Towards LES as a design tool: Wind loads assessment on a high-rise building

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ABSTRACT

The accurate evaluation of wind loads on high-rise buildings represents a key point in their design process. Traditionally, atmospheric boundary layer wind tunnel tests, conceived in order to be representative of the wind conditions expected on site, are used for this purpose. Recently, owing to the increase in computational power, Computational Fluid Dynamics (CFD) techniques have gained interest as a complementary tool to wind tunnel testing. Unfortunately, wind flow around bluff bodies is often very complex and substantial research efforts are still needed in order to assess the accuracy and reliability of CFD results. In this paper, Large Eddy Simulations (LES) are performed aimed at evaluating the wind loads on an isolated high-rise building. In order to assess the capabilities of LES for adoption as a design tool, the results are analysed in terms of both pressure distributions and internal forces on the structural elements. It is found that the accuracy of LES in reproducing the fluctuating pressure field is not necessarily maintained when internal forces are taken into account. Nevertheless, the design values predicted by LES can be still considered satisfactory, in particular when maximum and minimum values over different angles of attack are considered.

1. Introduction

The new generation of high-rise buildings is tending towards taller and more slender structures, while their shapes are becoming increasingly complex and often unconventional. Due to these characteristics, particular attention has to be paid to the design of these structures, which have to face increasing environmental loads while still remaining as light as possible. In particular, wind loads on tall buildings can play a fundamental role in the design process and have to be accurately assessed with respect to both structural integrity and serviceability (Irwin, 2009).

The turbulence naturally present in the Atmospheric Boundary Layer (ABL), together with aerodynamic phenomena typical of bluff bodies like vortex shedding and intermittent reattachment of shear layers, causes the structure to experience dynamic forces which lead to along-wind and across-wind vibrations.

The traditional approach for the assessment of wind loads on high-rise buildings strongly relies on atmospheric boundary layer wind tunnel practice. The most commonly adopted experimental techniques can be subdivided into three main methodologies (Irwin, 2009). The High-Frequency Force Balance (HFFB) method, initially developed by Tschanz et al. (Tschanz and Davenport, 1983), represents one of the first

approaches. It satisfied the need for a relatively simple technique able to evaluate the structural response in a reduced amount of time, without employing expensive aeroelastic models and without introducing simplifications typical of analytical approaches that are not generally applicable (Davenport, 1961; Kareem, 1984; Kareem and Zhou, 2003). The main characteristic of this method is that the structural response can be reconstructed by measuring only forces and moments at the building base. In this experimental approach, a rigid model of the building is mounted on a balance characterised by high stiffness and high sensitivity, which records time histories of shear forces, torque and bending moments. The structural response can be evaluated by post-processing results in the frequency domain (Chen and Kareem, 2005) or in the time domain (Xie and Irwin, 1998).

It should be noted that methodologies based on the use of HFFB measurements always imply the introduction of assumptions with respect to the structural behaviour and/or the pressure distributions. For this reason the HFFB method can perform well only for the fundamental modes of the structure (Zhang et al., 2015), while high frequency mode effects can not be satisfactorily predicted and structures characterised by unconventional geometries cannot be easily studied. Furthermore, in order to avoid inertial effects the model should be as stiff as possible. This

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requirement might be hard to respect, in particular when dealing with very slender tall buildings.

The second experimental methodology commonly adopted to assess wind loads on tall buildings is the High-Frequency Pressure Integration (HFPI) method. Developed by Irwin et al. (Irwin and Kochanski, 1995), this approach consists of equipping the exposed surfaces of the building model with a number of pressure taps, closely spaced in order to accurately sample the fluctuating pressure field acting on it at each sampling time. Differently from the HFFB method, no assumptions on the modal shapes or on the spatial distribution of the pressure field are necessary, so that wind effects on structures with irregular distributions of stiffness and mass and complex modal shapes can be analysed. Indeed, modal forces can be reconstructed directly from pressure measurements and the structural response can be consequently assessed. Furthermore, again differently from the HFFB method, the pressure measurements are not affected by inertial forces caused by the model itself. On the other hand, buildings with complex geometrical shapes would require a very large number of pressure taps acquiring data simultaneously and, sometimes, the limited number of available taps might lead to inaccurate sampling of the pressure field.

The third experimental methodology for wind load assessment on high-rise structures involves the use of aeroelastic models. In this case, the model is intended to reproduce the dynamic properties of the full-scale structure, approaching its modal shapes up to a certain natural frequency as closely as possible. The adoption of aeroelastic models allows measurement of the full response of the structure, taking into account aeroelastic effects like aerodynamic damping. By adopting such an approach, complex fluid-structure interactions can be experimentally investigated. Nevertheless, due to its high costs, this technique is usually limited to the study of structures for which aeroelastic effects are expected to be of primary importance. Despite the advantages of aeroelastic models, in many cases, the HFPI method represents a good alternative and a satisfactory trade-off between accuracy and complexity of the experimental tests.

In the last decades, thanks to the significant growth of available computer power, numerical approaches based on Computational Fluid Dynamics (CFD) have become more and more adopted as a complementary tool to investigate wind flow around buildings (Blocken, 2014). Numerical methods have several advantages compared to wind tunnel tests. In particular, the costs of CFD analyses are generally lower than those of experiments and each quantity of interest can be measured everywhere in the computational domain rather than sampled at just a few points. Due to these characteristics, numerical simulations allow the study of phenomena which might be difficult to analyse in wind tunnel tests.

CFD simulations based on the Reynolds-Averaged Navier-Stokes (RANS) models have been deeply investigated in the $p5 : 2ast$. However, such models are often found to be inaccurate where wind loads on bluff bodies are concerned and their predictive capability is limited to mean flow properties, while the ability to accurately predict turbulent fluctuations is recognised to be of fundamental importance for the assessment of the dynamic response of structures (Huang et al., 2010). The need to correctly take into account the flow dynamics led researchers to move towards scale-resolving turbulence approaches, such as Large Eddy Simulations (LES), which are promising for the numerical prediction of wind loads on buildings. Although such models are commonly considered to be well suited for the analysis of flows around bluff bodies, it has often been observed that results are considerably scattered when compared to experimental measurements, even when simple geometries are considered (Bruno et al., 2014; Patruno et al., 2016a; Ricci et al., 2016). Indeed, the complex, instability-driven phenomena observed in the turbulent flows around bluff bodies, like shear layer detachment/reattachment and vortex shedding, render the simulation of this kind of flow an extremely challenging task and results are often found to be dependent on the simulation setup and the adopted turbulence subgrid

scale model (Ricci et al., 2016). Additionally, it should be noted that the generation of realistic unsteady boundary conditions, able to reproduce the main features of the turbulence found in the ABL, is imperative in order to obtain accurate results in terms of pressure distributions and, thus, forces acting on the structure. Among other structures, this fact has been observed for high-rise buildings and, in particular, in the framework of the CAARC benchmark (Zhang et al., 2015; Elshaer et al., 2016; Braun and Awruch, 2009), since results of different turbulent inflow generation techniques were analysed in this case (Aboshosha et al., 2015; Huang et al., 2006).

As a result, even if CFD nowadays represents a very attractive tool for the assessment of wind loads on structures, it appears that substantial research efforts are still needed in order to evaluate its reliability and accuracy. In particular, the available literature is focussed mainly on the comparison of pressure distributions and their statistics, typically up to second order. Even if such studies are definitely necessary, they do not appear to be sufficient in order to assess the capabilities of the numerical models in the prediction of wind loads. In fact, internal forces on structural members are related to a number of factors which are often not taken into account in such studies like, for example, the spatial coherence of the pressure field and its frequency content, whose relevance and overall effect can be amplified or weakened depending on the mechanical transfer functions characterising the structural dynamic behaviour itself. A detailed evaluation of LES capabilities in the simulation of flow around low-rise buildings with specific application to structural design has been recently reported in (Ricci et al., 2017).

In this context, the present paper represents an essential step towards the assessment of LES as a design tool for high-rise buildings. In particular, the turbulent fluctuations which characterise the wind impinging on the structure are generated by means of the Modified Discretizing and Synthesizing Random Flow Generator (MDSRFG), which produces a solenoidal fluctuation field and allows control of its spatial and temporal correlations (Castro and Paz, 2013). First, results are analysed in terms of statistics of pressure distributions on the building and systematically compared with wind tunnel measurements. Then, dynamic structural analyses are carried out for each considered angle of attack starting from both numerically predicted and experimentally obtained pressure data. Results are analysed in terms of envelopes of internal forces acting on structural elements, allowing an analysis of the effectiveness of the adopted numerical strategy in predicting design wind loads on the considered high-rise structure. It should be noted that by adopting such a framework, all the elements which play an important role in the definition of the structural response are automatically taken into account, allowing the study of the effectiveness of the proposed numerical strategy compared to the traditional experimental practice.

The paper is organised as follows. In Section 2 the experimental setup adopted to obtain the data used for a comparison with numerical results is briefly described, while Section 3 reports the main features of the adopted computational model together with a discussion on the inflow condition adopted for the LES. Section 4 is devoted to the analysis of pressure distributions for each considered angle of attack. Then, Section 5 describes the results obtained from the dynamic structural analyses, focusing on internal forces on structural elements. Finally, in Section 6 some conclusions are drawn.

2. Experimental setup

In this section the experimental setup adopted to obtain pressure data used for comparison with numerical results is described. Experiments were carried out at the atmospheric boundary layer wind tunnel of the Tokyo Polytechnic University (TPU) and results are collected in a public database organised according to the geometry and the layout characteristics (Tokyo Polytechnic Univ., 2003). The present work focuses on an isolated high-rise building characterised by a height (H) to breadth (B) ratio equal to and a depth (D) to breadth (B) ratio equal to 1 : 2 (see Fig. 1

(a). During wind tunnel tests, the length scale was equal to $1/400$, leading to a model with $B = 200 \text{ mm}$, $D = 100 \text{ mm}$ and $H = 500 \text{ mm}$. The wind tunnel section was 2.2 m wide and 1.8 m high, so that the maximum blockage ratio was less than 2.5%. The wind profile reproduced in the wind tunnel corresponded to that of terrain category IV according to the Architectural Institute of Japan (AIJ) standards (Architectural Institute of Japan, 2004):

$$U(z) = 1.7 \left(\frac{z}{Z_G} \right)^\alpha U_{ref}, \quad Z_b < z \leq Z_G, \quad (1)$$

$$U(z) = 1.7 \left(\frac{Z_b}{Z_G} \right)^\alpha U_{ref}, \quad z \leq Z_b, \quad (2)$$

where the exponent α is equal to 0.25, Z_G is the reference height of the ABL equal to 550 m , and Z_b represents the characteristic dimension of the surface roughness elements equal to 20 m . U_{ref} is the reference wind velocity at reference height. All the quantities relative to the AIJ standards are intended to be in full scale. In the experiments, the wind velocity at the height of the building was equal to $U_H = 11.11 \text{ m/s}$, leading to a Reynolds number equal to $Re = \frac{U_H H}{\nu} = 3.8 \cdot 10^5$.

The adopted turbulence intensity profile was in agreement with AIJ standards for terrain category IV as reported in Eqs. (3) and (4):

$$I(z) = 0.1 \left(\frac{z}{Z_G} \right)^{-\alpha-0.05}, \quad Z_b < z \leq Z_G, \quad (3)$$

$$I(z) = 0.1 \left(\frac{Z_b}{Z_G} \right)^{-\alpha-0.05}, \quad z \leq Z_b. \quad (4)$$

The mean velocity and turbulence intensity profiles were obtained by means of spires and square blocks as roughness elements. Experiments were conducted for 21 wind directions, from $\theta = 0^\circ$ to $\theta = 100^\circ$ with 5° increments. The angle of attack θ is defined in an anti-clockwise sense around the vertical axis (z) with $\theta = 0^\circ$ corresponding to the direction orthogonal to the edge D , as reported in Fig. 1 (b). The model was equipped with 510 pressure taps that acquired data synchronously at a sampling frequency of 1000 Hz for a duration of 32.8 s per wind direction.

3. Computational model

In the present section the characteristics of the numerical model adopted for LES are described. In particular, Section 3.1 describes the computational domain characteristics and the numerical settings, while Section 3.2 focuses on the generation of the turbulent inflow data.

Fig. 1. Geometry of the high-rise building: (a) three-dimensional and (b) top view.

3.1. Domain, grid and solver settings

The computational domain dimensions in the across-wind section are close to those of the wind tunnel, although slightly reduced with respect to them. A three dimensional view of the adopted computational domain is presented in Fig. 2, while Fig. 3 (a) and Fig. 3 (b) show lateral and top views, respectively. The across-wind section is $4.4H$ wide and $3.6H$ high while the distance of the building from the inlet boundary is equal to $3.6H$. The resulting blockage ratio at 0° angle of attack is equal to 2.5%, while at 90° it equals 1.25%; in both cases it is lower than the maximum of 3.0% suggested by the COST guidelines (Franke et al., 2007). In accordance with the recommendation by Tominaga et al. (2008) the distance of the high-rise building from the outlet boundary is set equal to $10H$.

Aiming to reduce as much as possible the along-wind deterioration of profiles imposed at the inlet (Blocken et al., 2007), five staggered rows of square blocks with an edge length equal to $0.06H$ are placed upstream of the model. The block distribution is the same as that adopted during the wind tunnel tests. In order to check the aerodynamic roughness related to the adopted layout and the geometry of the blocks, the following equation proposed by Lettau is used (Lettau, 1969):

$$z_0 = 0.5h \frac{A_r}{A_t}, \quad (5)$$

where z_0 is the aerodynamic roughness length as defined by the European standard for wind actions on structures EN1991-1-4:2005 (EN 1991-1-4, 2005), A_r is the area of the element normal to the wind direction and A_t is the ground area per roughness element, as reported in Fig. 4 (a).

Fig. 2. Computational domain adopted for the numerical study: three-dimensional view.

Fig. 3. Computational domain adopted for the numerical study: (a) lateral and (b) top views.

Fig. 4. Three-dimensional view of (a) the roughness block geometry and (b) boundary layer height development according to Elliot's equation (6) (Elliott, 1958).

The resulting roughness length is about 0.3 m in full scale and is in good agreement with both the roughness length of terrain category IV in EN1991-1-4:2005 (EN 1991-1-4, 2005) and with that considered in the present study.

When the roughness fetch ends a new boundary layer starts to develop which corresponds to a lower roughness length. This new boundary layer might affect results at least in the lower part of the high-rise building. Its height can be estimated with Eq. (6) proposed by Elliot (Elliott, 1958):

$$H_{bl} = z_{0,2} \left(0.75 + 0.03 \log \left(\frac{z_{0,1}}{z_{0,2}} \right) \right) \left(\frac{d}{z_{0,2}} \right)^{0.8}, \quad (6)$$

where H_{bl} is the height of the new boundary layer, $z_{0,1}$ and $z_{0,2}$ are the aerodynamic roughness lengths characterising the two different zones and d is the distance measured from the last roughness element. The distance between the roughness blocks and the model is chosen in order to limit the new boundary layer height to $H/10$, which according to this equation gives $d = 1.4H$ (see Fig. 4 (b)).

With respect to the boundary conditions; the mean velocity profile is prescribed at the inlet boundary, while Neumann conditions on the velocity field are imposed at the outlet. The turbulent part of the inflow is generated by means of the MDSRFG method, discussed in more detail in the next section. Bottom surface, building and roughness blocks surfaces are modelled as smooth walls, while symmetry boundary conditions are imposed on the other domain boundaries.

A structured mesh is adopted near the high-rise building surfaces, as shown in Fig. 5 (a), where cell dimensions in x , y and z directions are respectively $\delta_x/H = \delta_y/H = \delta_z/H = 3.1 \times 10^{-3}$, leading to a resolution higher than that suggested by Tominaga et al. (2008). At the wall a structured mesh is adopted for the boundary layer with a first cell height of $\delta_z/H = 5.1 \times 10^{-4}$. Proceeding away from the building the mesh is slowly coarsened up to $\delta_x/H_0 = \delta_y/H_0 = \delta_z/H_0 = 5.0 \times 10^{-2}$. This sizing is kept constant until the inlet boundary is reached in order to correctly propagate inflow fluctuations and to keep the numerical dissipation caused by the mesh as low as possible. A view of the mesh of the roughness blocks and the building can be observed in Fig. 5 (b). The final mesh contains about 1.2×10^7 cells.

Pressure-velocity coupling is performed by means of the PISO algorithm, modified as proposed by Kim et al. (2013) in order to obtain a

divergence-free velocity field at the inlet. Time discretisation is performed with the second order accurate backward differentiation scheme. The adopted dimensionless time step value, based on H , is $\Delta t^* = \frac{\Delta U H}{H} = 4.4 \times 10^{-3}$. The maximum Courant-Friedrichs-Lewy number in all simulations is 2.9, while it is lower than 1.0 in 97% of the domain and its mean value in the whole domain equals 0.06. The maximum dimensionless wall distance y^+ is equal to 9.0, while it is lower than 5.0 on 94% of the high-rise building surface.

Spatial discretisation of the advective terms is performed by means of the LUST scheme, which is second-order accurate and has been shown to perform well in particular for LES in complex geometries (Weller, 2012), offering a good trade-off between low dissipative behaviour and numerical stability. For all the other terms of the equations a centered second-order differentiation scheme is adopted.

Regarding the LES subgrid scale model, the Smagorinsky-Lilly model (Lilly, 1992) is adopted with an additional transport equation for the subgrid turbulent kinetic energy. This model is able to adjust the turbulent eddy viscosity depending on the subgrid kinetic energy, showing a less dissipative behaviour compared to the standard Smagorinsky-Lilly model (Ricci et al., 2016).

The high-rise building is equipped with 2844 pressure monitors and data are acquired at each time step. Simulations are performed with the open source Finite Volume software OpenFOAM® v. 2.3.0 using 96 cores of the Galileo cluster at the Italian supercomputing institute CINECA (6 nodes with 2-eight core Intel® Xeon® 2.40 GHz processors with 128 GB RAM per node). Each simulation required about 2.5×10^4 CPU hours.

3.2. Turbulent inflow characteristics

The turbulent part of the simulated atmospheric boundary layer is generated by means of the MDSRFG method (Castro and Paz, 2013). In this method a homogeneous and anisotropic velocity field is computed as:

$$u_i(\mathbf{x}, t) = \sum_{m=1}^M \sum_{n=1}^N \left[p_i^{m,n} \cos \left(k_j^{m,n} \frac{x_j}{L_s} + \omega^{m,n} \frac{t}{\tau_0} \right) + q_i^{m,n} \sin \left(k_j^{m,n} \frac{x_j}{L_s} + \omega^{m,n} \frac{t}{\tau_0} \right) \right], \quad (7)$$

with

Fig. 5. Lateral views (xz plane through $(0,0,0)$) of the adopted mesh: (a) detailed view of the mesh near the building and (b) view of the roughness blocks and the building.

$$p_i^{m,n} = \text{sign}(r_i^{m,n}) \sqrt{\frac{2}{N} S_i(f^m) \Delta f \frac{(r_i^{m,n})^2}{1 + (r_i^{m,n})^2}}, \quad (8)$$

$$q_i^{m,n} = \text{sign}(r_i^{m,n}) \sqrt{\frac{2}{N} S_i(f^m) \Delta f \frac{1}{1 + (r_i^{m,n})^2}}, \quad (9)$$

$$\begin{cases} \mathbf{k}^{m,n} \cdot \mathbf{p}^{m,n} &= 0, \\ \mathbf{k}^{m,n} \cdot \mathbf{q}^{m,n} &= 0, \\ |\mathbf{k}^{m,n}| &= f^m / U_H, \end{cases} \quad (10)$$

where $i = 1, 2, 3$ denotes vector components in x , y and z directions, respectively, while N is the number of random samples generated for each of the M frequencies f^m . $\omega^{m,n}$ is a random angular frequency extracted from a Gaussian distribution $\mathcal{N}(f^m, \Delta f)$, Δf is the frequency increment adopted in the spectra sampling, U_H is the time averaged velocity as defined in Section 2, L_s is a scale factor calibrated *a posteriori* related to the turbulent length scale, and τ_0 is a dimensionless parameter, set equal to one in the present work, that allows control over the time correlation of the series. Parameters $r_i^{m,n}$ are random numbers extracted from a Gaussian distribution $\mathcal{N}(0, 1)$, while $S_i(f)$ is the target spectrum characterising the i -th velocity component whose total variance is equal to $u_i'^2$. For a detailed review of the characteristics of MDSRFG and other methods used for the generation of synthetic turbulence based on the spectral approach the reader is referred to Patruno et al. (Patruno and Ricci, 2017).

In this paper, the well-known von Kármán spectra reported below are assumed for the velocity components (Zhang et al., 2015):

$$S_{u_1}(f) = \frac{4(I_{u_1} U_H)^2 (L_{u_1}/U_H)}{[1 + 70.8(fL_{u_1}/U_H)^2]^{5/6}}, \quad (11)$$

$$S_{u_2}(f) = \frac{4(I_{u_2} U_H)^2 (L_{u_2}/U_H) [1 + 188.4(2fL_{u_2}/U_H)^2]}{[1 + 70.8(fL_{u_2}/U_H)^2]^{11/6}}, \quad (12)$$

$$S_{u_3}(f) = \frac{4(I_{u_3} U_H)^2 (L_{u_3}/U_H) [1 + 188.4(2fL_{u_3}/U_H)^2]}{[1 + 70.8(fL_{u_3}/U_H)^2]^{11/6}}, \quad (13)$$

where I and L are the turbulence intensity and the turbulence length scale at the reference height H , respectively, while the u_1, u_2, u_3 subscripts indicate the components in x, y and z directions. In the present work, I_{u_1} is set equal to 11.6% according to experimental measurements, while

$I_{u_2} = 0.75I_{u_1} = 8.7\%$ and $I_{u_3} = 0.5I_{u_1} = 5.8\%$ (Dyrbye and Hansen, 1999).

The turbulent length scale is not reported in the database for isolated high-rise buildings of TPU (Tokyo Polytechnic Univers, 2003), therefore two different values have been investigated: $L_{u_1}/H = 0.6$ and $L_{u_1}/H = 0.8$. These values were chosen to be close to those reported by Kim and Tamura (2014), who analysed several inflow conditions in the same wind tunnel facility referred to in the present work. It should be noted that the MDSRFG procedure does not allow the *a priori* prescription of the turbulence length scale and, therefore, an iterative procedure is necessary to match the target values. The iterative procedure consists of adjusting the parameter L_s in Eq. (7) until the resulting field matches the desired length scale. At the end of this procedure two synthetic inlet conditions are obtained, hereafter referred to as inflow #1 and inflow #2, that differ only in the target turbulent length scale.

In order to check whether the turbulence introduced in the proximity of the inlet is correctly propagated downstream, two simulations are performed by adopting the two aforementioned inflow conditions. The LES are performed in an empty domain identical to that shown in Fig. 2 but without the building. The numerical setup adopted for these analyses is the same as that described in Section 3.1. Within the computational domain, 21,560 velocity monitors sampling at each time step are arranged in a regular grid ranging from $-2.0 \leq x/H \leq 5.5$, from $-1.0 \leq y/H \leq 1.0$ and from $0 \leq z/H \leq 0.7$.

In order to check the convergence of the LES performed in the empty domain the velocity signal recorded at a point located at building height H and in the location where the building will be placed afterwards is considered. Fig. 6 (a) shows the time history of the velocity in the along-wind direction for inflow #1. In the spirit of the procedure proposed by Bruno et al. (2010) the time history of the along-wind velocity is expressed as a function of the dimensionless time $t^* = tU_H/H$ and subdivided in N sampling windows which extend from $t^* = 0$ to nT , where T has been chosen equal to 10 times the signal dimensionless integral time scale (calculated from the time autocorrelation function) and $n = 0, \dots, N$. For each sampling window first and second order statistics of the signal are calculated. Then, for each of these statistics the percentage residual ϕ_{res} is computed as $\phi_{res}^n = \frac{\phi_n - \phi_{n-1}}{\phi_n} \cdot 100$. Fig. 6 (b) reports the trend of ϕ_{res} for both the time average and the root mean square (r.m.s.). After $170t^*$ residuals of both average velocity and its r.m.s. are lower than 1%, indicating that a satisfactory convergence of inflow statistics can be considered achieved at this time, at least up to the second order statistics.

Results in terms of profiles of average velocity U , along-wind turbulence intensity I_{u_1} , and turbulence length scale L_{u_1} , at the location where the building will be placed afterwards are reported in Fig. 7 (a), (b) and

Fig. 6. Convergence of inflow velocity statistics: (a) time history of the along-wind velocity and (b) percentage residuals of its average and r.m.s.

Fig. 7. Profiles of (a) average velocity, (b) along-wind turbulence intensity and (c) turbulence length scale obtained from LES and experiments. A comparison with profiles given by AIJ reference standard (Architectural Institute of Japan, 2004) for terrain category IV is also provided.

(c) for the two analysed inflow conditions. No remarkable differences can be observed in terms of average velocity profiles between inflow #1 and #2, with both almost overlapping and very close to experimental measurements. When turbulence intensity is considered differences between inflow #1 and #2 slightly increase, although they are still lower than 3% (in terms of relative percentage difference) everywhere. When the along-wind turbulence intensity profile is compared to experimental data, it can be observed that at the reference height LES simulations underestimate I_{u_1} of about 17.8% (in relative terms), while numerical simulations overestimate the experimental results as the ground is approached, so for $z/H \leq 0.5$. Such mismatches in turbulence intensity might lead to mismatches of the same order of magnitude in terms of fluctuating pressures as will be later further discussed. It should be noted that these effects might be due to discrepancies between the inflow turbulence spectra adopted in simulation and wind tunnel experiments as well as to the roughness blocks introduced in the computational domain. Indeed, even if the considered pattern of blocks lead to the target aerodynamic roughness, as reported in Sec. 3.1, some discrepancies might exist between the layout of the blocks adopted in the proposed simulations and wind tunnel experiments, which can lead to the aforementioned overestimation of I_{u_1} in the lower part of the high-rise building.

The turbulent length scale has been computed using the Wiener-Khinchin theorem together with the Taylor hypothesis; the resulting integral turbulence length scale at each height has been averaged in the across-wind direction over a length equal to B , which is the maximum projection on the across-wind section of the building area (occurring at 0° angle of attack). In terms of turbulent length scale, the differences between inflow #1 and #2 remain almost constant until the height of $z/H = 0.5$ is reached and above $z/H = 0.2$ both curves are almost constant. In this range, L/H for inflow #1 is equal to 0.77 while for inflow #2 it is 0.65, showing a satisfactory agreement with the target values. Proceeding with increasing height the two curves become closer and differences almost vanish at height of $z/H = 1.0$.

Fig. 8 shows the power spectral density of the three velocity components, namely S_{u_1} , S_{u_2} and S_{u_3} , in dimensionless form for inflow #1 as well as the target von Kármán spectra used as inputs for the inflow turbulence generation. Aiming to analyse the dissipation of the turbulent fluctuations in the along-wind direction, spectra are plotted for two

different points, located respectively at the end of the roughness block fetch at $(-1.4H, 0, H)$ and in the location where the building will be placed at $(0, 0, H)$. It can be seen that turbulence energy decay between the two locations is small, since spectra are very similar to each other. Nevertheless, the underestimation of the turbulence intensity I_{u_1} at the building height shown previously in Fig. 7 (b) can be observed also in Fig. 8 (a). The same consideration holds also for the across-wind and vertical wind velocity components. Furthermore, for all the considered spectra, a frequency cut-off due to the mesh becomes evident starting from approximately $fH/U_H = 2$. The underestimation of the turbulence intensities shown by numerical simulations when results are compared to target spectra might be due to the turbulence generation method adopted, which is not able to control the turbulence length scale *a priori* as well as the coherence functions. On this regard, the adoption of inflow turbulence generation methods able to control the inflow coherence functions might improve the agreement between the target and the adopted spectra (Aboshosha et al., 2015).

In all, the synthetic inlet conditions comply satisfactorily with the target profiles, even if some discrepancies between numerical results and experimental data have been observed in particular in term of along-wind turbulence intensity profiles.

4. Large Eddy Simulations results versus experiments

In this section, first the analysis of calculation convergence together with the obtained integral forces for each angle of attack are reported in Section 4.1. Then, Section 4.2 shows some characteristics of the flow topology, while Section 4.3 compares results of the LES in terms of statistics of the pressure distributions to experimental results for each considered angle of attack. Finally, in Section 4.4, scatter plots between experimental and numerical predictions of the pressure distribution statistics are shown for all building surfaces.

For each simulation, pressures are acquired at each time step and data are subsequently coarsened to match the sampling frequency adopted in the wind tunnel tests. The positions of the pressure probes on the building model are identical to those reported for the experimental setup and shown in Fig. 9 (a), while Fig. 9 (b) reports the paths s_1 , s_2 and s_3 subsequently adopted for plotting pressure statistics.

Fig. 8. Spectra of the velocity time histories obtained from LES: (a) along-wind direction, (b) across-wind direction and (c) vertical direction.

Fig. 9. (a) Positions of the pressure probes according to the experimental setup and (b) view of the sampling lines adopted for plotting pressure coefficient statistics.

4.1. Convergence and integral forces

Simulation convergence is checked for each angle of attack by analysing the pressure signals acquired at the three locations indicated in Fig. 9 (a), namely $P_1 = (-0.1H, 0, 0.5H)$, $P_2 = (0.1H, 0, 0.5H)$ and $P_3 = (0, -0.2H, 0.5H)$. These points are chosen because they are located in regions with different flow topology, that is respectively on the windward, leeward and the lateral surface. For the sake of brevity, only results in point P_1 for the 0° angle of attack and inflow #1 are reported. In particular, Fig. 10 (a) shows the time history of the pressure coefficient $C_p = \frac{p}{0.5\rho U_H^2}$ for the location P_1 , while Fig. 10 (b) shows the percentage

residuals of its average $\overline{C_p}$ and r.m.s. C'_p , obtained by following the same method used for the inlet profiles in Section 3.2. It can be observed that after $250t^*$ the residuals in terms of both $\overline{C_p}$ and C'_p are lower than 1%. The same result is observed also for P_2 and P_3 .

Convergence is checked not only in terms of local pressures but also in terms of integral forces acting on the high-rise building. The same procedure adopted for the inlet condition and for surface pressures is applied to the integral force coefficients in x and y directions, defined respectively as $C_{F_x} = \frac{F_x}{0.5\rho U_H^2 DH}$ and $C_{F_y} = \frac{F_y}{0.5\rho U_H^2 DH}$ where F_x and F_y denote the integral forces, while their time-averaged values and their r.m.s are referred as $\overline{C_{F_x}}$, $\overline{C_{F_y}}$ and C'_{F_x} and C'_{F_y} , respectively. As previously observed for the pressure coefficient, also in this case after $250t^*$ the residuals of the integral force coefficients are below the threshold of 1%. In all, the duration of $250t^*$ is considered to be sufficient in order to reach satisfactory convergence of results at least up to second order statistics, so the simulations at all the investigated angles of attack are run until this duration is reached.

In order to analyse the performance of inflow #1 and #2, the pressure spatial correlations between each pressure probe and all the others are calculated and the resulting correlation fields are interpolated over the whole building surface. A comparison between inflow #1 and #2 at 0° is provided in Fig. 11, which shows the pressure coefficient spatial correlation $C_{p,corr}$ between the pressure probe P_1 and all the other probes along the path s_1 for the two considered inflow profiles, together with the corresponding experimental data. It can be observed that for inflow #2, characterised by the larger turbulent length scale, results are in good agreement with the experiments. On the windward surface the two curves almost overlap, while inflow #1 shows a more rapid decay of correlations proceeding away from probe P_1 . Differences become even more evident when the leeward surface is analysed. In this part, results

Fig. 10. Convergence of the pressure signal at location P_1 : (a) time history of the pressure coefficient and (b) percentage residuals of its average and r.m.s.

Fig. 11. Spatial correlation between the pressure probe P_1 and all the other probes along the paths s_1 (a) and s_2 (b): comparison between inflow #1 and inflow #2 at 0° angle of attack.

for inflow #1 are almost uncorrelated with the signal recorded in P_1 , as $C_{p,corr}$ is almost zero along the whole path. In contrast, correlations obtained for inflow #2 are negative and equal to about -0.2 in average, in rather close agreement with the experiments.

Differences observed in pressure correlations are also reflected in the flow bulk parameters, in particular in terms of the r.m.s. of the along-wind force coefficient C_{F_x} . Table 1 reports the statistics of the force coefficients for all the considered angles of attack, while Fig. 12 shows the spectra of the base integral moments M_x and M_y for the three angles of attack considered. As can be noted, at 0° the r.m.s. of C_{F_x} is equal to 0.153 when inflow #1 is considered, while it equals 0.196 when inflow #2 is adopted, resulting in this case very close to the experimentally observed value of 0.195. The better performance for inflow #2 in terms of both pressure correlations and C'_{F_x} led us to consider only this inflow condition for both other angles of attack considered here (i.e. 45° and 90°).

Table 1
Integral force coefficients.

Angle	Source	Inflow	$\bar{C}_{F_x}$	C'_{F_x}	$\bar{C}_{F_y}$	C'_{F_y}
0°	LES	#1	1.13	0.153	-0.01	0.12
	LES	#2	1.14	0.196	-0.01	0.13
	Exp.	—	1.13	0.195	0.002	0.19
45°	LES	#2	0.81	0.15	0.37	0.07
	Exp.	—	0.88	0.17	0.34	0.09
90°	LES	#2	0.42	0.08	0.002	0.18
	Exp.	—	0.43	0.09	0.02	0.19

Also for these wind directions, Table 1 shows that a satisfactory agreement is achieved between numerical and experiments both in terms of C_{F_x} and C'_{F_x} . Nevertheless, when C'_{F_y} is analysed, LES simulations show to significantly underestimate its value for all the considered angles of attack, being the relative difference compared to experimental data equal to 36%, 22% and 5% for 0° , 45° and 90° , respectively. This fact might be due to the underestimation observed in LES of the energy in the across-wind direction, in particular for $fH/U_H > 0.5$ (see Fig. 8 (b)). The underestimation of C'_{F_y} at the considered angles of attack is also reflected in spectra of the base moments reported in Fig. 12. As discussed in Sec. 3.2, the causes of this underestimation might be found in different factors, such as the target values used in the turbulence generation as well as the adopted turbulence generation method itself.

In all, the integral force coefficients are predicted by LES with satisfactory accuracy for all the considered angles of attack, in particular in terms of average quantities, with a maximum absolute relative error between numerical and experimental predictions equal to about 8% (excluding $\bar{C}_{F_y}$ at 0° and 90° , when it is expected to be zero) recorded at the angle of attack of 45° . It is worth to stress that the difference observed in the global forces spectra do not allow to characterise with good accuracy the mismatch in terms of structural response due to the presence of dynamic amplification. Such aspect will be considered in Sec. 5.

4.2. Flow topology

In this section, the flow topology obtained with LES is described for

Fig. 12. Spectra of the integral moments M_x and M_y for the 0° , 45° and 90° angles of attack.

each considered angle of attack. In particular, a qualitative view of the vortical structures for the 0° angle of attack is reported in Fig. 13 by means of isocontour of the invariant λ_2 (Jeong and Hussain, 1995), coloured with pressure. As can be observed, the boundary layer approaching the high-rise building separates leading to the development of the well-known horseshoe vortex. The vertical component of the velocity gradient that characterises the approaching boundary layer causes pressures to be higher near the top part of the high-rise building, where velocities are higher, and lower near the ground. This pressure gradient drives the flow downward close to the windward surface of the high-rise building and, as it approaches the ground surface, deviates the flow upwind. This is considered to be the mechanism responsible for the instability of the incoming boundary layer (Randerson, 1984) which also controls the position of the horseshoe vortex core.

These flow dynamics can be better appreciated if the average streamlines are observed. In particular, Figs. 14–16 show the average streamlines obtained with the Line Integral Convolution (LIC) technique (Cabral and Leedom, 1993) for 0° , 45° and 90° angles of attack, respectively. The streamlines are plotted for the xz plane through $(0, 0, 0)$ and for the xy plane through $(0, 0, 0.5H)$. As can be observed in Fig. 14, when the 0° angle of attack is considered the core of the horseshoe vortex is located at approximately $x/D = -1.3$ while it moves closer to the building's windward surface as the angle of attack increases, reaching $x/D = -1.0$ at 90° .

Focusing on the average flow reattachments/detachment locations,

Fig. 13. Three-dimensional view of the flow topology at 0° : isocontours of the invariant λ_2 at the dimensionless value of $\lambda_2 U^2 / H^2 = -30.5$ coloured with the instantaneous pressure coefficient.

which significantly affect the pressure distribution and consequently the wind load on the building, it can be observed in Fig. 14 that, when the 0° angle of attack is considered, the average flow field is detached from both the lateral and top surfaces. Conversely, the average flow reattaches on the top surface for the 45° angle of attack and on both top and along-wind surfaces for 90° , as shown in Figs. 15 and 16, respectively. In particular, at 90° (shown in Fig. 16 (b)) and for nominally smooth inflow conditions the flow is known to be detached, since the aspect ratio of the considered rectangular shape is $D/B = 2$ which is less than the threshold value of 2.8 reported by Noda et al. (Noda and Nakayama, 2003a). Nevertheless, the incoming turbulence and the enhanced turbulent mixing cause shear layer instabilities to occur further upwind than for the smooth inflow condition, leading the mean flow to reattach also for aspect ratios smaller than 2.8 (Noda and Nakayama, 2003b). Furthermore, it is worth noting that, in the considered case, the flow topology is complicated by the fact that the model is immersed in a turbulent shear flow, that is the simulated atmospheric boundary layer, so the reattachment length on the side walls changes between the bottom and top of the high-rise building. In particular, Fig. 17 (a) shows the change in the along-wind position of the reattachment point (indicated as x_r) with height at the 90° angle of attack. In the same figure the curve $1/I_{u_1}(z)$ is drawn, where $I_{u_1}(z)$ is the turbulence intensity profile, shifted by an offset that minimizes the difference between both curves calculated by means of a least squares procedure. Interestingly, the two curves show a very similar trend, at least up to approximately $z/D = 2.0$ where the reattachment bubble reaches its maximum along-wind extension, suggesting a linear relation between the reciprocal of the turbulence intensity and the reattachment length in the considered range. Moreover, it is worth noting that the height $z/D = 2.0$ at which the reattachment length reaches its maximum value represents the boundary between the regions where the flow is deviated downwards or upwards, as can be observed in Fig. 16 (a). Above this height the flow is deviated upwards by the high suction occurring in the top part of the high-rise building, as shown in Fig. 17 (b), so the along-wind position of the reattachment point starts decreasing from a maximum of $x_r/D = 0.34$ to $x_r/D = 0.005$.

4.3. Pressure distributions

In this section, results obtained from LES are systematically compared with experimental data in terms of pressure distributions. The distribution of the average pressure coefficient $\bar{C}_p$ and of its r.m.s. C'_p along the

Fig. 14. Average streamlines obtained with the LIC technique at 0°: (a) view of the xz plane through (0,0,0) and (b) of the xy plane through (0,0,0.5H).

Fig. 15. Average streamlines obtained with the LIC technique at 45°: (a) view of the xz plane through (0,0,0) and (b) of the xy plane through (0,0,0.5H).

Fig. 16. Average streamlines obtained with the LIC technique at 90°: (a) view of the xz plane through (0,0,0) and (b) of the xy plane through (0,0,0.5H).

path s_1 for the 0° angle of attack is reported in Fig. 18. As can be observed a good agreement is reached in terms of $\overline{C_p}$ on both windward and leeward sides. Focusing on the windward surface, both numerical and experimental data show a maximum of $\overline{C_p}$ at approximately $s_1/H = 0.85$. Regarding the numerical prediction of C'_p it can be observed that while on the windward surface LES tends to slightly overestimate, on the leeward surface the behaviour is opposite, with LES underestimating C'_p in particular near the bottom side where the average streamlines in Fig. 14 (a) show the presence of a recirculation vortex.

When the angle of attack is equal to 45° and path s_1 is analysed (Fig. 19) the average pressure coefficient predicted with LES almost overlaps the experimental data, while discrepancies in terms of C'_p previously observed for the 0° angle of attack become more evident in this

case, in particular in the leeward part of the path. Also in this case, C'_p appears to be satisfactorily predicted by LES on the windward surface, with a maximum relative difference between numerical and experimental data of less than 8%.

In order to analyse the pressure distributions characteristics also on the windward surface as previously done for the 0° and 45° angles of attack, when the 90° angle of attack is considered the $\overline{C_p}$ and C'_p distributions are plotted along the path s_2 in Fig. 20. This path has particular interest also because it allows to observe the effects of flow reattachment on the pressure distributions on lateral surfaces. Looking at the $\overline{C_p}$ distribution, the same observations which were made for 0° and 45° hold. The numerical and the experimental data are in good agreement, with a maximum relative difference of about 15%, recorded close to the leading

Fig. 17. (a) Average position of the reattachment point and (b) average pressure field on the along-wind surface at 90°.

Fig. 18. Pressure coefficient at 0° along the path s_1 : (a) average and (b) r.m.s..

Fig. 19. Pressure coefficient at 45° along the path s_1 : (a) average and (b) r.m.s..

edges where high suction due to flow separation occur. In this respect it should be noted that the accurate reproduction of flow dynamics near such zones represent a very challenging task for numerical simulations, since a significant reduction of relative differences would require extremely high spatial and, consequently, temporal resolutions, rendering LES very time consuming and, indeed, compromising its use for practical applications. Moving away from the locations where the highest suction are recorded, the pressure recovery starts and the slope of the curve increases until the reattachment point is reached, at $s_2/H = 0.32$ and after $s_2/H = 0.68$. Looking at the distribution of C'_p reported in

Fig. 20 (b) the numerical results underestimate the peak of C'_p , with a maximum relative difference between numerical and experimental data equal to 20%. This underestimation occurs in the along-wind sides of s_2 and might be due to discrepancies between the across-wind turbulence intensity in experiments and numerical simulations. Nevertheless, also the body aerodynamics as well as the turbulence subgrid-scale modelling can play an important role and it is really challenging to assess the relative effects of these factors on the C'_p distribution. Conversely, the numerical predictions of C'_p values on the windward part of the path ($0.4 \leq s_2/H \leq 0.6$) are in good agreement with experimental

Fig. 20. Pressure coefficient at 90° along the path s_2 : (a) average and (b) r.m.s.

measurements, indicating that the energy content in the incoming flow is correctly reproduced. This observation also holds for the previously analysed angles of attack.

4.4. Overall pressure distributions

In order to analyse the accuracy of the numerical prediction of the pressure coefficient over the whole building, in this section scatter plots between the experimental measurements and data obtained with LES for all the pressure probes and all the considered angles of attack are reported. Fig. 21 shows the scatter plot for $\overline{C_p}$ and C'_p at 0° angle of attack, while the same plots for 45° and 90° are reported in Figs. 22 and 23, respectively. Furthermore, Table 2 reports the percentage of points whose difference between numerical and experimental predictions is less than 10%, 20% and 30% (hereafter referred to as ranges of tolerance) for each angle of attack and each analysed statistic. The same ranges are highlighted in the scatter plots. In addition to these data, Table 2 also shows the Mean Normalized Bias (MNB) for each case, which is the average relative error between numerical and experimental predictions defined as follows:

$$MNB = \frac{1}{N} \sum_{i=1}^N \left(\frac{Q_{i,num} - Q_{i,exp}}{Q_{i,exp}} \right), \quad (14)$$

where N is the number of pressure probes, while $Q_{i,num}$ and $Q_{i,exp}$ are the considered quantities obtained from the numerical model and from the experiments, respectively. The MNB is used as an index able to synthetically characterise the correlation plots.

As previously observed in Section 4.3 for paths s_1 , s_2 and s_3 , when the

average pressure coefficient is analysed the numerical results are quite accurate over the whole building and for all angles of attack. Table 2 shows that at 0° the percentage of points in the 30% range of tolerance is 93.3%, while at 45° and 90° the corresponding percentages are 82.0% and 99.0%, respectively. The MNB equals 1.5% at 0° and 1.33% at 45°, while at 90° it changes sign and its absolute value increases, becoming equal to −9.47%. This indicates that in this case the average tendency of LES is to underestimate the mean pressure coefficient.

The scatter plots of Fig. 21 (b), 22 (b) and 23 (b) show that the r.m.s. of the pressure coefficient is predicted with lower accuracy than the average, with a distribution of points which slightly deviates from the bisector for all considered wind directions. These characteristics are also reflected in Table 2 which reports that, while at 0° 94.5% of the points is within the 30% range of tolerance, this percentage decreases to 69.4% and 88.8% for 45° and 90°, respectively. Also the MNB values are higher than those observed for the distributions of $\overline{C_p}$, showing a maximum value of 19% for the 45° angle of attack. Nevertheless, it is worth noting that in this case the overestimation of the numerical data is mainly concentrated in points with relatively small values of C'_p (see Fig. 22 (b)).

In all, the maximum absolute MNB observed for the $\overline{C_p}$ distribution is less than 10%, while for the C'_p distributions, the maximum absolute MNB is less than 20%.

4.5. Pressure spatial correlations

In order to accurately assess the structural response, not only the local statistics of the pressure coefficient should be well predicted, but also the spatial correlations need to be correctly reproduced. In order to analyse

Fig. 21. C_p scatter plots at 0°: (a) average and (b) r.m.s..

Fig. 22. C_p scatter plots at 45°: (a) average and (b) r.m.s..

Fig. 23. C_p scatter plots at 90°: (a) average and (b) r.m.s..

Table 2

Performance metrics for the pressure coefficient at 0°, 45° and 90°.

Performance metrics	0°		45°		90°	
	$\overline{C_p}$	C'_p	$\overline{C_p}$	C'_p	$\overline{C_p}$	C'_p
10% tolerance	45.1 %	27.8 %	59.8 %	31.6 %	30.6 %	52.9 %
20% tolerance	89.6 %	53.9 %	76.1 %	49.2 %	90.2 %	80.8 %
30% tolerance	93.3 %	94.5 %	82.0 %	69.4 %	99.0 %	88.8 %
MNB	1.15 %	—	1.33 %	19.0 %	—	4.54 %
		14.4 %			9.47 %	

this aspect, for each angle of attack the spatial correlations between each probe and all the others are calculated. Then for each angle of attack the spatial correlations between the probe located on the windward surface, $C_{p,corr}$, and all the others are analysed. The spatial correlation $C_{p,corr}$ calculated in this way is normalised with the autocorrelation function of the probe located on the windward surface for each wind angle of attack. At 45° angle of attack, spatial correlations of probe P_1 along the paths s_1

and s_2 are reported in Fig. 24, while when the 90° angle of attack is considered, the probe P_3 is located on the windward surface, so in this case Fig. 25 shows the spatial correlation along the paths s_3 and s_2 to which P_3 belongs. For the sake of brevity spatial correlations at 0° angle of attack reported in Fig. 11 are not here repeated.

Focusing on the windward part of the paths a good agreement between experimental and numerical data can be considered achieved for all the considered wind directions. Conversely, despite results at 0° being satisfactory for both the leeward and along-wind surfaces, numerical predictions at 45° and 90° angles of attack are inaccurate in these zones, even showing values of the spatial correlation opposite in sign with respect to experiments. Although it is not so straightforward to identify the cause of the discrepancies observed in the pressure correlations it is worth pointing out that they are affected by both the aerodynamic characteristics of the body as well as by the turbulent characteristics of the incoming flow, such as the spatial spectra (which can be obtained with a Fourier transformation performed in the space domain) and the turbulence length scales in the along-wind and across-wind directions,

Fig. 24. (a) Spatial correlation between the pressure probe P_1 and all the other probes along the paths s_1 and (b) s_2 at 45° angle of attack.

Fig. 25. (a) Spatial correlation between the pressure probe P_3 and all the other probes along the paths s_3 and (b) s_2 at 90° angle of attack.

which can not be directly controlled with the MDSRFG method. Regarding this last aspect, the adoption of inflow turbulence generation methods able to accurately reproduce the inflow coherence functions (Aboshosha et al., 2015; Castro et al., 2017) might improve the results in terms of pressure spatial correlations. In addition to the aforementioned aspects, some parameters, like the turbulence intensities in the across-wind and vertical directions, have been assumed since they are not reported in the experimental data. All these factors can contribute to the discrepancies observed for the pressure spatial correlations plots and also to those observed for the r.m.s. of the integral force coefficients reported in Table 1, since these quantities are directly related to each other.

5. Assessment of wind loads on structural elements

In this section linear structural dynamic analyses are performed using

as inputs the pressure fields obtained from both experiments and numerical simulations. Indeed, the pressure time histories are available in the probe locations (see Fig. 9 (a)) from two different sources, which are from LES simulations and from experimental measurements. Using once a at a time these two different datasets, the accuracy of LES in reproducing internal forces on the structural elements is assessed. In accordance with the experimental setup, also in LES simulations the model of the high-rise building is considered to be rigid, therefore aeroelastic effects are not taken into account in the following analyses.

The characteristics of the adopted structural model are reported in Figs. 26 and 27. The structure is a steel-framed tube embedding two cores and counts 50 floors with a regular distribution along the height. The steel-framed tube structure represents a very efficient structural solution for high-rise buildings since it tends to behave as an equivalent hollow tube, leading to a considerable saving of material compared to classical

Fig. 26. (a) Three-dimensional view and (b) frontal view of the structural model.

Fig. 27. (a) View of a characteristic floor of the structural model and (b) sectional dimensions of frame and core elements.

framed buildings (Taranath, 1988). Each floor is composed of a concrete slab with a thickness of 0.45 m. At the ground floor, the end sections of the beams are considered perfectly clamped.

The dimensions of the cross-sections of the structural elements constituting the framed structure and the core are varied with height every 40 m according to the 5 levels shown in Fig. 26 (b). According to the nomenclature introduced in Fig. 27 (b) the dimensions of the cross-sections for each level are reported in Table 3.

The first three natural structural modes are reported in Fig. 28, while for the linear dynamic analyses discussed below 10 structural modes are considered. The first natural frequency is equal to 0.204 Hz and is in good agreement with an empirical estimate for steel-framed structures which can be calculated as $f_1 = 1.0/(0.1N_f) = 0.2 \text{ Hz}$ (Taranath, 1988), where N_f is the total number of floors.

The linear structural dynamic analyses are performed by adopting the procedure described in Patruno et al. (2016b). Following this methodology the structural response is assessed by means of modal superposition while quasi-static corrections are introduced to take into account the effect of high-frequency modes. In this way static, quasi-static and resonant responses to the wind loads can be simply and efficiently assessed. The design wind speed adopted for the analyses is equal to 30 m/s at the

building top for all the considered directions.

Results are analysed in terms of axial forces in 12800 sections. Fig. 29 (a)–(c) show scatter plots for the r.m.s. of the axial force (referred to as N') for the 0°, 45° and 90° angles of attack, respectively, while Fig. 29 (d) shows the envelope of these three angles. In order to better analyse the characteristics of the distributions of N' , in accordance with what was previously done for the r.m.s. of the pressure coefficient, Table 4 reports for each graph of Fig. 29 the percentage of points falling within the three considered ranges of tolerance, together with the MNB. As can be observed, while for the 0° angle of attack 99% of points is within the 30% range of tolerance, when 45° and 90° are considered the same metric decreases to 22.5% and 37.3%, respectively. When these results are compared to those reported for C'_p in Table 2, it is observed that the accuracy shown by LES in predicting the r.m.s. of the pressure coefficient is not reflected in the r.m.s. of the axial forces. Focusing on the MNB reported in Table 4 a negative value is observed for all considered angles, indicating that LES tends to underestimate the value of N' . This can be noted also in Fig. 29 (a), (b) and (c), where points are concentrated near the lower 30% tolerance boundary. The best agreement between the values of N' obtained starting from experimental and numerical pressure fields is observed at 0°, for which the absolute value of MNB is lowest and equals -14.2% . When the envelope for the three angles of attack is considered differences decrease: in this case 98.2% of the points is within the 30% tolerance and the MNB is equal to -23.9% , despite a maximum of -33.7% observed at 45°. Furthermore, if the higher level of tolerance is increased from 30% to 38% the percentage of points included in the new tolerance range increases significantly, reaching 77% for 45° and 99.6% for 90°, indicating that the distribution of the normalized bias is quite narrow around its mean value.

In all, it is worth noting that in order to accurately assess the wind load effects on structures the correct reproduction of the local pressure

Table 3
Dimensions of the sections of the structural elements.

Level	Core elements			Framed tube elements		
	b_c [m]	h_c [m]	t_c [m]	b_f [m]	h_f [m]	t_f [m]
1	0.7	0.7	0.18	0.3	0.5	0.08
2	0.7	0.7	0.14	0.3	0.5	0.05
3	0.7	0.7	0.1	0.3	0.5	0.045
4	0.7	0.7	0.08	0.3	0.5	0.04
5	0.7	0.7	0.04	0.3	0.5	0.04

Fig. 28. First three structural modes and their natural frequencies (coloured by displacement magnitude).

Fig. 29. Scatter plots for the r.m.s. of the axial force (N').

Table 4

Performance metrics for the r.m.s. of the axial force (N').

Performance metrics	0°	45°	90°	Envelope
10%	23.3 %	0 %	0 %	0.1 %
20%	82.9 %	0.43 %	0.5 %	8.86 %
30%	99.0 %	22.5 %	37.3 %	98.2 %
MNB	− 14.2 %	− 33.7 %	− 30.5 %	− 23.9 %

coefficient statistics on the building surfaces is not sufficient, even if it represents a necessary first step. Indeed, the structural analysis is also affected by the spectral content of the pressure signals as well as their spatial correlations. Bearing in mind this consideration, in the analysed cases it can be observed that the numerical predictions of N' show the highest accuracy for the 0° angle of attack, for which also the pressure correlations are in very good agreement with experimental data, as shown in Fig. 11. Conversely, Figs. 24 and 25 show that for 45° and 90° the pressure correlations are predicted with lower accuracy, and the scatter plots of N' are less accurate as well. In particular, as previously observed for these wind directions, LES results show a positive pressure correlation between the windward and leeward surfaces, whereas

experimental data report a negative correlation. This fact might be responsible for the observed almost systematic underestimation of N' in these cases.

In order to also provide quantities of interest for the design of structural elements Fig. 30 (a), (b) and (c) show scatter plots for the peak axial forces N_{peak} at 0°, 45° and 90°, respectively, while Fig. 30 (d) shows the envelope for the three considered wind directions. The peak axial forces are defined as the mean values plus (and minus) 3.5 times the r.m.s. (N'). It can be seen that a good agreement between experimental and numerical data is achieved, even if some discrepancies are observed at all angles of attack, in particular for lightly loaded elements.

If elements loaded less than 10% of the maximum axial force in the whole structural model are disregarded, data reported in Table 5 show that the number of points within the 30% range of tolerance significantly increases and the absolute values of the MNB decrease for all considered wind directions. In particular, when the total envelope is considered, the MNB for N_{peak} equals −8.28% which is significantly lower (in absolute terms) than the value of −23.9% obtained for N' . Since the average axial force is also predicted more accurately than its r.m.s. N' , it appears that the agreement between numerical and experimental results improves significantly when global effects are considered.

Fig. 30. Scatter plots for the peak axial force (N_{peak}).

Table 5

Performance metrics for the peak axial force (N_{peak}) disregarding elements loaded less than 10% of the maximum load.

Performance metrics	0°	45°	90°	Envelope
10%	78.0 %	5.18 %	0 %	54.8 %
20%	89.6 %	47.1 %	5.26 %	66.0 %
30%	99.0 %	96.8 %	70.0 %	90.9 %
MNB	0.283 %	– 18.6 %	– 27.3 %	– 8.28 %

6. Conclusions

In the present paper the capabilities of LES as a structural design tool were investigated. In particular, the turbulent flow around an isolated high-rise building was simulated and the numerically predicted pressure field was compared to experimental measurements, after which both were used to assess the wind load effects on the structure.

A key point in the simulation process is represented by the inflow boundary conditions generation. In order to obtain accurate results in terms of pressure distributions the characteristics of the incoming turbulent flow need to be represented as accurately as possible. In order to do this, two synthetic fluctuation fields matching the target spectra were generated by means of the MDSRFG method and introduced in the computational domain. The thus generated fields differed only in the

choice of the target turbulent length scale, and since the MDSRFG procedure does not allow to fully control it *a priori* an iterative procedure was followed until the desired turbulent length scale was obtained. Then, in agreement with experimental practice, LES were performed in an empty domain, representing the wind tunnel in absence of the high-rise building model. A good agreement between both inflow profiles and the experimental measurements was obtained in terms of average velocity and turbulence intensity profiles. Then, the high-rise building was introduced in the computational domain and two LES were performed at 0° angle of attack using the two inflow conditions. Results in terms of statistics of the pressure distributions on the high-rise building were systematically compared with experimental measurements, and for the best performing inlet condition simulations at angles of attack equal to 45° and 90° were also performed.

First the local statistics of the pressure field for the considered angles of attack were analysed. In terms of $\overline{C_p}$ a good agreement between numerical predictions and experimental measurements was achieved, with a maximum MNB for all cases of less than 10%. With respect to the fluctuating part of the pressure field LES results were shown to be slightly less accurate, with a maximum MNB equal to 19%. Nevertheless, in particular for 45° and 90° it was observed that the larger relative errors are concentrated in points with relatively small values of $\overline{C_p}$. This fact might suggest that secondary flow mechanisms were not predicted by

LES with the same accuracy as global more energetic flow mechanisms.

In order to study the effects of the wind loads also in terms of internal forces, once the characteristics of the pressure field were analysed, a steel-framed tube structure was considered for the building and linear dynamic structural analyses were performed for each angle of attack, starting from both experimental measurements and numerical predictions of the pressure field. It was observed that the accuracy shown by LES in reproducing the C'_p distribution was not reflected in the r.m.s. of the axial forces N' , with a maximum MNB over all analyses equal to -33.5% . Furthermore, differently from the distributions of C'_p , an almost systematic underestimation was obtained in terms of N' . This fact might be related to the results obtained from the analysis of pressure spatial correlation fields. While at 0° a good agreement in terms of pressure correlations was obtained, at 45° and 90° some discrepancies were observed in particular on the leeward surfaces, where numerical and experimental results were opposite in sign. This different flow dynamics might be responsible for the systematic underestimation of N' , which also becomes particularly pronounced at 45° and 90° . Nevertheless, when the envelope of the three angles is considered results significantly improved, since the effects of minor flow mechanisms were partially hidden by the envelop of the results. In this case the MNB characterising the N' distribution was equal to -23.9% .

Regarding the peak axial forces results were shown to be more accurate than those obtained for N' for all considered angles of attack due to the fact that the mean values of the axial forces were predicted with higher accuracy than their fluctuating parts. When the envelope of the three angles was considered, the relative MNB was equal to -8.28% indicating that a satisfactory level of accuracy could be considered achieved.

Summarizing it can be stated that, with a view of using LES as a design tool for the sizing of structural elements, a comparison between experimental and numerical results in terms of pressure fields is necessary but not sufficient. The structural response is deeply affected also by the spectral content of the pressure field as well as by its temporal and spatial correlations. These characteristics are directly related to both the body aerodynamics and the turbulent inflow conditions. Considering the complexity of the problem and bearing in mind that a large number of factors contribute to the definition of the structural response, the present study indicates that a satisfactory accuracy can be obtained with LES if the envelope of several angles of attack is considered, in particular in terms of peak forces. In the awareness that wind tunnel tests still represent the most reliable technique to assess the structural response to the wind action, the obtained results are considered to be encouraging for pursuing further research which is still needed in order to develop the use of LES as a reliable tool for structural design.

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References

- Aboshosha, H., Elshaer, A., Bitsuamlak, G., El Damatty, A., 2015. Consistent inflow turbulent generator for LES evaluation of wind-induced responses for tall buildings. *J. Wind Eng. Ind. Aerod.* 99, 272–284.
- Architectural Institute of Japan, 2004. AIJ Recommendations for Loads on Buildings.
- Blocken, B., 2014. 50 years of computational wind engineering: past, present and future. *J. Wind Eng. Ind. Aerod.* 129, 69–102.
- Blocken, B., Stathopoulos, T., Carmeliet, J., 2007. CFD simulation of the atmospheric boundary layer: wall function problems. *Atmos. Environ.* 41 (2), 238–252.
- Braun, A., Awruch, A., 2009. Aerodynamic and aeroelastic analyses on the CAARC standard tall building model using numerical simulation. *Comput. Struct.* 87 (9–10), 564–581.
- Bruno, L., Fransos, D., Coste, N., Bosco, A., 2010. 3D flow around a rectangular cylinder: a computational study. *J. Wind Eng. Ind. Aerod.* 98 (6–7), 263–276.
- Bruno, L., Salvetti, M.V., Ricciardelli, F., 2014. Benchmark on the aerodynamics of a rectangular 5:1 cylinder: an overview after the first four years of activity. *J. Wind Eng. Ind. Aerod.* 126, 87–106.
- Cabral, B., Leedom, L.C., 1993. Imaging vector fields using line integral convolution. *Proc. SIGGRAPH* 263–270.
- Castro, H.G., Paz, R.R., 2013. A time and space correlated turbulence synthesis method for Large Eddy Simulations. *J. Comput. Phys.* 235, 742–763.
- Castro, H., Paz, R., Mroginski, J., Sorti, M., 2017. Evaluation of the proper coherence representation in random flow generation based methods. *J. Wind Eng. Ind. Aerod.* 168, 211–227.
- Chen, X., Kareem, A., 2005. Coupled dynamic analysis and equivalent static wind loads on buildings with three-dimensional modes. *J. Struct. Eng.* 131 (7), 1071–1082.
- Davenport, A., 1961. The application of statistical concepts to the wind loading of structures. In: *ICE Proceedings*, no. 6480.
- Dyrbye, C., Hansen, S., 1999. *Wind Loads on Structures*. Wiley, Chichester, West Sussex, England.
- Elliott, W.P., 1958. The growth of the atmospheric internal boundary layer. *Eos, Transactions American Geophysical Union* 39 (6), 1048–1054.
- Elshaer, A., Aboshosha, H., Dagnew, A., Bitsuamlak, G., El Damatty, A., 2016. LES evaluation for wind-induced responses of a tall building with isolated and complex surrounding. *Eng. Struct.* 115, 179–195.
- EN 1991-1-4, 2005. Eurocode 1: Actions on Structures -Part 1-4: General Actions -Wind Actions, vol. 4. European Committee for Standardization, pp. 1–148, 2005.
- Franke, J., Hellsten, A., Schlünzen, H., Carissimo, B., 2007. Best Practice Guideline for the CFD Simulation of Flows in the Urban Environment, vol. 44.
- Huang, S., Li, Q., Xu, S., 2006. Numerical evaluation of wind effects on a tall steel building by CFD. *J. Constr. Steel Res.* 63, 612–627.
- Huang, S.H., Li, Q.S., Wu, J.R., 2010. A general inflow turbulence generator for large eddy simulation. *J. Wind Eng. Ind. Aerod.* 98 (10–11), 600–617.
- Irwin, P.A., 2009. Wind engineering challenges of the new generation of super-tall buildings. *J. Wind Eng. Ind. Aerod.* 97 (7–8), 328–334.
- Irwin, P.A., Kochanski, W.W., 1995. Measurements of structural wind loads using the high frequency pressure integration method. In: *Proceedings of the 13th Structures Congress*, pp. 1631–1634.
- Jeong, J., Hussain, F., 1995. On the identification of a vortex. *J. Fluid Mech.* 285, 69.
- Kareem, A., 1984. Model for predicting the acrosswind response of buildings. *Eng. Struct.* 6 (2), 136–141.
- Kareem, A., Zhou, Y., 2003. Gust loading factor - past, present and future. *J. Wind Eng. Ind. Aerod.* 91 (12–15), 1301–1328.
- Kim, Y.C., Tamura, Y., 2014. Effects of incident flows on wind loads and their combinations on a low-rise building immersed in a long upstream fetch. *J. Struct. Eng.* 140.
- Kim, Y., Castro, I.P., Xie, Z.T., 2013. Divergence-free turbulence inflow conditions for large-eddy simulations with incompressible flow solvers. *Comput. Fluids* 84, 56–68.
- Lettau, H., 1969. Note on aerodynamic roughness-parameter estimation on the basis of roughness-element Description.pdf. *J. Appl. Meteorol.* 8, 828–832.
- Lilly, D.K., 1992. A proposed modification of the germano-subgrid-scale closure method. *Phys. Fluid. Fluid Dynam.* 4 (3), 633–635.
- Noda, H., Nakayama, A., 2003. Free-stream turbulence effects on the instantaneous pressure and forces on cylinders of rectangular cross section. *Exp. Fluid* 34 (3), 332–344.
- Noda, H., Nakayama, A., 2003. Reproducibility of flow past two-dimensional rectangular cylinders in a homogeneous turbulent flow by LES. *J. Wind Eng. Ind. Aerod.* 91 (1–2), 265–278.
- Patruno, L., Ricci, M., 2017. On the generation of synthetic divergence-free homogeneous anisotropic turbulence. *Comput. Meth. Appl. Mech. Eng.* 315, 396–417.
- Patruno, L., Ricci, M., De Miranda, S., Ubertini, F., 2016. Numerical simulation of a 5:1 rectangular cylinder at non-null angles of attack. *J. Wind Eng. Ind. Aerod.* 151, 146–157.
- Patruno, L., Ricci, M., de Miranda, S., Ubertini, F., 2016. An efficient approach to the evaluation of wind effects on structures based on recorded pressure fields. *Eng. Struct.* 124, 207–220.
- Randerson, D., 1984. "Atmospheric Science and Power Production," DOE Report.
- Ricci, M., Patruno, L., de Miranda, S., Ubertini, F., 2016. Effects of low incoming turbulence on the flow around a 5 : 1 rectangular cylinder at non-null-attack angle. *Math. Probl Eng.* 2302340, 12 pages. <https://doi.org/10.1155/2016/2302340>.
- Ricci, M., Patruno, L., de Miranda, S., 2017. Wind loads and structural response: benchmarking LES on a low-rise building. *Eng. Struct.* 144, 26–42.
- Taranath, B.S., 1988. *Structural Analysis and Design of Tall Buildings*. McGraw-Hill.
- Tokyo Polytechnic University Aerodynamic Database, 2003. <http://www.wind.arch.t-kougei.ac.jp/system/eng/contents/code/tpu>.
- Tominaga, Y., Mochida, A., Yoshie, R., Kataoka, H., Nozu, T., Yoshikawa, M., Shirasawa, T., 2008. AIJ guidelines for practical applications of CFD to pedestrian wind environment around buildings. *J. Wind Eng. Ind. Aerod.* 96 (10–11), 1749–1761.
- Tschanz, T., Davenport, A.G., 1983. The base balance technique for the determination of dynamic wind loads. *J. Wind Eng. Ind. Aerod.* 13 (1–3), 429–439.
- Weller, H., 2012. Controlling the computational modes of the arbitrarily structured C grid. *Mon. Weather Rev.* 140, 3220–3234.
- Xie, J., Irwin, P.A., 1998. Application of the force balance technique to a building complex. *J. Wind Eng. Ind. Aerod.* 77–78, 579–590.
- Zhang, Y., Habashi, W.G., Khurram, R.A., 2015. Predicting wind-induced vibrations of high-rise buildings using unsteady CFD and modal analysis. *J. Wind Eng. Ind. Aerod.* 136, 165–179.