise is known, the temperature as well as frequency-dependent properties of VE dampers can be simulated. The model MT1 has been used by a number of researchers (e.g., Aprile et al. 1997; Lai et al. 1999; Kasai et al. 2017).

The second model, referred to as MT2, is the new thermal-mechanical model developed by Guo et al. (2016). The model of Guo et al. (2016) uses (1) two-dimensional heat propagation and (2) a discretized model for the thermal part and a simplified single degree of freedom model for the mechanical part, as opposed to the model proposed by Kasai et al. (2017), which uses (1) one-dimensional heat propagation and (2) a discretized model for both thermal as well as mechanical parts. The MT2 model considered in this study used a finite-volume model (FVM), where the temperature at any location during a time step of analysis is calculated using the classical heat transfer equation considering conduction in the steel. Heat convection, however, is also not considered in this model. In addition to the product of the density and the specific heat of the steel, i.e., $(\rho c)_{steel}$, thermal conductivities of the VE material $k_V$ as well as that of the steel $k_{steel}$ are needed in this model. In both of these models, i.e., MT1 and MT2, the mechanical part of the models is calibrated to the manufacturer-specified material-level test data, while the thermal part does not require any calibration.

In-house MATLAB programs were used to evaluate the effectiveness of MT1 and MT2 in predicting the test results. For calibration purposes, manufacturer-specified properties at 20°C, 30°C, and 40°C in the frequency range of 0.05–3 Hz were used. The calibrated parameters of the 4-parameter FDM were as follows: $a = 0$, $b = 4.69$, $c = 0.54$, $G = 32.68$ kN/m², $p_1 = 8.71$, $p_2 = 50.49$, and $\theta_{ref} = 21.37$°C. The material properties were as follows: $(\rho c)_V = 1.949,3$ kN/(m²°C), $k_V = 0.188$ N/(s°C), $(\rho c)_{steel} = 3,637,1$ kN/(m²°C), and $k_{steel} = 43.13$ N/(s°C).

The force-time histories obtained using these two models are compared with the test results in Fig. 15 for the earthquake loading that caused the highest temperature rise in the tests (i.e., MCE$_R$-level EQ #1). The time histories of the temperature rise for selected earthquake loading are shown in Fig. 16. Although time histories of temperature rises are plotted in this paper, Table 3...

![Fig. 15. Test and analysis force-time histories for MCE$_R$-level EQ #1 obtained using the two coupled thermal-mechanical models.](image)

![Fig. 16. Test and analysis time histories of the temperature rise for the MCE$_R$-level ground motions using thermal-mechanical models: (a) EQ #1; (b) EQ #3; and (c) EQ #5.](image)
presents differences between the analysis and test results in terms of the actual temperature. MT1 simulates the test results well (Table 3), although it underestimates the energy dissipation as well as the peak force and overestimates the peak temperature; however, the difference between the test and analyses results is less than 15%. MT2, on the other hand, provides better predictions of the temperature rise as well as the energy dissipation and the peak force for all the loading cases (Fig. 16 and Table 3). The test and analyses results for the wind loading with the highest temperature rise, i.e., the 500-year wind storm, are plotted in Fig. 17 and the time histories of the temperature rise for various storms are plotted in Fig. 18. MT1 does a reasonable job in predicting the response for the 1-year storm but performs poorly in predicting the response and temperature rises under higher intensity wind storms [see, for example, Figs. 18(b and c)], with the maximum overestimation being greater than up to 50% (Table 3). MT2, on the other hand, leads to analysis results that are within 5% of the test results (Figs. 17 and 18; and Table 3).

**Summary and Recommendations for Numerical Modeling**

In summary, based on the results of this study, for the structures and loading conditions that were considered, the GMM with four Maxwell elements is sufficient for the analysis of tall buildings under a wide range of frequencies for wind as well as seismic loadings. The GMM with two Maxwell elements can be used for seismic analysis of most tall buildings without the loss of significant accuracy. The GMM with four Maxwell elements and the 4-parameter FDM provide nearly similar results. The parameters of the GMM and FDM provided in this paper can be used for analysis of high-rise buildings (greater than about 50 m in height) and do not have to be determined on a case-by-case basis. For low-rise buildings, it might be useful to recalibrate the parameters of these models. For tall building applications, for earthquake loading (DE and MCEq) and wind loading of shorter intensities such as the 1-year wind storms considered in this study, the GMM and FDM as well as the MT1 simulate the response accurately in terms of the time histories of the response, energy dissipation, peak force, and peak temperature, with the difference being within 15% compared with the test results. For higher intensity wind loading such as the 10-year wind storm, the use of MT1 is not recommended, while the GMM and FDM still provide very good predictions of the test results, with the difference compared with the test results being within 5%. Thus, analyses for such loading conditions can be carried out in commonly used structural analyses programs. On the other hand, for very-high-intensity wind loading, such as the 50-year storms and the 500-year storms, special thermal-mechanical models (i.e., MT2) should be used because they provide analysis results within 5% of the test results for these loadings. Therefore, the thermal-mechanical models such as MT2 should be implemented in common structural analysis programs.

**Conclusions**

Dynamic tests were carried out on a full-scale VCD specimen under realistic seismic and wind loadings that correspond to distinct design targets for two real tall buildings. The considered ground motions consisted of far-fault long-period long-duration ground motions as well as near-fault pulse-like ground motions. Unlike most previous studies, wind loading of up to 6 h in duration was considered to capture a more realistic self-heating response of the damper.
The test results indicated well-defined force-deformation hystereses of the specimen at all the levels of strain amplitudes including those at extremely small deformation amplitudes such as 2.5 μm of deformations. The near-fault motions led to larger peak forces in the damper compared with the far-fault motions, but the peak temperature rises were similar for these two types of ground motions. Overall, the maximum temperature rise was found to be less than 1°C for the DE- as well as the MCEg-level motions considered in this study. For wind loading, the temperature first increases with the increasing duration and then becomes constant with further increase in the duration. The maximum temperature rise during the wind loading was less than 4°C.

Capabilities of four different numerical models with increasing complexity in simulating the test results were investigated: (1) a GMM, (2) an FDM, (3) a thermal-mechanical model that neglected heat conduction (MT1), and (4) an advanced thermal-mechanical model that simulated heat conduction (MT2). For earthquake loading (DE and MCEg) and wind loading of smaller intensities, i.e., the 1- and 10-year wind storms considered in this study, the GMM and FDM simulated the test results very well. MT1 was found to be appropriate for earthquake loading as well as 1-year wind storms. On the other hand, only MT2 was able to accurately simulate the test results for the 50- and 500-year wind storms.

References
Asano, M., H. Masahiko, and M. Yamamoto. 2000. “Simulate the test results for the 50- and 500-year wind storms. On the other hand, only MT2 was able to accurately simulate the test results for the 50- and 500-year wind storms.


