On the Wind-Induced Response of Tall Buildings: The Effect of Uncertainties in Dynamic Properties and Human Comfort Thresholds

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ABSTRACT

Modern buildings with low inherent damping become dynamically sensitive to wind actions, which potentially increase their acceleration levels under even moderate wind speeds. As a result, issues of occupant comfort have become increasingly concerning for tall building designers and researchers, prompting a number of studies to determine acceptable levels of acceleration. Due to a number of factors, including the variability of individual responses to building motion, the definition of an acceptable habitability limit state is still largely debated, as discussed in this study. Considering the importance of damping in meeting these perception criteria, this study also explores the issues of amplitude-dependence and uncertainty in damping, with comparisons to recently collected full-scale data. In light of the uncertainties in both the occupant comfort criteria and damping value, and in the design wind speeds and other related parameters, a probabilistic framework is then introduced to evaluate a building's habitability performance at a variety of wind speeds.

KEY WORDS: Damping, uncertainty, dynamic response, tall buildings, full-scale, reliability, design wind speeds

INTRODUCTION

In order to limit the response of tall buildings under the action of wind, lateral stiffness may be increased, which in turn will decrease the amplitude of the displacements, though it may not significantly diminish accelerations. However, as accelerations are considered the stimulus for motion perception by occupants, increasing stiffness alone may not be sufficient to insure that the structure satisfies serviceability and habitability criteria, the latter often governing tall building design. By increasing the level of inherent damping, the acceleration response of the building will be decreased, making it a structural property critical to meeting habitability criteria. Unfortunately, inherent damping cannot be determined with a high degree of certainty in design [1] and cannot be predictably engineered in a structure like mass and stiffness, since its mechanisms are complex and, as of yet, not fully understood.

Although there have been some efforts to develop predictive tools for damping estimation based upon values measured in full-scale, there is considerable scatter in the data, as well as a lack of any information for buildings of significant height for which resonant response components dominate. Thus, rather generic damping values are assumed, resulting in designs that may satisfy habitability criteria on paper, but not necessarily in service. The scatter in current full-scale databases can be attributed to two factors: (1) the difficulty in estimating damping from ambient vibration data and (2) its dependence on the amplitude of motion [2]. Therefore, not only is there a definitive need for more full-scale damping estimates for tall buildings, but those estimates must also account for potential amplitude dependence. In this paper, amplitude-dependent damping values are estimated from full-scale data collected in the Chicago Full-Scale Monitoring Program [3] and are compared to existing predictive models, whose sensitivities are evaluated by a parameter study.

As damping and ensuing accelerations are not the only quantities surrounded by uncertainty, this study will also consider the uncertainty inherent in the habitability criterion itself, arising from its subjective nature and a number of additional factors that contribute to perception of motion [4, 5]. This study discusses a number of habitability criteria that have been proposed in recent decades based on full-sale studies (e.g., [6]) and controlled testing and emphasizes the importance of uncertainty in modeling human biodynamical response to motion in light of available information from these tests. Subsequently, a probabilistic framework is proposed so that uncertainty in damping and human sensitivity to motion, as well as other variables contributing to wind-induced response, can be propagated to assess the probability of failure for the habitability limit state. An example is presented to demonstrate the analysis framework and its utility in the performance evaluation process.

The organization of this paper is as follows: first, the methodology used to determine the wind-induced response is provided, followed by a discussion of models presently used for damping estimation, with comparisons to recently-collected full-scale data and a parameter study. A review of various occupant comfort criteria is then provided. Finally, the aforementioned analysis framework is introduced to assess the probability of failure in the habitability limit state.

WIND-INDUCED RESPONSE

In order to derive the structural response from wind loads, basic random vibration theory is utilized. In general, the equations of motion are derived to provide two translations and one rotation per story level; however, for the sake of illustration, it is assumed here that the structure is uncoupled in each direction. The root mean square (rms) value of the response in physical coordinates, σ_x^2, can be expressed as:

$$\sigma_x^2 = \frac{\phi^2 \pi f_n S_P(f_n)(2\pi f_n)^2}{4(2\pi f_n)^4 \xi m^2} + \frac{\phi^2 \int_0^{f_n} S_P(f)df\,(2\pi f_n)^2}{(2\pi f_n)^4 m^2} \tag{1}$$

where f_n is the natural frequency, ξ is the damping ratio, ϕ is the mode shape, m is the modal mass, $S_P(f) = \{\phi\}^T[S_F(f)]\{\phi\}$ and $S_F(f)$ is the power spectral density (PSD) of the wind load obtained from a wind tunnel study. The first term in Equation (1) represents the resonant response component, while the second term is the background component [7]. This equation is an approximation of the area under the response PSD, which is very close to exact for most lightly damped structures and can be modified to include the influence of higher modes. In addition, formulae for the derivatives of response, $x^{(r)}$, are derived in [7] where r = 0,1,2,3 denotes displacement, velocity, acceleration and jerk. The preceding equation can be evaluated in closed-form using the analysis framework presented, e.g., in ASCE 7 [7]. A critical unknown parameter is the loading, which can, at present, only be estimated accurately with wind tunnel tests. These mode generalized loads can be obtained utilizing high-frequency base balance (HFBB) or synchronously monitored surface pressure measurements on scaled building models.

Alternatively, the peak along-wind acceleration at any height of the building can be found using the following equations from the NatHaz Aerodynamic Loads Database (http://aerodata.ce.nd.edu/):

$$\hat{\ddot{x}}(z) = \frac{\int_0^H \hat{P}_R(z)\phi(z)dz}{\int_0^H m(z)\phi^2(z)dz}\phi(z) \tag{2}$$

$$\hat{P}_R(z) = \hat{M}_R \frac{m(z)\phi(z)}{\int_0^H m(z)\phi(z)z\,dz} \tag{3}$$

$$\hat{M}_R = g_R\sqrt{\frac{\pi}{4\xi} f S_M(f)} = g_R \sigma_{C_M} \overline{M}'\sqrt{\frac{\pi}{4\xi}C_M(f)} \tag{4}$$

where $\hat{P}_R$ is the resonant component of equivalent static wind loading, $\hat{M}_R$ is the resonant base bending moment, $\overline{M}'$ is the reference moment equal to $\frac{1}{2}\rho \overline{U}_H^2 BH^2$ in which ρ is the air density, B is the width of the building, H is its height, $\overline{U}_H$ is the gradient wind speed, and the parameters σ_{C_M} and C_M are related to the spectra of the aerodynamic base moments and obtained from the website [8]. It is also possible to evaluate these equations by employing the web-based on-line module [9]. The approach using the Aerodynamic Loads Database will be utilized in the uncertainty analysis discussed later.

AMPLITUDE-DEPENDENT DAMPING

Given the significant role structural damping plays in the wind-induced response of tall buildings, the accurate prediction of damping levels in the design stage is essential to insuring that designs satisfy occupant comfort criteria. As a result, there has been significant effort in developing predictive models for damping based on full-scale data. Some of the amplitude-dependent damping models are those defined in [2], [10], and [11]. In the subsequent discussions, these models will be referred to as Model A, Model B, and Model C for simplicity. The three damping models for steel buildings are defined in the literature as:

$$\text{Model A:} \quad \xi = 0.01 f + 10^{\sqrt{D}/2} \frac{x_H}{H} \tag{5}$$

$$\text{Model B:} \quad \xi = \frac{\beta_1}{f} + \beta_2 f + \frac{\beta_3}{\lambda}\left(\frac{x_H}{H}\right) \tag{6}$$

$$\text{Model C:} \quad \xi = 0.013 f + 400 \frac{x_H}{H} + 0.0029 \tag{7}$$

where x_H is the generalized amplitude, H is the height, D is the depth, f is the natural frequency, $\lambda = \frac{H}{D}$, and β_1, β_2, β_3 are model parameters. In addition to the linear model described above, a variation of Model A has been introduced, which is the same as the one described by Ellis [12], with low and high amplitude plateaus [13]. For a steel building under Model B, β_1, and β_2 have been determined as 0.00319 and

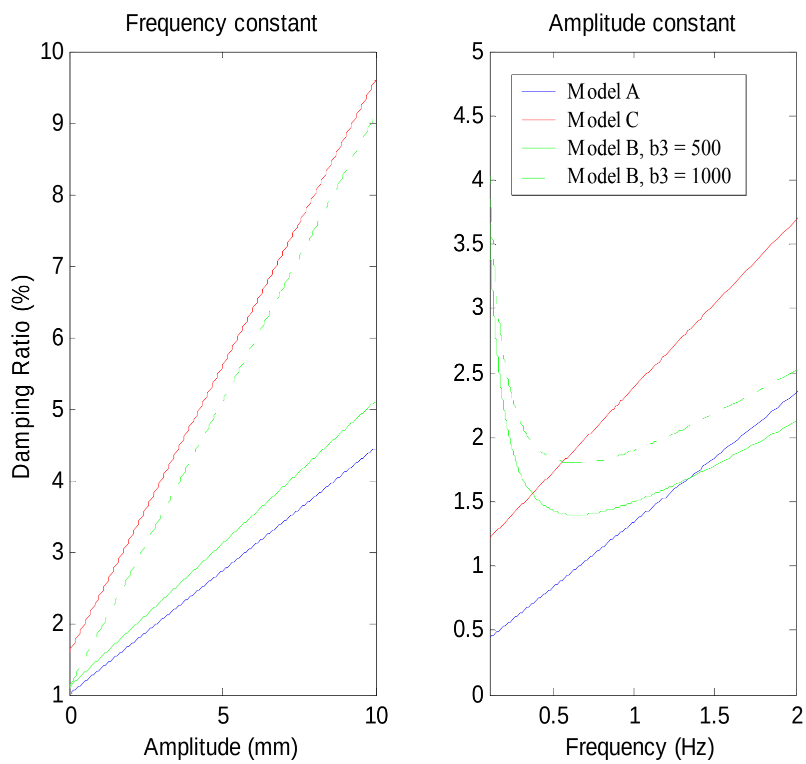


Figure 1 *Comparison of damping models*

0.00781, respectively [10]. The parameter β_3 depends on the structural system and is often included in an error term (ε_ξ) in the literature [10, 14]. Model C, which is similar in mathematical form to Model A, was derived from a systematic study involving full-scale measurements [11]. It should be noted that the three models were developed using databases that were primarily composed of mid-rise to moderately tall buildings.

Figure 1 compares the three damping models for a building with properties typical of the structures used in the databases. First, the frequency was held constant and the response amplitude was varied, as shown in the figure on the left. In Model B, the parameter β_3 can modify the slope and thus affect the predicted damping drastically, as demonstrated in Figure 1. For these typical building properties, Model C yields damping ratios that are higher than Model A. However, since these models are parameter-dependent, using different building properties will yield different results. This may not be significant for Models B and C, but, in the case of the building depth, will have significant effect on the predictions of Model A. Thus, the use of an upper limit or high amplitude plateau becomes necessary for practical applications.

In Figure 1, the graph on the right represents the effect of frequency on the predicted damping ratio, as described by the different models. The significant difference between Model B with Models A and C is the $1/f$ term, which results in a singularity at $f = 0$ Hz. Since tall buildings have frequencies in the range of 0.1 to 0.5 Hz, Model B will actually predict larger damping values for these buildings than for low or mid-rise buildings. Though this seems counterintuitive, it should be reiterated that these models were calibrated on data sets that did not include any values of damping for significantly tall buildings. Thus, this analysis represents an extrapolation of these models to this class of tall buildings. For this reason, the accuracy of these models in predicting the damping of an actual building is further analyzed in the following section through a comparison with the full-scale damping estimates obtained from the Chicago Full-Scale Monitoring Program.

Finally, it should be noted that, despite the evidence of amplitude dependent damping from full-scale measurements, current practice in North America assumes a constant level of damping, usually 1% for tall steel buildings, as described in ASCE 7-05 [15]. In some cases, the effect of amplitude-dependence is considered by retaining the 1% value for serviceability/habitability design (10-year event) and increasing the damping value to 1.5% for the survivability design (50-year event).

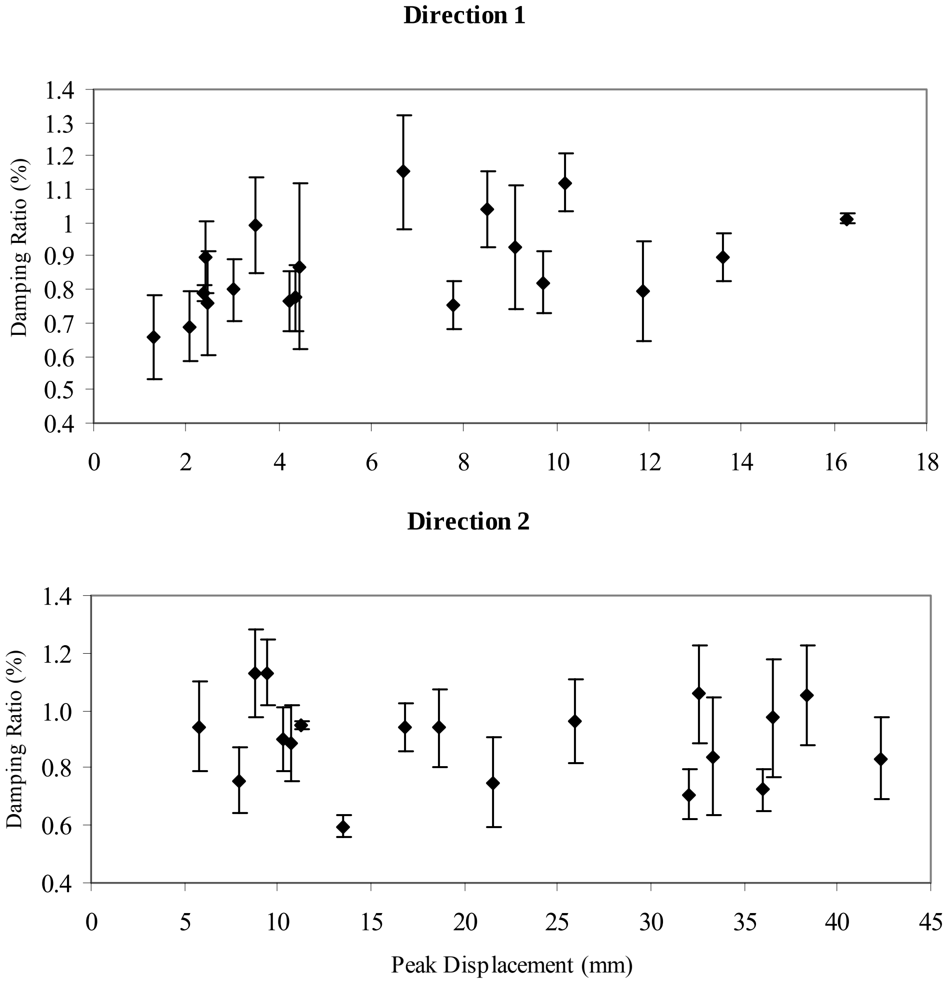
DAMPING FROM FULL-SCALE

One of the objectives of the Chicago Full-Scale Monitoring Program is the determination of in-situ dynamic characteristics of tall buildings excited by wind [3]. Currently, three tall buildings in Chicago are instrumented with a combination of accelerometers, GPS, and anemometers. The natural frequency and damping of the monitored buildings under various wind events are extracted using the Random Decrement Technique (RDT) [1, 16]. As this system identification approach invokes assumptions of stationarity, three separate stationarity tests were performed to investigate the validity of this assumption. These tests included the Run and Reverse Arrangements Tests [17] and a method proposed by Montpelier [18]. It was determined that an 80% passing rate for at least two of the stationarity tests was sufficient to establish stationarity for a given record. Only wind events producing at least five hours of data satisfying this criterion were considered. The analysis here shall focus only on Building 1, a steel tube structure with additional stiffening elements. The observed dynamic properties associated with the two perpendicular axes will be provided herein, dubbed Direction 1 and 2.

The stationary response data was first pre-processed by Butterworth bandpass filters to isolate each mode of interest before applying the Random Decrement Technique, employing a positive point trigger value of X_p. The resulting Random Decrement Signature (RDS) was then processed by the Hilbert Transform and frequency and damping estimates were extracted from the phase and amplitude of the analytic signal, respectively [17].

The Random Decrement Technique is inherently sensitive to the trigger conditions, which directly influences the number of segments captured and, due to the inherent randomness in the data, the quality of the RDS. However, the reliability of RDT can be improved through repeated triggering, as proposed in

[19]. This is accomplished by generating a suite of Random Decrement Signatures associated with a range



of positive point triggers that are within a few percent of the desired trigger X_p. Frequency and damping are then estimated from each RDS in the suite and the resulting parameter estimates are averaged to determine a mean value and corresponding coefficient of variation (CoV). Then by varying X_p, amplitude dependence of the dynamic properties can be investigated [20, 21].

As only annual and sub-annual wind events have been observed to date, the resulting response levels tend to be in the lower amplitude range. As such, the high amplitude plateau may not yet be known for Building 1. As shown in Figure 2, the estimated damping levels over a range of amplitudes show no discernable amplitude dependence in light of the uncertainty associated with the estimated damping. This level of uncertainty is again gauged from the CoV values and is visually represented by the error bars in Figure 2. These damping estimates and displacement amplitudes are similar to those determined by Xu, et al. [22] for a building of comparable height and natural frequency. Xu, et al. [22] included a linear least-squares fit to the data suggesting amplitude dependence; however, no discernable trend exists in the data. A similar fit is not applied to the data presented herein as the damping estimates contain significant scatter that would preclude a meaningful fit. Interestingly, for another building with similar properties, the high amplitude plateau was determined to begin at approximately 5 mm [23]. Thus, the damping values could be relatively constant if they indeed define the high amplitude plateau for this building; however, this cannot be confirmed until more high amplitude response data is observed. In addition, although the wind speeds associated with these events were again sub-annual, the level of response in the recorded data analyzed here may have been too large to permit the observation of the low amplitude damping values for Direction 2, due to the fact that data is only collected by the automated data acquisition system when a prescribed level of motion is surpassed [3].

To compare how the aforementioned damping models predict the damping of a tall building whose height is beyond those used to develop the original predictive models, damping values of Building 1 of the Chicago Full-Scale Monitoring Program are compared to the predictions by Equations (5)-(7), based on the in-situ periods of vibration. The results are provided in Table 1 for a range of amplitudes. Because the depth of the building is relatively large, predictions by Model A will increase rapidly with amplitude. Thus an upper limit must be applied or very unrealistic values of damping will be obtained. For the purposes of this example, the suggested upper limit on damping prescribed in the Hong Kong code is adopted [23]:

$$\xi_m(\%) = \frac{46}{H} + 0.8 * \frac{46}{H} * \frac{x_H}{H} \tag{8}$$

Table 1 *Comparison of full-scale damping with damping predictors*

Frequency (Hz)	Amplitude (mm)	Full-Scale (%)	A (%)	B (%)	C (%)
0.206	2	0.7	0.1	1.8	0.8
0.206	5	0.9	0.1	1.9	1.1
0.206	10	1.1	0.1	2.1	1.7
0.142	2	–	0.1	2.4	0.7
0.142	5	0.9	0.1	2.5	1.1
0.142	10	0.9	0.1	2.6	1.6

As the table illustrates, none of the damping models accurately predict the damping of Building 1; however Model C is most consistent with the values estimated in full-scale, particularly in the low amplitude range. The serviceability damping value that was assumed in the design of Building 1 was 1% [3], which is appropriate considering the damping values observed in full-scale (the average damping for Direction 1 was 0.87% and 0.9% for Direction 2). Also note from Table 1 that Building 1's damping values show no clear evidence of amplitude dependence and that Model A produces an unreasonably small level of damping for this building.

OCCUPANT COMFORT

As discussed previously, damping proves to be a critical parameter in reducing the acceleration response of tall buildings; however, there is still much debate surrounding the levels of acceleration that are truly acceptable and how to quantify them. In many cases, the habitability design defined with respect to these accelerations becomes the governing limit state of tall buildings. In fact, these structures may completely satisfy survivability and serviceability requirements, yet undergo accelerations that may cause occupant discomfort, triggering emotional and physical reactions that include concern, anxiety, fear, dizziness, headaches, and nausea [24]. The perception of these motions increases with the frequency of vibration and can be affected by body position [25, 26]. In light of these factors, a number of studies have attempted to quantify the acceptable levels of acceleration using both motion simulators and full-scale studies, as summarized below.

HUMAN RESPONSE

The biodynamical response of the human body to motion can be defined as:

$$R = KS^n \tag{9}$$

where R is sensory greatness, S is the stimulus, n is the exponent, and K is a constant [27]. The physical parameters of stimulus are products of the amplitude of motion and a power of frequency, i.e., acceleration or jerk. Therefore, stimulus needs to be controlled to improve the human comfort in tall buildings. Human sensory greatness is also called the occupant sensitivity quotient, which defines the ratio of acceptable levels of motion to the maximum motion perceived by a given percentage of the population, as established by Chen and Robertson [27, 29]. Human response to building oscillation, which differs from person to person, depends on many cues, including the amplitude and direction of the motion, visual observations, noise, and co-worker comments [26, 27, 30]. In particular, building motion perception is accentuated by the visual cues of shifting contents and moving horizons with respect to external sightlines, which is often enhanced in torsional response [27]. As Hansen et al. [30] noted, "a moving building is different from a ship, an airplane, or other moving vehicle….a building is not supposed to move." So while the motion of buildings is typically low enough that disturbance is only annoying, the movement needs to be constrained to very low amplitudes that are not perceptible by most people to preserve occupant confidence.

MOTION SIMULATOR TESTS

Two methods have been employed to identify the acceleration amplitude at which people begin to perceive motion: (1) motion simulator tests and (2) full-scale studies. Motion simulators have been used to determine the threshold of perception in many studies, most notably those performed by Chen and Robertson [26]. The results of these tests are often the basis for current standards; however, many of these tests show discrepancies with actual building performance [24]. These discrepancies can be attributed to the uni-axial, sinusoidal motion used in the many motion simulator tests that do not represent the multi-directional, random motion created by wind [24, 25]. In addition to the differences in the motion itself, test subjects isolated in the small rooms used in motion simulators may lack the visual and audio clues contributing to motion perception in actual buildings [24].

There has been some effort to remedy these shortcomings. In the 1990s, a number of researchers included visual cues, multi-directional motion, and random excitation in the motion simulator tests [31, 32, 33, 34, 35]: Goto, et al. [33] looked at the effects of both visual cues as well as torsional motion; Fujimoto, et al. [31] studied the influence of body position and movement on the perception threshold; and Shioya, et al. [34] used random Gaussian white noise for the input instead of sinusoidal motions. Currently, the CLP Power Wind/Wave Tunnel Facility at the Hong Kong University of Science and Technology is using a motion simulator with two degrees of freedom and Gaussian white noise excitation [36]. In addition, they are also utilizing a series of psychological tests to investigate the effects of wind-induced motion on the ability to perform cognitive and non-cognitive tasks.

FULL-SCALE STUDIES

While motion simulators provide a controlled environment for quantifying the levels of perceivable motion, full-scale studies can provide a far more realistic assessment of the levels of motion causing distress and discomfort. The first study to evaluate human perception at a full-scale level was performed by Hansen, et al. [30] in the early 1970s. This project analyzed two buildings after wind storms and established a tentative criterion that was based mostly on two occupant surveys [30]. In establishing their criterion, they combined the occupant perception thresholds with the expected number of times per year a person would be willing to tolerate these motion levels and incorporated the number of complaints a building owner would have to receive before taking action. The resulting criterion was a maximum rms acceleration of 5 milli-g for a storm with a return period of 6 years, assuming only 2% of the population would detect the motion [30].

This study by Hansen, et al. [30] led to further research in the area and the establishment of perception criteria in wind codes/standards and prompted efforts by other researchers: Davenport utilized peak accelerations as opposed to rms values, becoming the accepted standard in North America [37]; Irwin developed the concept of frequency-dependent vibration based on sinusoidal motions later adopted as the ISO 6897 (International Organization for Standardization) Standard, which is used by most of the world [29, 37, 38]; and Melbourne and Palmer [29] related normally distributed and sinusoidal accelerations according to the premise that peak motions were most important and developed frequency-dependant criteria for peak accelerations.

While most of the criteria used in practice are derived from these early full-scale studies, researchers have added to the current knowledge base. Denoon, et al. [39] monitored airport control towers to quantify perception levels and included an attempt to examine the effects of motion on motor skills [39]. Fijimoto, et al. [31] looked at the response of actual buildings during typhoons and related the results to a simulator test. In a further attempt to relate a building's motion to occupant comfort, the Survey of Wind-Induced Accelerations of Tall Buildings was sent out by ASCE Technical Committees and the Council on Tall Buildings in Urban Habitats (CTBUH) [40].

PERCEPTION THRESHOLDS

The perception thresholds determined from the motion simulator and full-scale studies discussed previously are summarized in Table 2, distinguishing between thresholds that are felt by very few people (1-2% of study population) and those that are felt by most (~50% of study population). Since some studies were performed at specific frequencies and others investigated a range of excitation periods, the table lists the thresholds established at a frequency of 0.2 Hz, as this was used in almost all tests except those done by Denoon, et al. [39] (occurring at 0.95, 0.39, and 0.54 Hz). Coincidently, for Building 1 in the Chicago Full-Scale Monitoring Program, Direction 1 has a natural frequency of approximately 0.2 Hz, nicely lending it for comparison to the criteria in Table 2. In nearly all the studies, the probability density function (PDF) of the perception threshold is assumed to be log-normal, although Goto, et al. [33] adopted a Weibull distribution.

Table 2 *Perception thresholds determined from motion simulator tests and full-scale studies*

	Perception Threshold (mg)	% People Perceiving Motion*	Peak or rms	Resulting Distribution	Type of Motion	Simulator or Full-scale
Chen & Robertson [26]	5.9	A	P	Lognormal	Sinusoidal	S
Denoon, et al. [39]+	0.7	A	R	Lognormal	Gaussian	FS
Denoon, et al. [39]+	0.8	A	R	Lognormal	Gaussian	FS
Denoon, et al. [39]+	0.6 - 0.7	A	R	Lognormal	Gaussian	FS
Fujimoto, et al. [31]	9.2	A	P	Lognormal	Sinusoidal	S
Goto, et al. [33]	5.1	A	P	Weibull	Sinusoidal	S
Kanda, et al. [32]	3.4	A	P	Lognormal	Sinusoidal	S
Shioya, et al. [34]	5.1	A	P	Lognormal	Sinusoidal	S
Fujimoto, et al. [31]	2.4	F	P	Lognormal	Sinusoidal	S
Goto, et al. [33]	2.1	F	P	Weibull	Sinusoidal	S
Hansen, et al. [30]	5	F	R	None	Gaussian	FS
Kanda, et al. [32]	0.96	F	P	Lognormal	Sinusoidal	S
Shioya, et al. [34]	1.8	F	P	Lognormal	Sinusoidal	S

* A = Average (~50%), F = Few (1-2%)
+ Not at 0.2 Hz

The results of these motion simulator tests and full-scale studies have become the basis for habitability criteria currently used in practice. These criteria typically focus only on lateral motions, even though it has been well-established that the presence of torsional motion can significantly increase perception of motion, even at low amplitudes [25, 40]. As such, a few sources have set criteria to limit the amount of motion in torsion. For example, Canada has established a guideline of 1.5 milli-rad/sec for torsional motion in a 1-year event and 3 milli-rad/sec in a 10-year event [33]. Further, almost all of the criteria depend on the building usage. North America has a peak limit of 10-15 milli-g for residential buildings, while 20-25 milli-g is considered acceptable for office buildings in a 10-year event [40]. Isyumov, et al. [40] have suggested an additional criterion of 15-20 milli-g for hotels. In addition, Canada has a peak limit of 5-7 milli-g for residential buildings and 9-12 milli-g for office buildings in a 1-year storm [40]. ISO and AIJ both have frequency-based criteria that also depend on building usage [6, 38, 41]. For instance, the rms response of a building with a natural frequency of 0.2 Hz is limited to 5 milli-g under a 5-year wind [37]. Other criteria have been suggested in the literature, ranging from 8 milli-g for rms accelerations to 20 milli-g for peak accelerations [27, 42].

As the various criteria depend on several factors, including return period, frequency dependency, usage of rms vs. peak response level, and averaging time, a direct comparison can be difficult. For example, to compare rms and peak values, depending on whether the input motion is sinusoidal or a Gaussian random process, a simple peak factor can be applied:

$$\sigma = g\hat{x} \tag{10}$$

where $g = \sqrt{2}$ for sinusoidal motion and $g = \sqrt{2\ln(fT)} + \dfrac{0.5772}{\sqrt{2\ln(fT)}}$ for Gaussian random processes, σ is the rms value, $\hat{x}$ is the peak value, f is the frequency of motion and T is the length of averaging time in seconds [e.g., 5].

DEBATE OF RMS VERSUS PEAK ACCELERATIONS

Since the issue of human comfort in tall buildings was introduced, there has been a debate over whether rms or peak acceleration is a more accurate descriptor. This issue was discussed in detail at the 2002 Structures Congress in a panel discussion; however, no resolution has been reached [24]. To date, North America continues to use peak accelerations to establish the maximum allowable acceleration whereas much of the rest of the world uses rms values. As an alternative, jerk (the rate of change of acceleration) may be a more appropriate measure of motion, as a change in acceleration requires adjustment by the human body, but little work has been done to investigate the feasibility of jerk as a descriptor [24, 25].

Advocates of the rms measure generally feel it better represents the sensations experienced by occupants in sustained events, as the duration and number of cycles of motion that occur above a threshold value may be more significant for occupants than an occasional high peak [27]. In general, rms accelerations are easier to use, especially when combining accelerations in different directions [24, 25, 30] and are easier to predict from wind tunnel tests [24].

In the peak combination approach, peaks are defined in each direction and then combined using an empirical combination approach such as square-root sum of the squares (SRSS) or complete quadratic combination (CQC), although CQC is recommended [25, 28]. Advocates for the use of peak values contend that occupants are more dramatically affected by large events or peaks in the response [24]. Furthermore, rms criteria ignore the probability distribution of the peak accelerations, which can vary significantly, as the discussion on peak factors in [29] highlights.

UNCERTAINTY ANALYSIS

In a given structure, there are many variables that have a degree of uncertainty associated with them. These uncertainties enhance the risk of failure in one of any of the design limit states. This risk is often expressed in terms of a probability of failure defined as [43]:

$$P_f = P(Z < 0) = \int \dots \int_{g() < 0} f_X(x_1, x_2, \dots, x_n) dx_1 dx_2 \dots dx_n \tag{11}$$

where Z, the limit state, can be described as $Z = g(X_1, X_2, \dots, X_n)$.

Several options exist to solve Equation (11), including a full distribution approach, analytical approximations to the integral, or simulation techniques [43, 44]. Using the full distribution approach is typically not feasible as this requires knowing the joint PDF for all the random variables. Analytical approximations are used often and can vary in their level of computational effort [43, 45]. A few other studies concerning wind effects have looked at the uncertainty associated with various aspects of wind effects on structures using an analytical approximation technique such as a First-Order Second-Moment (FOSM) method or Monte Carlo simulation [44, 46]. For this paper, a Monte Carlo simulation technique [43] is used to determine the probability of failure defined in Equation (11).

***Table 3** Variables used in Monte Carlo simulations with PDFs, COVs and mean values used in example*

Variable	Unit	Description	PDF	Mean	CoV
H	m	Building height	–	180	–
B	m	Building width	–	30	–
D	m	Building depth	–	30	–
m	kg/m	Mass per unit height (assumed constant over height)	Normal	90000	0.05
k	–	Mode shape exponent	Normal	1	0.05
d	%	Damping ratio	Lognormal	1.0	0.3
f	Hz	Natural frequency of building	Lognormal	0.2	0.01
N	year	Return Period	–	5, 10	–
μ_U	m/s	Mean wind speed (3-sec gust at H_{ref}, open exposure)	Normal	40	0.1
σ_U	m/s	Rms wind speed (3-sec gust at H_{ref}, Exposure C)	Normal	12	0.2
$\bar{\alpha}$	–	Terrain exposure constant, ASCE 7 (Exposure A)	–	0.33	–
$\bar{b}$	–	Terrain exposure constant, ASCE 7 (Exposure A)	–	0.3	–
T	s	Averaging time of wind speed	–	3600	–
H_{ref}	m	Reference height of wind	–	10	–
ρ	kg/m^3	Air density	Normal	1.225	0.05
κ	–	Tail-length parameter of reverse Weibull distribution	–	-0.2	–
e_1	–	Errors associated with using scaled-models in wind tunnels	Normal	1	0.075
e_2	–	Use of model-scale versus full-scale	Normal	1	0.05
e_3	–	Transforming aerodynamic effects to structural load effects	Normal	1	0.025
e_4	–	Observation errors of wind speeds	Normal	1	0.025
e_5	–	Uncertainty of $\bar{b}$	Normal	1	0.05
e_6	–	Uncertainty of $\bar{\alpha}$	Normal	1	0.05
e_7	–	Uncertainty of C_M	Normal	1	0.25
e_8	–	Uncertainty of σ_{C_M}	Normal	1	0.15
$\hat{\ddot{x}}$	milli-g	Peak acceleration criteria	Lognormal	19.3, 20	0.20
$\sigma_{\ddot{x}}$	milli-g	Rms acceleration criteria	Lognormal	5, 5.5	0.20

HABITABILITY LIMIT STATE

To understand the extent to which the uncertainty in assumed levels of damping and other parameters affect the ability of a given design to satisfy any of the habitability criteria described above, the probability of failure will be calculated with respect to the habitability limit state. To do this, the limit state will be defined such that $Z = R - S$ where R will represent the habitability criterion, expressed as an acceleration value, and S will represent the acceleration of a structure at the roof, determined by Equation (2). In addition to damping, the wind speed used in the determination of wind-induced response is also difficult to reliably estimate. As such, several uncertainty factors are used to describe the variability of modeling the wind, as outlined in Kareem [4] and Minicarelli, et al. [47] and defined in Table 3. These include e_1 for the aerodynamic errors associated with the use of scaled-models in wind tunnels, e_2 to account for the errors of using model-scale versus full-scale data, e_3 to allow for transforming aerodynamic loads into structural load effects, e_4 accounts for observation errors of measuring wind speed, e_5 and e_6 assign uncertainty to the terrain exposure constants of ASCE 7, while e_7 and e_8 consider the uncertainty of the Aerodynamic Loads Database parameters [4, 5, 8]. The wind speed is assumed to be represented by a power law expression, using the parameters defined in ASCE 7.

The probabilistic parameters listed in Table 3 were used in the Monte Carlo simulations of structural response in the habitability limit state. The PDFs and CoVs values listed in Table 3 are assumptions of the authors based partly on experience and partly gleaned from those reported in the literature [4, 47, 48]; however, these parameters still need refinement. In the case of damping and frequency, CoVs were determined by the aforementioned RDT analysis. The CoV of the acceleration criteria was determined using the perception thresholds listed in Table 2. While most of the random variables were modeled as either Gaussian or lognormal, the extreme wind speed was modeled as a reverse Weibull distribution [47]. However, as the wind speed of a particular return period is deterministic, the distribution was assumed to be [48]:

$$U = \mu_U - \sigma_U \left[-\ln\left(1 - \tfrac{1}{N}\right) \right]^{-\kappa} \tag{12}$$

where κ is the tail parameter, and μ_U, σ_U are random, normally-distributed variables that represent the variability of the mean wind speed and the uncertainty of the standard deviation, respectively.

A sensitivity study determined that the parameters which influence the acceleration estimates of Equation (2) the most are wind speed and damping. As even slight uncertainties in wind speed can have significant implications for the predicted response, there is a considerable need for reliable estimates of gradient level winds over urban zones. In most cases, models for gradient wind speeds developed for most urban areas are based on surface level data collected at regional airports often a number of miles from the downtown zone. The extrapolation of this data to gradient introduces a high degree of uncertainty into any response prediction. In order to more faithfully relate surface level winds to gradient in urban zones, the second phase of the Chicago Full-Scale Monitoring Program is expanding its anemometer network and introducing computational fluid dynamics models for the city that will be calibrated using the measured wind speeds to develop a more reliable means to estimate wind characteristics at the rooftop of monitored buildings throughout the city [3].

EXAMPLE AND DISCUSSION

The authors now introduce a probabilistic framework for analyzing a particular building's habitability performance, including all inherent uncertainties. In this framework, a Monte Carlo simulation is performed using Equations (2) through (4) to determine the probability of failure of the habitability limit state for different return periods (5-year and 10-year). Table 4 shows the results obtained using this framework for an example office building having the properties listed in Table 3. Significantly high winds are not analyzed in this example as the focus here is habitability performance not survivability. For the 5-year wind, the criteria given by ISO were utilized, while the North American criteria were used for the 10-year case.

For this example building, the structure will fail in the habitability limit state nearly 40% of the time under a 5-year wind, yet only has a probability of failure of 30% for the 10-year wind. The failure implies here that two percent of the people will perceive motion in the building. Because these results are only for one building, no firm observation can be made as to whether these occupant comfort criteria are reasonable. It is the intent of the authors to further develop this example to include the response of full-scale buildings along with an indication of the actual perception threshold of occupants.

__Table 4__ Example results for the habitability limit state framework

Return Period (years)	Mean Wind Speed		Acceleration Criteria		Probability of Failure	
	Reference Height in Exposure C (m/s)	Building Height in Exposure A (m/s)	rms (mill-g)	Peak (mill-g)	rms (%)	Peak (%)
5	31.2	24.2	5	19.3	37.7	37.7
10	32.3	25.5	5.5	20	28.5	31.4

By using a Monte Carlo simulation with the above variables to solve Equation (11), with *S* defined in Equation (2), the probability that the acceleration at the top of the building will exceed the criteria can be calculated, providing an indication of the performance of the building under the specified habitability limit state. This reliability analysis can then become part of a risk-based decision analysis framework with the goal of determining if a supplementary damping device is needed [49]. By including the effects of the additional damping elements into the equation used in the limit state definition, the simulations can be repeated to assess a variety of different damping strategies. These damping treatments could range from simply adding passive direct energy dissipation devices to bracing elements to the inclusion of sophisticated active damping devices [49, 50].

CONCLUSIONS

Although the habitability limit state generally governs the design of tall buildings, there is no unified consensus on the criteria defining acceptable motions given the number of variables contributing to individual sensitivity. Furthermore, when determining the accelerations of a building in the design stage, there is an additional level of uncertainty as damping values are largely assumed. As such, this paper provided a careful examination of damping models and habitability criteria in the context of tall building design, with reference to an on-going full-scale monitoring program in the City of Chicago. In particular, it was demonstrated that many of the damping models in the literature are inappropriate for tall buildings, given their basis on datasets lacking a significant number of tall buildings. A number of the unresolved debates surrounding the appropriate definition of habitability criteria were also highlighted in this study. Given the uncertainty associated with the design damping level, habitability criteria and various other factors associated with the prediction of wind-induced response, a probabilistic framework was introduced to assess the reliability of tall buildings in the habitability limit state using a Monte Carlo simulation. The example using this framework demonstrated that a typical tall building can have a 30-40% probability of failure in the habitability limit state. This approach can then be used within a risk-based decision making framework to further examine the habitability of a building as well as measures to enhance its performance.

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