Full-Scale Testing to Evaluate the Performance of Standing Seam Metal Roofs Under Simulated Wind Loading

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Abstract

The current methods for evaluating the adequacy of metal roofs in withstanding wind-induced loads involve undertaking uniform uplift pressure tests. These methods may not be truly representative of real conditions, and might set higher minimum design requirements than necessary in some cases and in others it could underestimate effects of very localized peak pressures. This research work presents results of a full-scale experimental study conducted under more realistic wind loading with the panels installed as they would be in the American Society for Testing and Materials (ASTM) E1592 test chamber. The research objectives were to (i) measure the uplift roof pressure experience by mono-sloped standing seam metal roofs and compare them with the provisions of the American Society of Civil Engineers (ASCE) 7-10 standard, (ii) evaluate the performance of standing seam roofs under high winds, and (iii) compare the deflections and failure modes observed under more realistic wind loading to uniform loading tests. The research has provided test based data on aerodynamic loading of two types of standing seam metal roofs (i.e. vertical-leg and trapezoidal), as well as their performances under high wind speeds. Significantly higher pressure was recorded on the trapezoidal roof. This showed that roof panel profile and perimeter

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eave attachments can significantly affect uplift pressures. The ASCE 7-10 standard was observed to underestimate corner wind suctions on trapezoidal roof. Lower deflections were recorded by the vertical-leg roof owing to its higher stiffness and lower suctions experienced. The ASTM E1592 test protocol was observed to produce higher deflections and more conservative failure wind speeds than those experienced in the current tests. However, entirely different failure modes were observed between the uniform and dynamic tests. This was attributed to wind-induced vibrations that were observed in the current tests that are not present in the ASTM E1592 test, which is entirely static. The current research results may suggest future directions to enhance the existing testing standards.

Keywords: Full-scale testing; Standing seam metal roof; Roof aerodynamics; Roof panel deflection; Roof failure; Uniform uplift testing; Wall of Wind

1. Introduction

Roofs have been identified as one of the most wind storm vulnerable components of a building envelope. High dynamic wind suctions created at the surface of a roof due to wind flow separation (or conical vortices) coupled with positive internal pressures are the main cause of damage to the roof systems. Water leaking through a failed roofing system during rain accompanied wind storms can result in major damage to interior contents, disrupt the functionality of critical and essential facilities, and even cause ceiling weakening and collapse which can result in injury to the occupants ([1]; [2]; [3]). Currently, the performance of metal roofs under wind load is evaluated by undertaking physical tests to assess their capability to withstand a “design load” as provided by wind loading codes and standards. The ability of such tests to assess the true performance of the roof system depends on how well the tests represent the true wind loading actually experienced in wind storm conditions.

In North America, the methods of physical testing protocols which are most commonly recommended by testing standards to evaluate the adequacy of metal roof panels in withstanding wind loads are the Underwriter Laboratories (UL) 580 [4] and American Society for Testing and Materials (ASTM) E1592 testing protocols [5]. The UL 580 involves putting the test specimen in a pressure chamber capable of applying steady positive pressure on the underside of the test assembly and a uniform oscillating negative pressure on its top surface. Regular checks for permanent deformations are conducted, and the roof metal is assigned a class rating depending upon the magnitude of positive and negative pressures that cause permanent deformation. In the ASTM E1592, a static positive air

pressure is applied on the underside of a test specimen at regular pressure increments until failure of principal or critical element is observed. The deflections and deformations of panels and anchors are recorded, from which load versus deformation curves are obtained. Those plots are then used to evaluate the strength and serviceability of the metal panels. Both testing protocols use defined positive/negative pressures that include neither the temporal nor the spatial pressure variations which are inherent in “real” wind loading, and might on one hand set higher minimum design requirements for the entire system than necessary or on the other hand might underestimate effects of localized peak pressures on critical locations. Moreover, the modes of roof specimen failures observed in uniform loading tests might not be representative of observed field failures ([6], [7]).

Experiments conducted using the BRERWULF setup were one of the earliest efforts to evaluate the performance of standing seam metal roofs subjected to non-static simulated uplift pressures [8]. Although the BRERWULF setup can apply pre-defined time varying pressure loads on a specimen, it uses uniform pressure hence doesn't duplicate the spatial variation of actual wind loading [8]. The authors in [8] used influence surface experiments to understand the spatial variation in load transfer mechanism of standing seam metal roofs. They also showed that the choice of specimen end restraints can significantly affect the clip loads. Under the objective of relating high local loads to their corresponding design uniform static loads, Sinno et al. 2003 [9] used electromagnetic drivers to simulate the non-uniform dynamic uplift forces due to wind on standing seam metal roofs at Mississippi State University (MSU). Even though the tests at MSU used real roof structures, and applied dynamic uplift forces on the roof, the resolution of the forces was limited and the magnetic actuators could apply loads of single polarity [7]. Farquhar et al. 2005 [10] conducted small scale uniform uplift and dynamic wind tunnel pressure tests on elastic standing seam roof model at University of Western Ontario (UWO), and formulated an effective pressure coefficient that can relate uniform uplift failure pressures with the actual dynamic wind pressures that cause clip-failure. Those authors also demonstrated that the relationship of effective uniform pressure to wind speed can be predicted analytically, with good accuracy, by integrating clip reaction influence functions with non-uniform external pressures measured on a rigid model in the wind tunnel. Even though the tests at UWO used “realistic” wind loads, scaled models of standing seam metal panels were used, which posed difficulty in modeling the detailed behavior of all roof components in small-scale. In full-scale testing, besides being able to represent the true details of roof systems, Reynolds number mismatches, which are inherent in small scale wind tunnel experiments, can be

avoided. When Reynolds number mismatch exists, the viscous forces within high-frequency turbulent eddies become larger than their full-scale counterparts and cause viscous dissipation of those eddies' energy [11]. In a sense, while the MSU tests used real roofs under approximated wind load, the UWO tests used realistic wind load but approximated roof structures. The results from both approaches suggest that the ASTM E1592 test contains conservatism of as much as about 50% for the particular roof systems studied [7].

The objective of this paper is to understand the behavior of standing seam metal roofs under as close to realistic wind loading as possible and using full scale specimens rather than models. Experimental investigations which included aerodynamic pressure measurement and deflection measurement were carried out on mono-sloped full-scale standing seam metal roofs using Florida International University's (FIU) Wall of Wind (WOW) facility. To evaluate the adequacy of the "design loads" adopted in the testing of the roof panels, the pressure results were compared with the provisions of the ASCE 7-10 standard [12]. Although it is generally agreed that using realistic wind loading and real mockups produces the best results, the costs associated with running tests on a large scale test specimen for all angles and speeds in a large wind tunnel represents an impediment to undertaking such tests on a routine basis for product approval purposes. Therefore, one of the objectives of this research is to compare the performance of identical metal roof systems under dynamic and uniform wind loading. The strength and serviceability performance of identical metal roofs under uniform and realistic wind loads are evaluated and compared. This is expected to be helpful in evaluating the validity of conventional uniform pressure testing protocols.

2. Experimental Setup and Testing Protocols

Two types of standing seam metal roof systems; a vertical-leg standing seam metal roof and a trapezoidal standing seam metal were selected for testing. A three-phase full-scale experimental investigation was conducted on each roof system. The vertical-leg standing seam metal roof and the trapezoidal standing seam metal roof are henceforth referred to as vertical-leg roof and trapezoidal roof respectively for brevity. In the first phase, aerodynamic pressure measurements were conducted at different wind directions. In the second phase, deflection measurements were undertaken at different wind directions and different wind speeds. Failure observations were also performed.

2.1. FIU 12-fan Wall of Wind (WOW)

The 12-fan Wall of Wind (WOW) open jet facility at Florida International University (FIU) was used to generate the wind field in this experimental investigation. The 12 electric fans are arranged in a two-row by six-column pattern to produce a wind field 6.10 m (20 ft.) wide and 4.30 m (14 ft.) high, allowing aerodynamic testing of large-scale models or full-scale portions of buildings. A contraction section is used downwind of the array of 12 fans to create acceleration of the flow and the attainment of a uniform flow field with high wind speeds (up-to 70 m/s (157 mph)). A 9.75 m (32 ft.) long flow simulation section downwind of the contraction incorporates triangular spires and floor roughness and provides the required fetch length and flow confinement to develop the desired mean velocity profile and turbulence characteristics. Figure 1 shows the WOW simulated open country condition's mean wind speed (with target velocity profile exponent $\alpha = 1/6.5$) and turbulence intensity which were used in the experiments. It should be noted that the tests were performed in partial turbulence simulation, hence the turbulence intensity at roof height was lower than that of atmospheric boundary layer (ABL) which contains full spectrum of turbulence. However, using the method proposed by Irwin ([13];[14]) and Asghari Mooneghi ([15];[16]) the adequacy of the current turbulence intensity can be shown. The comparison of the WOW partial spectrum and Kaimal spectrum (Fig. 2) shows that while the low-frequency turbulence is missing, satisfactory agreement is achieved in the high frequency end which has been noted by a number of researchers ([17];[18];[19];[20];[21]) as necessary for the correct simulation of flow separation and reattachment.

Figure 1: Simulated open terrain ABL in the WOW: Mean wind speed profile (left) and Turbulence intensity profile (right)

Figure 2: Comparison of the WOW and Kaimal spectrum

2.2. Test Building Model and Roof Systems

The standing seam metal roofs were attached to a base structure which was designed to support interchangeable mono-sloped roofs with slope of 4.57 deg. The base structure consisted of cold form steel studs with plywood exterior sheathing and Z-purlins to support the roof panels. The base had plan dimensions of 4.57 m x 3.05 m (15 ft. x 10 ft.) and low eave height of 1.52 m (5 ft.). The test model height was around 39% of the wind field height generated by the WOW. This was within the 33% to 50% of the wind field height recommended by Aly et al. (2011) [22] for obtaining roof pressure measurements with insignificant blockage effects in open jet facilities. The test model was located at a distance of around 13.70 m (45 ft.) from the WOW fans, thus abiding by the minimum proximity requirement recommended by Bitsuamlak et al. (2010) [23].

Two types of typical high yield, light gage steel standing seam metal roofs; *vertical-leg standing seam* and *trapezoidal standing seam* roofs, which are commonly used by metal building industries, were selected for investigation (see Fig. 3). The metal panels in both roofs consisted of 4.57 m (15 ft.) long 24 gauge metal panels attached to each other mechanically and using tape-sealer at the perimeter. The panels had 3 spans at 1.52 m (5ft) O.C. The vertical-leg roof panels consisted of 460 mm (18 in.) wide flat pan sections, 51 mm (2 in.) tall ribs and 4.57 m (15 ft.) long 24 gauge metal panels. Floating perimeter plates and perimeter eave trims (attachments) were installed on all four edges. The panels were connected to the Z-purlins using floating clips. In the case of the trapezoidal roof, 24 gauge trapezoidal metal panels having width of 610 mm (24 in.), 76.2 mm (3 in.) tall ribs and

length of 4.57 m (15 ft.) were used. The panels were attached to each other mechanically and tape sealer was also applied. Sliding clips were used to hold the roof panels to the Z-purlins and eave trims were installed only on the sloping edges of the roof. At the low-eave end, the trapezoidal rib openings were filled by light gauge metal inside-closures pieces which were attached to the end purlins as well as the panel ribs using self-drilling screw fasteners. In both roof systems, each clip was connected with the purlins using two self-drilling screws. It should be noted that both roof systems had no deck, i.e., they were non-composite assemblies and they were installed by professional installers according to industry standards. With the objectives of simulating a continuous panel property and replicating ASTM E 1592 boundary conditions, in the deflection measurement tests the low eave side of each roof was screwed, but the high eave side of each roof was free and had only one exposed continuity fastener per panel-rib. This fastener was installed through the vertical rib to make allowance for the factory lap cut in the panels that would not allow full panel/clip engagement. In the pressure reading tests, tape and plastic were used to seal against leakage.

Figure 3. Standing seam metal roof mock-ups used in testing: a. Vertical-leg roof and b. Trapezoidal roof

2.3. *Full-Scale Experiments*

In the first phase of testing, aerodynamic pressure measurements were conducted for twelve wind directions at low-eave height mean wind speed of 24.7 m/s (55.3 mph). The locations and numbering of the pressure sensors, deflection measurement devices and wind directions are shown in Figure 4a and 4b for the vertical-leg and trapezoidal roof respectively. Each roof was instrumented with a total of 28 pressure taps to capture the external pressures and 4 pressure taps to measure the pressure inside the base structure. Time-histories of external and internal wind pressures were collected using 6.35 mm ID polyurethane pressure tubes and a DSA 4000, ZOC 33 Scanivalve data acquisition system. One minute duration time history pressure data were collected for each test at a sampling frequency of 512 Hz.

The second phase of full-scale testing, whose objective was to evaluate the strength and serviceability performances of the two types of standing seam metal roofs under simulated hurricane force winds, consisted of measuring mid-panel (panel pan) and panel-rib deflections at different wind speeds and wind directions. The turbulent nature of the wind forces creates non-steady vibratory deflections which are non-uniform across the surface of the metal roofs. To capture this non-uniformity, deflection measurement devices were installed at different locations across the roof surfaces (Fig. 4a and 4b). The testing protocol followed in the deflection measurement tests is shown in Table 1. Mid-panel and panel-rib deflections were recorded using RDP Electrosense LDC3000A linear variable differential transformers (LVDTs) and Celecso SP2 type string pots. The deflection measurement devices were installed on rigid platforms inside the base structure. Deflection measurement was conducted for an effective duration of 1 minute (i.e. excluding fan ramp up times), at sampling rate of 100 Hz. After each deflection measurement test the wind speed was brought down to zero, and the roofs were inspected for any permanent deformation and attachment failures. Baseline (reference) deflection measurements at zero wind speed were also conducted before each test.

Roof type	Free leeward end	Mean wind speed at low-eave height, m/s	Wind directions (see Fig. 4 for definition of wind direction)
Vertical-leg Roof	High eave	30.5 (68.2)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		39.1 (87.5)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		47.8 (107)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		56.8 (127)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		65.7 (147)	0°, 30°, 45°, 60°, 85°, 275° and 315°
	Low eave	47.8 (107)	95°, 135°, 180°, 210°, 225°, 240° and 275°
		65.7 (147)	95°, 135°, 180°, 210°, 225°, 240° and 275°
Trapezoidal Roof	High eave	30.5 (68.2)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		39.1 (87.5)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		47.8 (107)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		56.8 (127)	0°, 30°, 45°, 60°, 85°, 275° and 315°
		65.7 (147)	0°, 30°, 45°, 60°, and 85°

Table 1: Wind-induced deflection testing protocol

Figure 4: Pressure taps, LVDT and String pot locations and numbering; a. Vertical-leg roof, b. Trapezoidal roof

3. Data Analysis

The external pressure taps on the roof specimen were divided into groups of pressure taps where each group represented a roof region. When comparing the pressure results with the provisions in the ASCE 7-10, the external pressure taps were grouped into different possible pressure tap combinations where each combination represented a surface area of 0.93 m^2 (10 ft^2), which is the minimum components and cladding area specified by the ASCE 7-10. For instance some of the pressure tap groups used were: P1, P3, P5 and P7; P1, P3, P11, P12, P13 and P14; P15 to P20 etc... The time-series pressure readings from the pressure taps within each group were spatially-averaged. This

produces time-histories of averaged pressures for different roof regions, from which the mean and peaks can be computed. Note that since fluctuations at different pressure taps do not occur in a synchronized manner, area-averaging tends to smooth pressure spikes from individual pressure taps. Nevertheless spatially-averaging is necessary because the uplift pressure is expected to be shared among the roof panels, and designing for highly localized peaks is overly conservative [7]. Before analysis, the aerodynamic pressure data were corrected for pressure signal distortions due to tubing length by evaluating the transfer functions for the different tubing lengths used and applying the inverse transfer function to the data [24]. The pressure data were low-pass filtered using a cut off frequency of 100 Hz.

The pressure results are presented as mean and peak pressure coefficients of different roof regions. The mean pressure coefficients $C_{p\text{mean}}$ were obtained using Eq. (1);

$$C_{p\text{mean}} = \frac{P_{\text{mean}}}{\frac{1}{2}\rho V_{\text{mean}}^2} \quad (1)$$

where P_{mean} is mean of 1 minute duration area averaged pressure of a roof region, V_{mean} is the mean upstream wind speed at model low-eave height; and ρ is the density of air. The peak pressure coefficients were obtained using the Eq. (2), and are normalized to the three second gust dynamic pressure:

$$C_{p\text{peak}} = \frac{P_{\text{peak}}}{\frac{1}{2}\rho V_{3\text{sec}}^2} \quad (2)$$

where P_{peak} is the estimated peak pressure and $V_{3\text{sec}}$ is the three second wind speed at model low-eave height. In order to calculate the estimated peak pressure, a method called “Partial Turbulence Simulation” was used (Asghari Mooneghi 2014 [15]). This method requires that the high frequency end of the wind turbulence spectrum be correctly simulated in the wind flow (see Fig. 2). The effects of low frequency turbulence can then be included in the post-test analysis. Fu et al. (2014) [25] developed a different approach for predicting the full-scale equivalent peak pressure coefficients from tests performed in a flow with partial turbulence simulation. In their method, the effects of low frequency turbulence were included in the post test analysis by increasing the mean wind speed of the test (U) by an increment ΔU . The mean wind speed increment ΔU can be viewed as a flow fluctuation with zero

frequency and perfect spatial coherence and, therefore, as a reasonable approximation of the missing low-frequency fluctuations in the spectrum. This increased wind speed was then used to obtain scaling parameters needed in the rest of the analysis for calculations of the peak pressures based on a statistical approach proposed by Sadek and Simiu (2002) [26]. In the “Partial Turbulence Simulation” method used in this paper, the turbulence is divided into two distinct statistical processes, one at high frequencies which can be simulated correctly in WOW, and one at low frequencies which can be treated in a quasi-steady manner. The joint probability of load from the two processes is derived, with part coming from the WOW data and the remainder from the assumed Gaussian behavior of the missing low frequency component. For the evaluation of the estimated values, the peak value with 95% probability of not being exceeded in one hour of full spectrum wind was selected (a more detailed explanation is provided in Appendix A).

For the deflection measurement tests, the mean and observed maximum net deflections were evaluated from time histories of net deflections, $d_{net}(t)$ which were computed using Eq. (3)

$$d_{net}(t) = d_{rec}(t) - \text{mean}(d_{baseline}(t)) \quad (3)$$

where $d_{rec}(t)$ denotes time series of recorded deflections at specified wind speed and $d_{baseline}(t)$ represents the baseline (reference) deflections measured at zero wind speed i.e. before the actual deflection measurement tests.

4. Test results and discussion

4.1. Aerodynamic Pressure Results

In both standing seam roofs, relatively higher uplift pressures were recorded in the windward corner and edge roof regions, which is an expected aerodynamic property of flat or near flat roofs. Previous researches ([27];[28]; [29]; [30]; [31]; [32]; [33]; [34]) have shown that buildings experience highest suction due to the formation of conical vortices by oblique or cornering winds. The formation of the conical vortices creates strong suction forces on the roof surface below the vortices. Wind approaching a building normal to its walls leads to the formation of separation bubble, another phenomenon of flow separation which is responsible for high roof suction at the edges. The mean pressure coefficients on the high-eave corner (i.e. P11 to P14) and low-eave corner (i.e. P25 to P28) roof regions for both types of standing seam roofs are shown in Figure 5. The high and low-eave corner regions cover surface areas of 0.35 m² (3.79 ft²) in the vertical roof and 0.49 m² (5.27 ft²) in the trapezoidal roof. The results show that, although

the model geometry (such as eave height and roof slope of the test models) and test setup conditions in the vertical-leg and trapezoidal roofs were identical, considerably higher suctions with larger non-uniformity were observed in case of trapezoidal roof. Similar pressure differences were also observed in all the other roof regions. This is a clear indication that details of the standing seam profile and spacing, and eave attachment details significantly affect the magnitude and distribution of wind pressures on mono sloped standing seam roofs. This is similar in theory with the findings of previous researches on roof-tiles ([35]; [36]) which showed the high dependence of wind pressure on the tile geometry, and implied that using bare decked roof models in aerodynamic testing might underestimate the actual roof pressures. These results also reinforce the well-established notion that efficient aerodynamic design can play critical role in mitigating roof uplift pressures.

Figure 5: Mean pressure coefficients

The ribs of the trapezoidal roof were 25.4 mm (1 in.) higher and spaced 152.4 mm (0.5 ft.) wider than that of the vertical-leg roof. Moreover, in contrast to the vertical-leg roof which had eave trims/attachments on all its 4 edges, the trapezoidal roof had eave attachments installed at its two sloping edges only. Rounding or chamfering eaves is known to significantly reduce suction at the leading edge [37], hence, the eave configuration of the vertical-leg roofs (see Fig. 3a) might be alleviating roof suction by weakening or eliminating the possibility of separated flow creation due to straight sharp edges. It can also be hypothesized that since the eave trims in the trapezoidal roofs extend to about 72.6 mm (3 in.) above the surface of the panels they might be acting as low parapets with parapet height to eave height ratio, h_p / H of around 0.05. Low parapets have been observed to increase roof corner uplift pressures for oblique wind directions ([38], [39]). This might be because the formed vortices become more stable as they are originated above the roof surface [39]. Furthermore, parapets on one side of the roof only (similar to the tested eave attachment configuration in the trapezoidal roof) have been observed to increase the mean and peak local suction on the roof corner and edge regions [40]. This significant difference in wind pressure at the surface of the vertical-leg and trapezoidal seam roofs can invoke several hypothetical explanations; nevertheless detailed experimental investigation is needed to precisely identify the actual cause of such substantial pressure differences.

In most code provisions for wind loading, a roof surface is divided into roof zones having different pressure coefficients depending on the building's roof type, roof pitch, plan dimensions, and roof height. According to the ASCE 7-10, the experimental mono-sloped roofs can be divided into three zones, namely Zone 3' (1.83 m (6 ft.) wide corner zone near the high end), Zone 3 (1.83 m (6 ft.) wide corner zone near the low eave end), and Zone 2' (0.91 m (3 ft.) wide edge zone in mid roof region). The ASCE 7-10 recommends using external GCp values of -2.6, -1.8 and -1.6 for Zone 3', Zone 3 and Zone 2' respectively. It should be noted that equivalent pressure coefficients GCp_{eq} need to be computed before comparing the Cp from the WOW experiments to the GCp recommended by the ASCE 7-10. Equivalent pressure coefficients GCp_{eq} were calculated using the method explained in St.Pierre et al. 2005 [41] , as shown in Eq. (4).

$$GCp_{eq} = \left(\frac{\frac{1}{2} \rho V_{h,3sec}^2}{\frac{1}{2} \rho V_{10m,3sec}^2 K_z K_{zt} K_d} \right) C_p \quad (4)$$

where C_p is the peak area-averaged pressure coefficient obtained from the pressures measured at WOW after being normalized using the 3-sec wind speed at the low-eave height ($V_{h, 3 \text{ sec}}$), $V_{10 \text{ m}, 3 \text{ sec}}$ is the 3-sec wind speed at 10 m (33 ft.) height, K_z is the velocity pressure exposure factor which in this case converts $V_{10 \text{ m}, 3 \text{ sec}}^2$ to $V_{h, 3 \text{ sec}}^2$, K_{zt} is the topographic factor, and K_d is a non-dimensional quantity smaller than unity that reflects the fact that the climatologically and aerodynamically or dynamically most unfavorable wind directions do not typically coincide [42]. Assuming K_d to be 1.0 as directionality effects were not accounted for [41] and using K_{zt} as 1.0, we get a C_p to $GC_{p \text{ eq}}$ conversion factor of 1.0, hence $GC_{p \text{ eq}} = C_p$. Note that the ASCE 7-10 specifies a constant K_z value for all heights less than 4.5 m (15 ft.), hence when calculating the conversion factor the velocity at model eave height (i.e. 1.5 m or 5 ft.) was assumed to be same as the velocity at 4.5 m (15 ft.) height. The effect of building height on uplift roof pressure of standing seam metal roofs will be addressed in a future work.

For each wind direction, the highest area-averaged peak uplift pressure of all possible combinations of pressure taps that constitute a surface area of 0.93 m² (10 ft²) inside roof zone 3', zone 3 and zone 2 are shown in Figure 6a, 6b and 6c respectively for both vertical-leg and trapezoidal roofs. Figure 6 also compares those maximum peak uplift pressures with the ASCE 7-10 provisions. For the case of the vertical-leg standing seam roof, the ASCE 7-10 provisions were found to be quite conservative. But in the case of the trapezoidal standing seam roofs, the ASCE 7-10 provisions were exceeded at some critical wind directions. Particularly, the ASCE 7-10 provision of GC_p -1.80 and -1.60 for Zone 3 and Zone 2' respectively was found to be un-conservative. A peak pressure coefficient as high as -2.57 was observed in the low-eave edge region at a wind direction of 270°. Reducing the pressure averaging area was observed to have a significant amplifying effect on the computed peak pressures especially in the corner roof regions. For instance, in the case of the trapezoidal roof, reducing the averaging area from 0.93 (10 ft²) to 0.49 m² (5.27 ft²) increased the highest estimated peak pressure coefficients from -2.51 to -3.57 and from -2.56 to -3.83 in the high eave and low eave corner regions, respectively.

Figure 6: Peak pressure coefficients in: a. Zone 3', b. Zone 3, and c. Zone 2'

The authors in [39] addressed contradicting conclusions from past studies on the conservativeness of the ASCE 7 pressure coefficients for components and cladding when applied to standing seam metal roofs. Some researches (Kopp et al. 2005 [39] and St. Pierre et al. 2005 [43]) concluded that the ASCE pressure coefficients were unconservative when compared to measured wind tunnel pressures, while others (Surry et al. 2007 [7] and Farquhar et al. 2005 [10]) used worst clip reactions to show that ASCE provisions were highly conservative. The authors in [44] carried out wind tunnel experiments using a 1:50 scale test model with full-scale dimension of 45.72 m (150 ft.) by 30.48 m (100 ft.) and roof height of 9.75 m (32 ft.) with a roof slope of 1.2° . In their wind tunnel experiments, the

highest GCp_{eq} experienced by a 0.93 m^2 (10 ft^2) area of any shape in the corner and edge zones were -3.57 and -2.55 respectively. Although the highest GCp_{eq} recorded in the corner and edge zones on the model in WOW are less than those peak values recorded by Morrison and Kopp [44], both investigations show that the ASCE provisions could be un-conservative in the corner and edge regions which is in agreement with Kopp et al. 2005 [39] and St. Pierre et al 2005 [43] . But Morrison and Kopp [44] also showed that the observed conservatism in Surry et al. 2007 [7] and Farquhar et al. 2005 [10] was merely due to the fact that the critical clips were not located in the worst aerodynamic region of the roof.

4.2. Wind-induced deflection results

During high-wind loading, metal roofs experience vibratory deflections which are either upward or downward dominated depending on the direction of the wind-induced forces on their surface. In few cases downward deflections were recorded, but most cases showed upward dominated deflections. All results given in this paper are for vertically upward deflections. Under an uplift pressure the flat pan sections of standing seam roof panels assume the shape of a semi-cylindrical shell spanning between the panel ribs. The panel wind-induced vibrations are unsteady as well as highly non-uniform. An increase in wind speed increases not only the mean deflection but also the amplitude of the vibrations. This is shown in Figure 7 which depicts time-series of deflections recorded by LVDT 5, installed on the vertical-leg roof at two different low-eave mean wind speeds; wind speed-1 (30.5 m/s (68.2 mph)) and wind speed-2 (65.7 m/s (147.0 mph)) at 0° wind direction. The deflections experienced by the roof panels were also highly dependent on wind direction.

Figure 7: Time series deflection at LVDT 5 in vertical-leg standing seam roof, 0° wind direction

Relative to the mid-panels, lower deflections were observed along the panel-ribs. This behavior is expected of standing seam roofs subjected to wind loading. The highest deflections were observed on the edge panels. Particularly, as the wind direction approaches 90° or 270° the deflection experienced by the free end side of the upwind edge panel becomes significantly higher than the interior panels. Those observed high edge panel deflections are in agreement with the finding of Sinno 2005 [45] which also recommended stiffening the edge panels in both vertical-leg and trapezoidal standing seam roofs. During the WOW deflection measurements, the low eave side was fastened whereas the high eave side was free and deflection measurements were taken at 5 different wind speeds and 7 wind directions. The roof boundary conditions and location of deflection measurement devices were selected in accordance with the protocol normally followed in the ASTM E15692 tests. As a result, deflection measurement devices were not placed at the edge panels which were later observed to experience the highest deflections. Nevertheless, for the purpose of comparing the WOW deflections to ASTM E1592 deflections, estimates of edge panel deflections were undertaken using a number of snapshots from the experiment video recordings. Note that in contrast to realistic wind loading, in uniform pressure tests all the roof panels experience similar uplift pressure, hence measuring the deflection at the interior panels only is sufficient.

Spectral analysis of the deflection time-histories was conducted to better understand the behavior of the roof metal panel wind-induced vibrations. Figure 8 depicts normalized spectral density of deflections (i.e. $nS(n)/D^2$, where D is mean deflection) measured on both roofs at different mean wind speeds. It can be seen from Figure 11 that roof panels vibrate at a fundamental frequency that depends on the stiffness of the metal panel assembly. At mean speed of 30.5 m/s (68.2 mph), the frequency of vibration of the vertical-leg (low-eave fixed case) and trapezoidal roof panels were 13 Hz and 10 Hz, respectively. This difference is expected due to the higher stiffness of the vertical-leg roof.

Figure 8: Spectrum plots of vertical-leg and trapezoidal roof deflections at LVDT 5

At low mid-panel deflections, the roof panel vibrations were observed to be mainly limited to the flat metal pan section only. But as wind speed increases, the vibrations start creeping into the panel-ribs and at a sufficiently high wind speed, the flat pan section gets lifted entirely and the whole metal panel section starts vibrating from the ribs. At this stage, the rate at which deflection increases with increasing wind speed reduces and the dominant frequency of vibration also changes. In a sense, at very high deflections the metal panel assembly becomes increasingly stiffer until failure is encountered. Figure 8 shows that as the wind speed increases, the frequency of vibration of the roof panels also increases. In the vertical-leg roof slight change in natural frequency is observed only after wind speed of 47.8 m/s (107 mph), but in the trapezoidal roof change in the fundamental frequency of vibration starts at a much lower wind speed. Which means, for instance, at a wind speed of 47.8 m/s (107 mph) the panel vibrations in the trapezoidal roofs have already crept into the panel-ribs while the vibrations in the vertical-leg roof were still limited to the metal pans. Note that the results of this spectral analysis only apply to the interior metal panels where measurements were available.

Since one objective of this study was to evaluate the structural performance of standing seam metal roofs under high wind speeds, more emphasis was given to deflection measurement devices that registered the highest deflections. The maximum deflections are believed to have been caused by the peak wind gusts, hence the deflection

results are reported as maximum deflections versus peak wind gusts. The peak wind gusts were evaluated by moving averaging from the time-histories of wind speeds. Recall that the tests were conducted under a partially simulated turbulence with an equivalent full-scale gust duration of 0.1 sec (Appendix A), hence, the peak 0.1 sec gust wind speeds were first evaluated, and then scaled to 3 sec gust speeds. The highest deflections were recorded by the deflection measurement devices closest to the free end and the upwind edge panel. In the vertical-leg roof, for the low-eave fixed case since LDVT 5 and LVDT 6 were symmetrically placed they experienced similar highest deflections at wind directions of 275° and 85° respectively, and the highest rib deflection was recorded by SP 3 which as expected was much smaller than the mid-panel deflections. Figure 9 shows the variation of maximum deflections recorded by LVDT 1, LVDT 6 and SP 3 of the vertical-leg roof with wind directions when the low-eave side was fixed. The deflections occurred at a low-eave peak 3 sec gust of 56.1 m/s (125.5 mph). For all three deflection measurement devices, the critical wind directions were 275° and 85° . In the high-eave fixed case, the highest mid panel deflections were recorded by LVDT 7 and LVDT 2 in the high and low eave sides respectively, and the highest seam-line deflection was recorded by SP 3. Figure 10 shows maximum observed deflections versus wind direction for LVD7, LVDT 2 and SP3 at peak 3 sec wind speed of 47.2 m/s (105.7 mph) for the high-eave fixed case.

Figure 9: Maximum deflection versus wind direction in vertical-leg roof, low-eave fixed

Figure 10: Maximum deflection versus wind direction in vertical-leg roof, high-eave fixed

In the trapezoidal roof, the highest mid-panel deflection was recorded by LDVT 5 and the highest rib deflection was recorded by SP 3 both at wind direction 275° . Figure 10 shows deflection versus wind direction for LVDT 2, LVDT 5 and SP 3 of the trapezoidal roof at peak 3 sec gust of 56.1 m/s (125.5 mph). As anticipated from the pressure results, the deflections experienced by the trapezoidal roof were significantly higher than the vertical-leg roof. Moreover, the trapezoidal roof was more flexible as the spans between adjacent ribs in the trapezoidal roof were 152.4 mm (6 in.) longer than those of the vertical-leg roof. In both roofs the variability of deflection with wind direction was observed to be less at the deflection measurement devices which were placed near the fixed high eave end.

Figure 11: Maximum deflection versus wind direction in trapezoidal roof

5. Observations of Permanent Deformations and Roof Failure

Wind-induced vibratory deflections exert tension stress on the metal panels and cyclic loading at the clips and attachments which can result in roof failure. The performance of standing seam metal roof panels during high-wind events is a function of the panel strength, their interlocking with clips, clip spacing and attachment, and strength of the clips and attachments. The presence of strong interlocking and continuity between the panels increases their uplift resistance and makes them more resilient against leakage, but it can also make them susceptible to progressive failure [46]. Post-storm investigations have shown that typically after roof panel blow-off, the clips are found attached to the purlins ([1], [47]) implying that the main mode of failure is at the clip-rib connection. Excessive vibratory deflections at the panel-ribs might lead to rib-clip disengagement by either clip slippage as a result of seam opening or rupture, which can lead to blow-off of the metal panels. During the high wind speed experiments, two types of panel-rib/clip vibrations were observed (1) vertical vibrations as a result of the unsteadiness in the vertical component of the tension forces exerted by the panels on the metal clips (shown by F_{V-1} and F_{V-2} in Fig. 12), and (2) lateral vibrations created due to the unevenness in the lateral forces exerted by adjacent metal panels (i.e. $F_{L-1} \neq F_{L-2}$ in Fig. 12). As a result of the lateral forces, the clips are observed to rotate about their attachment points. It should be noted that neither the vertical nor the horizontal vibrations can be simulated in uniform uplift pressure tests, this might distort the true picture of experience wind load and failure mechanisms. Note that correct simulation of the actual failure mechanism is important because the component that fails first and its mode of failure determine the ultimate strength of the system [48].

Figure 12: Forces experienced by a clip, trapezoidal roof

In the vertical-leg roof even though panel pan (mid-panel) deflections of more than 110 mm (4.33 in.) were observed at the edge panels, no apparent permanent deformation or attachment failure was observed for the full range of wind speeds and directions tested. Similarly, in the trapezoidal roof, panel pan deflections of up to 190 mm (7.5 in.) were recorded without any visible roof failure. These show the ability of standing seam metal roofs to experience high elastic deflections. Failure of the trapezoidal roof was encountered at a low-eave height peak 3 sec gust speed of 64.9 m/s (145.2 mph). For wind direction of 85°, the roof failure occurred at the free end of the upwind edge panel. The 64.9 m/s (145.2 mph) wind speed experiment was repeated for 275° wind direction and roof failure occurred at a similar position. In both cases, the mode of failure was clip breakage/rupture (Fig. 13), which is assumed to be due to fatigue resulting from excessive vibrations at the panel-rib to clip connection. The clip breakage caused panel-rib to clip separation at the high eave corner which weakened the rigidity of the seam line, and led to failure propagation along the high eave edge as well as down the roof slope. As a result, initiation of seam disengagement at the clips adjacent to the ruptured clip was observed (Fig. 14). Other type of failure observed on the trapezoidal roof include panel rib rupture (Fig. 15).

Figure 13: Clip rupture at the free high eave corner, Trapezoidal Roof

Figure 14: Initiation of seam line disengagement, Trapezoidal Roof

Figure 15: Panel-rib rupture, Trapezoidal Roof

Even though the highest roof uplift pressure was measured at the low eave corner region, failure occurred at the high eave corners because the high eave edge was not fastened. To check whether roof failure occurred because of the free boundary condition at high eave side, the free high eave ends of the two edge panels were fastened to the purlin (Fig. 16) and the testing at the wind speed causing failure and critical wind direction was repeated. When the end panel's high-eave and low-eave ends were fastened, the shape of the uplift deflection changed and the uplift force was concentrated on the two middle clips of the upwind edge panel, and eventually clip-slippage occurred at the middle clip near the high eave end (Fig. 16). Moreover, under this boundary condition the deflections of the metal panel adjacent to the upwind edge panel was observed to be higher than when the edge panel was free. This might be due to the fact that, the edge panel, in its fixed- free boundary condition might have been acting as an

aerodynamic shield to its neighboring panel. At high deflections the roof geometry changes can affect the mean pressure distribution and uplift forces experienced.

Figure 16: Clip failures with free (upper) and fixed (lower) edge panel boundary conditions

6. Comparison of WOW and ASTM E1592 results

The roof panel deflections and failure modes observed in the WOW experiments on the vertical-leg were compared with results from ASTM E1592 experiment carried out on a 4 span at 1.52 m (5 ft.) O.C. vertical-leg roof. The WOW trapezoidal roof results were compared with ASTM E1592 experiment conducted on a 3 span at 1.52 m (5 ft.) O.C. trapezoidal roof. The ASTM E1592 experiments were conducted at Force Engineering & Testing Inc. The panel material, panel width, type of clips and panel boundary conditions of the compared metal roofs were similar, but the methods of wind loading application were entirely different. As previously described, the WOW experiments were conducted by applying simulated wind load at incremental wind speeds, whereas the ASTM E1592 testing was performed under incrementally applied uniform pressures. The testing protocol followed in the WOW is explained in previous sections. In the ASTM E1592 experiments, the system was initially preloaded to a pressure of 239.4 Pa (5.0 psf) to ensure proper seating of the panels, and initial deflection readings were performed (deflection measurement positions are shown in Fig. 17). Pressure was then applied at specific increments for 60 sec. duration.

After each load duration the system was allowed to return to normal pressure, this was continued until the system reached ultimate failure. Deflection measurements were taken during each step of pressure loading, and also during the intervals between loadings when the pressure was returned to normal. A New York Blower was used to apply the required pressure, and Aluminum-ruler deflection indicators were used to measure mid-panel and panel-rib deflections.

Figure 17: ASTM E1592 Test setup: a. Vertical-leg and b. Trapezoidal roof

While the pressure used in uniform uplift tests (e.g. ASTM E1592 test) is clearly defined, it is difficult to accurately represent realistic wind pressures on a roof surface by a single pressure value [7]. The spatial and temporal variation of real wind loading complicates computation of the exact wind pressure or force associated with a roof cladding deflection. Furthermore, load-sharing and uncertainties involved with rigidity of panel rib-clip

and/or clip-purlin attachments make the problem indeterminate and very difficult to compute analytically. It is important to mention that while the results of the ASTM E1592 testing can be applied to different roof shapes, the results of this WOW experiment can only represent buildings of geometrical shapes similar to the test model. As a result, the comparison provided here applies only to specific buildings with low mono-sloped roofs.

It was mentioned earlier that in the WOW tests, the deflections at the edge roof panels were significantly larger than the deflections experienced by the interior panels. Particularly, when the edge panels were at the upwind position and situated normal to the approaching wind they experienced the highest deflections. Since the edge panels were not instrumented with deflection measurement devices, snapshots taken from video recordings were used to estimate their deflections. Large numbers of snapshots were taken from the experiment video recordings and the mid-panel deflections were estimated relative to the known panel rib heights as shown in Figure 18. For instance, in the vertical-leg roof (Fig. 18), the average of image panel rib heights r_{h-1} and r_{h-2} represents the actual rib height of 50.8 mm (2 in.), from which the actual height represented by the image height Y can be computed by linear scaling. Although the deflections computed using this method might not be of the highest accuracy, they can serve as good estimates of the edge panel deflections and have been used in the WOW versus ASTM E1592 comparisons. The deflections measured using this procedure are the mid-panel deflections at the extreme open end of the edge panels for the critical wind direction of 275° .

Figure 18: Snapshot for estimating edge panel deflection

The comparison of the two testing methods was performed under the assumption that the ASTM E1592 test pressures are interpreted (or designed) using pressure coefficients provided by the ASCE 7-10. Under this assumption, 3 sec gust eave height wind velocities ($V_{h, 3 \text{ sec}}$) equivalent to the uniform pressures used in the ASTM E1592 were computed using Eq. (5):

$$V_{h, 3 \text{ sec}} = \sqrt{\frac{P}{\frac{1}{2} \rho K_d K_{zt} GC_p}} \quad (5)$$

where P is the uniform pressure applied during ASTM E1592 testing, and GC_p represents net pressure coefficient values as provided by the ASCE 7, K_d and K_{zt} are directionality and topographic factors which are taken as 0.85 and 1 respectively. It can be seen from Eq. (5) that an equivalent wind velocity corresponding to a uniform ASTM E1592 testing pressure used in the ASTM E1592 depends on building geometry and roof zone. According to the ASCE 7-10, the model used in the WOW testing has three roof zones i.e. zone 3', zone 3 and zone 2' with net GC_p of -2.78, -1.98 and -1.78 respectively. Comparisons of deflections in roof zone 3' and zone 3 for the vertical-leg and trapezoidal roofs are shown in Figure 19 and Figure 20 respectively. Both figures show that for every equivalent wind speed, the mid-panel and panel-rib deflection in the ASTM E1592 tests are higher than those of the WOW. Even though the edge panel deflections in the WOW were significantly higher than the interior panel deflections, they were still less than the ASTM E1592 deflections.

Figure 19: Comparison of WOW and ASTM E1592 deflections: vertical-leg roof

Figure 20: Comparison of WOW and ASTM E1592 deflections: trapezoidal roof

In an effort to further compare the two testing methods, a case study was conducted assuming the WOW building model to be situated in Miami, FL (i.e. using ultimate 3 sec gust wind speed of 78.2 m/s (175 mph) at 10 m

(33 ft.) height). The WOW and ASTM E1592 results were then used to evaluate the serviceability and strength adequacy of the tested standing seam metal roofs according to the requirements set by the FBC 2010 [49]. The pressure at which deflection requirement should be checked at (i.e. P_{def}) is computed using Eq. (6)

$$P_{def} = d_f * a_f * \left(\frac{1}{2} \rho K_z K_{zt} K_d G C_p V_{10m,3sec}^2 \right) \quad (6)$$

where d_f is a factor of 0.7 which according to the FBC 2010 specification is allowed to be used when checking deflection requirements, and a_f is a factor which converts the ultimate pressure to design pressure and it's taken as 0.6. For a building located in Miami, P_{def} is equal to -3.2 kPa (-66.1 psf) and -2.3 kPa (-47.1 psf) for roof zone 3' and zone 3 respectively. In the WOW tests, the equivalent 3 sec gust wind velocity at which the deflection requirements should be checked at (i.e. $V_{h,3sec}^d$) is computed using Eq. (7). For a building located in Miami, $V_{h,3sec}^d$ is calculated as 43.1 m/s (96.3 mph).

$$V_{h,3sec}^d = \sqrt{d_f * a_f} * \left(\sqrt{K_d K_{zt} K_z} * V_{10m,3sec} \right) \quad (7)$$

According to the FBC 2010, the rib deflections at P_{def} and $V_{h,3sec}^d$ should be less than $l/240$, using l of 1.52 m (5 ft.) the panel-rib deflection limit requirement for these roof models is set at 6.35 mm (0.25 in.). Table 2 shows mid panel-rib deflections at P_{def} and $V_{h,3sec}^d$ in both ASTM E1592 and WOW tests. The values are computed using linear interpolation. The deflections shown for the ASTM E1592 are measured at position 5, while those of the WOW are measured at SP 3. The results show that for the case of ASTM E1592, both roof types surpassed the FBC 2010 deflection criteria, but in the WOW test the FBC 2010 criterion was exceeded in the trapezoidal roof only.

		Deflection, mm (in.)	
		Vertical-leg roof	Trapezoidal roof
ASTM E1592	Zone 3'	34.79 (1.37) ^a	33.33 (1.31) ^b
	Zone 3	19.88 (0.78)	35.56 (1.40)
WOW	Mean	0.76 (0.03)	2.54 (0.10)
	Maximum	3.56 (0.14)	6.60 (0.26)

^a permanent deformation of 8.9 mm (0.35 in) has already occurred

^b permanent deformation of 20.9 mm (0.8215 in) has already occurred

Table 2: Deflections at P_{def} and $V_{h,3sec}^d$

The pressure at which strength requirement is checked (i.e. P_{st}) can be calculated using Eq. (8)

$$P_{st} = S_f * a_f * \left(\frac{1}{2} \rho K_z K_{zt} K_d G C_P V_{10m,3sec}^2 \right) \quad (8)$$

where S_f is factor of safety and which is taken herein as 2 and a_f is taken as 0.6. Note that S_f can either be multiplied to P_{st} or be divided from the experiment roof failure pressures. For roof zones 3' and 3, P_{st} is computed as -9.1 kPa (-189.0 psf.) and -6.4 kPa (-134.6 psf.) respectively. The equivalent 3 sec eave height wind velocity at which the strength requirements should be checked at (i.e. $V_{h,3sec}^{st}$) is computed using Eq. (9). For Miami, $V_{h,3sec}^{st}$ is equal to 72.9 m/s (163.0 mph).

$$V_{h,3sec}^{st} = \sqrt{S_f} * \sqrt{a_f} * \left(\sqrt{K_d * K_z} * V_{10m,3sec} \right) \quad (9)$$

According to the FBC 2010 requirement, the roofs should be able to stay intact up to those pressures and/or equivalent wind speeds. In the ASTM E1592 test, ultimate roof failure occurred at uniform pressures of -5.2 kPa (-109.0 psf) and -3.6 kPa (-75.0 psf) in the vertical-leg and trapezoidal roof respectively. In the WOW, roof failure was observed in the trapezoidal roof at a low-eave height peak 3 sec gust, $V_{h,3sec}$ of 64.9 m/s (145.2 mph), but in the vertical-leg roof failure was not observed up to $V_{h,3sec}$ of 64.9 m/s (145.2 mph). The equivalent 3 sec failure wind speed for both tests are shown in Table 3. The strength requirement is unsatisfied in all cases except the WOW's

vertical-leg roof where no deduction can be made since the roof failure was not encountered up to 64.9m/s (145.2 mph). For the purpose of comparing dynamic pressure test results to uniform pressure results, Sinno 2005 [45] created an index factor which can be approximated by the square of the ratio of roof failure velocity in dynamic testing to roof failure velocity in uniform pressure testing. In the vertical-leg roof since no roof failure was observed in the WOW an index factor couldn't be established, but for the trapezoidal roof index factors were 1.69 and 1.21 for zone 3' and zone 3, respectively. Index factor larger than one suggests conservativeness of the uniform uplift test.

		Failure Wind speed, m/s (mph)	
		Vertical-leg roof	Trapezoidal roof
ASTM E1592	Zone 3'	58.6 (131.1)	49.8 (111.3)
	Zone 3	71.1 (159.0)	59.0 (131.9)
WOW		> 64.9 (145.2)	64.9 (145.2)

Table 3: Failure wind speeds

Different modes of roof failures were observed in the two methods of testing. In the ASTM E1592 tests, panel buckling occurred first and the main mode of roof failure was usually clip slippage. But in the WOW as explained in details in the previous section failure of the trapezoidal roof occurred at the first span from the free end and the main mode of roof failure was clip rupture. The clip rupture failure in the WOW occurred at deflections less than the maximum deflections experienced in the ASTM E1592 test. By the time the trapezoidal roof clips failed, they had been subjected to intermittent low and high deflection vibrations which lasted for more than 1.2 hr. in the WOW. Moreover, the discontinuity at the open end allowed a higher degree of vibration than would be seen in real world application of the roof system. Similar clip breakage failure was also encountered in Sinno 2005 [45].

7. Conclusions

An experimental investigation was conducted with the objective of evaluating the structural performance of standing seam metal roofs under high-wind speeds. Full-scale investigations involving wind-induced roof surface pressure measurements and roof panel deflection measurements were conducted under realistic wind loading in WOW facility at FIU. The deflection results were also compared with results from experiments conducted by following the

conventional ASTM E1592 method. The following points summarize the major observations and conclusions of the research work:

- Wind-induced pressure loading on the surface of the metal roofs is highly non-uniform and dependent on the wind direction. For mono-sloped metal roofs, the corner and edge regions experience the highest suctions. Although an identical base structure and roof slope were used, the magnitude of wind-induced pressures on the surface of the trapezoidal roof were significantly higher than on the vertical-leg roof. This led to the conclusion that roof geometrical profile and eave details significantly affect the nature of wind pressures on standing seam roofs. The comparison of WOW pressures with ASCE 7-10 provisions showed that the wind loading provisions in ASCE 7-10 might underestimate the wind loading on a mono-sloped trapezoidal-leg standing seam roof.
- The finding that the roof geometrical profile and eave details significantly affect the magnitude of wind suctions on standing seam metal roofs leads to the possibility of alleviating roof suction by employing aerodynamically favourable profiles. Further design optimization studies may be warranted to assess the aerodynamic behaviours of various standing seam profiles and eave details.
- Relatively higher deflections were recorded on the trapezoidal roof as a result of the larger panel rib spacing and higher suctions experienced. In both roofs, under the free-fixed boundary conditions, the highest deflections were observed in the free end of the edge panels when the longer side of the panels was perpendicular to the approaching wind. The roof panel deflection spectral analysis and high wind speed tests showed that after a certain amount of deflection, the fundamental frequency of vibrations of a roof panel increases meaning that the metal panel assembly becomes increasingly stiff before failure.
- When compared with the WOW, the ASTM E1592 testing protocol produced more conservative deflections. For the vertical seam roof the present tests did not show any signs of failure up to the maximum speed tested which was consistent with expectations based on the ASTM test for Zone 3'. For the trapezoidal roof failure did occur but at a speed somewhat higher than expected from the ASTM test. The mode of failure in the WOW tests was clip rupture rather than the clip slippage seen in the ASTM tests and this was attributed to the vibration observed in the WOW tests. Vibrations were observed on all parts of the roof in the WOW tests but they were probably exaggerated at the free end of the roof (i.e. where clip

rupture occurred) due to the unrestrained end condition. Note that this unrestrained end condition was selected to mimic the end condition used in the static ASTM tests. Nevertheless, the results highlight the importance of testing under cyclic vibratory loads and warrant further investigation on the impact of wind induced vibrations and possibility of fatigue failures under low, but long lasting winds (which was also reported as the main reason for widespread damage of metal roofs during Cyclone Tracy (Morgan 1976) [50]).

The results from this study have highlighted the need for more experimental investigation on wind loading and uplift-capacity of standing seam metal roofs to provide additional information to enhance the existing testing standards. For the same terrain conditions, although insignificant deviation in uplift pressure at the upwind roof corner and edge regions is expected as the roof plan dimensions change (this was shown by several researchers including Lin et al (1995) [32] and Kopp et al (2005) [39]), effects of roof height might exist. Hence, future research is recommended to investigate the effect of roof height and various geometric profiles and eave details on pressures on standing seam metal roofs. Use of large-scale models (about 1:10) is being contemplated for exploring these effects.

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Appendix A: Partial Turbulence Simulation (PTS) for Predicting Peak Pressure Coefficients

Summary of the Theory

In this paper a method called “Partial Turbulence Simulation (PTS)” is used to estimate peak pressure coefficients. In this method, the turbulence is divided into two distinct statistical processes, one at high frequencies which can be simulated in wind tunnel, and one at low frequencies which can be treated in a quasi-steady manner. It can be shown that in a partially simulated turbulence spectrum with turbulence intensity I_{uH} the intensity of the missing low frequency I_{uL} is:

$$I_{uL} = \sqrt{I_u^2 - I_{uH}^2} \quad (A1)$$

where I_u is the full-spectrum longitudinal turbulence intensity.

Although in reality there is not any sharp dividing line between the low and high frequency turbulences, it can be theoretically shown that Eq. (A2) can be used for approximately calculating an equivalent cut-off frequency, n_c , between the simulated high frequency and the missing low-frequency turbulence.

$$n_c = 0.0716 \frac{U}{^xL_u} \left(\frac{I_u}{I_{uH}} \right)^3 \quad (A2)$$

where the xL_u and U are the full spectrum values of longitudinal integral scale and the mean velocity respectively. This means that in a partial turbulence simulation, turbulence fluctuations at frequencies above n_c can be simulated.

Method for predicting peak pressure coefficients

In the PTS method for predicting peaks the sample period is divided into subintervals of sufficient duration so that they can be treated as independent events. The probability that the peak pressure coefficient will not exceed a given value in a subinterval can be measured using Fisher Tippet Type I distribution. The probability distribution of wind turbulence in a generic boundary layer (i.e. one free from local aerodynamic effects of upwind structures) is approximated as Gaussian. To calculate the expected peak pressure coefficients ($\hat{C}_p$), the joint probability of load from the two processes is derived, with one part coming from the WOW data and the remainder from the Gaussian behaviour of the missing low frequency component. It can be shown that the probability that a given value of $\hat{C}_p$ will be exceeded in a particular subinterval in the full turbulence can be obtained from the following equation:

$$G\left(\frac{\hat{C}_p}{b}\right) = \int_{-\infty}^{\infty} \frac{1}{\sqrt{2\pi}} e^{-\frac{1}{2}\xi^2} \left[1 - \exp \left[- \exp \left[- ab \left(\frac{\frac{\hat{C}_p}{b}}{(1 + I_{uL}\xi^2)} - 1 \right) \right] \right] \right] d\xi \quad (A3)$$

were $\xi \equiv \frac{u_L}{U}/I_{uL}$ in which U is the mean flow velocity and u_L is the part of the fluctuating velocity contributed by the low frequency end of the spectrum. The parameters a and b can be measured from time histories of pressures in partial simulations. Then, given the turbulence intensity of the low frequency fluctuations, I_{uL} , the probability of exceeding a given value of $\hat{C}_p/b$ in a single subinterval can be computed by numerical integration as shown in Eq. (A3).

If full sample period is one hour in which we have N subintervals then the highest pressure coefficient will have probability $1/N$ of being exceeded. So, if our subintervals on the model scale up to be $t_{gust,p}$ seconds at full scale then we need to determine the value of $\hat{C}_p/b$ that has $t_{gust,p}/3600$ probability of being exceeded in one hour. The cut-off frequency as calculated from Eq. (A2) can be used to estimate the equivalent gust-duration at full scale using: $t_{gust,p} = 0.45/n_c$.

Note that the probability G in the way calculated above gives the peak pressure coefficients with 37% probability of not being exceeded in the hour of wind. To convert it to other probability values like P_{tar} , the following equation can be used:

$$G = 1 - P_{tar}^{1/N} \quad (A4)$$

Scaling parameters

The speed scaling was set as the ratio of the mean speed in PTS to the full-scale gust due to missing low frequency turbulences which can be estimated using the peak factor of 3.4 used in ASCE 7 for background turbulence:

$$\hat{U}_{LP} = U_p (1 + 3.4 I_{uL}) \quad (A5)$$

Having the length scale as $\frac{b_p}{b_m}$ and speed scaling as $\frac{U_m}{U_{LP}}$, the frequency scale comes from $\frac{n_m}{n_p} = \frac{U_m}{U_p} \frac{b_p}{b_m}$. So the time scale will be $\frac{t_m}{t_p} = \frac{n_p}{n_m}$. The subscripts m and p denote the model and prototype respectively.

It should be noted that the number of subintervals used in calculating the a and b parameters in the Fisher Tippet Type I distribution should be limited by the requirement that no correlation should exist between events in adjacent subintervals. To calculate the maximum number of subintervals first the gust-duration at model scale is calculated from $t_{gust,m} = t_{gust,p} \times \frac{t_m}{t_p}$. The maximum number of subintervals can then be calculated from

$$\frac{\text{Test duration in PTS}}{t_{gust,m}}.$$

Calculation for the current problem

Table A1 shows the required flow parameters in the PTS method for both the experiments in WOW and full-scale.

	Full-scale	WOW partial turbulence simulation
Turbulence intensity	$I_u = 22\%$	$I_{uH} = 6.7\%$

Integral length scale (L_u^x)	15 m	0.5 m
Reference height	$Z_{\text{ref}} = 1.52$ m	$Z_{\text{ref}} = 1.52$ m
Mean wind speed	$U_p = 24.7$ m/s	$U_m = 24.7$ m/s
Test time	$t_p = 60$ min	$t_m = 1$ min

Table A1: Required flow parameters for the PTS method

- $I_{uL} = \sqrt{I_u^2 - I_{uH}^2} = 21\%$
- $\hat{U}_{LP} = U_p(1 + 3.4I_{uL}) = 24.7 \times (1 + 3.4 \times 0.21) = 42.33$ m/s
- Velocity scaling is: $\frac{U_m}{U_p} = \frac{24.7}{44.92} = 0.583$
- Frequency scale is: $\frac{n_m}{n_p} = \frac{U_m}{U_p} \frac{b_p}{b_m} = 1 \times 0.58 = 0.58$
- Time scale is: $\frac{t_m}{t_p} = \frac{n_p}{n_m} = 1.71$
- Cut-off frequency: $n_c = 0.0716 \frac{U}{L_u^x} \left(\frac{I_u}{I_{uH}} \right)^3 = 0.0716 \times \frac{24.7}{15} \times \left(\frac{0.22}{0.067} \right)^3 = 4.17$ Hz
- Equivalent gust-duration at full scale: $t_{gust,p} = \frac{0.45}{4.17} = 0.1$ (sec.) $\rightarrow t_{gust,m} = 0.18$ (sec.)
- Maximum number of sub intervals without correlation between events in adjacent subintervals: $1 \times \frac{60}{0.18} = 325$

For calculating the required G, 150 subintervals is used:

- Each subinterval in WOW=60/150=0.4 sec
- Each subinterval at full scale= 0.4/1.71=0.23 sec at full scale
- Targeted probability for 60 min full spectrum is $G = \frac{0.23}{60 \times 60} = 0.000065$

$$G_{95\%} = 1 - 0.95^{0.000065} = 0.0000033 \text{ (G for 95\% probability of not being exceeded in the hour of wind)}$$

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