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MODELING OF RC FRAME-WALL STRUCTURES FOR NONLINEAR SEISMIC ANALYSIS

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ABSTRACT

In this paper the attention is focused on the modeling of reinforced concrete (r.c.) frame-wall structures to check effectiveness and reliability of a pseudo-three-dimensional model (PTDM) when using different macroscopic wall models for nonlinear seismic analysis. The wall behaviour is simulated in turn by an equivalent beam model (EBM), a three-vertical-line-element model (TVLEM) and a multi-component-in-parallel model (MCPM). A numerical investigation is carried out with reference to a 1/5th-scale model of a seven-storey r.c. structure, whose measured dynamic response was obtained by other authors. The results prove that the PTDM, in combination with TVLEM and MCPM, is effective and suitable for simulating important effects due to the spatial interaction of the wall with the surrounding frames; on the contrary, the PTDM fails to simulate adequately these effects when using EBM. Further studies are needed to improve the accuracy in describing the measured response under strong ground motions.

KEYWORDS

Seismic nonlinear analysis; reinforced concrete structures; r.c. frame-wall system; r.c. frame-wall modeling; r.c. structural walls; pseudo-three-dimensional model; r.c. wall models; spatial interaction; wall rocking.

INTRODUCTION

It is recognized that well-designed r.c. frame-wall structural systems are very effective during severe earthquake ground motions. An important contribution in understanding the hysteretic behaviour of these systems was gained within the framework of a joint U.S.-Japan cooperative research project, which included the pseudo-dynamic testing of a full-scale seven-storey r.c. frame-wall structure (Kabeyasawa *et al.*, 1984) and the dynamic testing - on earthquake simulator - of a 1/5th-scale replica (Bertero *et al.*, 1984).

These testings contributed significantly for improving the analytical-modeling reliability of r.c. frame-wall structures, with particular regard to r.c. shear-wall modeling (Bachmann *et al.*, 1992; Bertero *et al.*, 1984; Fischinger *et al.*, 1992; Kabeyasawa *et al.*, 1984; Park *et al.*, 1987; Vulcano *et al.*, 1987 and 1988; Vulcano, 1992). However, the authors believe that the analytical modeling of r.c. frame-wall structures needs further improvement for adequately simulating the nonlinear seismic response.

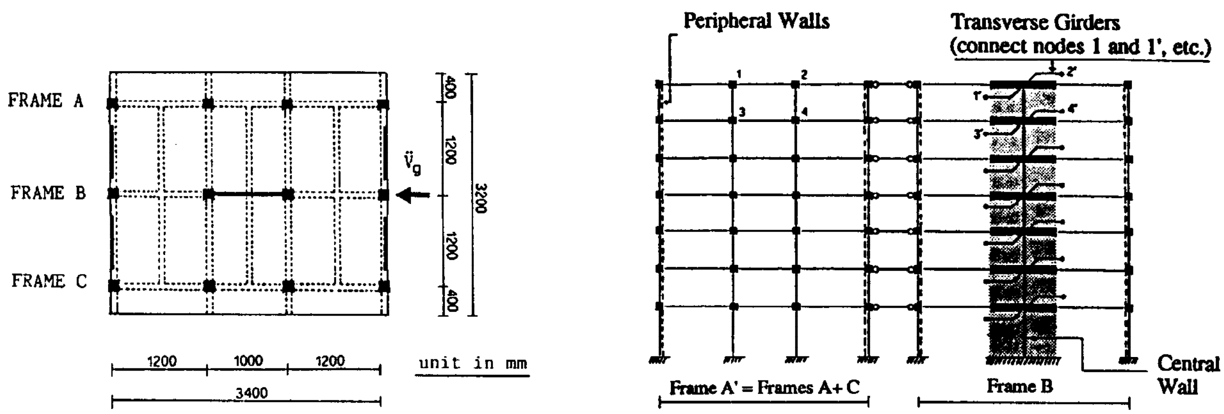
For this purpose it is considered very important to investigate on the dynamic response of a PTDM, paying attention to the macroscopic wall modeling. As a first step in this perspective, in a previous work (Vulcano and Azzato, 1994), with reference to the 1/5th-scale r.c. frame-wall structure mentioned above, a numerical investigation was carried out simulating the behaviour of the wall by an EBM or, in alternative, by the TVLEM proposed by Kabeyasawa *et al.*, 1984. In this paper also the MCPM proposed by Vulcano *et al.*, 1988 is considered, assuming simplified laws for the materials to simulate the hysteretic response of the vertical axial components.

STRUCTURAL MODELING

The reliability of a r.c. frame-wall model depends on the accuracy in describing the hysteretic behaviour of individual structural members and their interaction. At the same time, relatively simple, yet reasonable accurate, analytical models should be used to make practical the nonlinear seismic analysis. As evidenced in previous studies (see, e.g., Vulcano and Azzato, 1994), the simulation of important phenomena experimentally observed is crucial: in particular, the spatial interaction between the wall and the surrounding frames because of the "wall rocking" (see, e.g., Bertero *et al.*, 1984).

Modeling of the r.c. frame-wall structure

For sake of clearness, the following discussion refers to the 1/5th-scale seven-storey r.c. frame-wall structure tested by Bertero *et al.*, 1984 on earthquake simulator (see plan in Fig. 1a) and chosen as test structure for the numerical investigation. To account for the spatial interaction, the PTDM shown in Fig. 1b is adopted. Because of the structural symmetry, the two side frames A and C are lumped together in the resulting frame A'. Frame A' and wall-frame B are constrained by rigid horizontal truss elements to have identical lateral displacement at each floor. The transverse girder-slab system is assumed to be effective in relating vertical displacements of the frames A' and B. The four peripheral walls are modeled as truss elements in parallel with the exterior column elements. The joint zones are assumed to be rigid. The deformable part of girders and columns is idealized as a line element (e.g., as that described in the next Section), while the central wall can be idealized by an EBM (that is, treated as a column line element) or, more adequately, by a multi-vertical-line-element wall model (e.g., the TVLEM and the MCPM, both described below).



(a) Plan (b) Pseudo-three-dimensional model

Fig. 1. Test structure and its modeling (Bertero *et al.*, 1984).

Line (beam/column) element model

Following a macroscopic approach many beam models, sometimes very sophisticated, were developed. According to the aim of this work, basically addressed to clarify main aspects of the structural modeling connected with wall idealization, a reasonable compromise between efficiency and accuracy is obtained in this study considering a member-type-lumped-plasticity model of the kind adopted in previous works (e.g., Aristodemo *et al.*, 1982). But it should be mentioned that this model is capable of further improvement for a more realistic simulation of the observed hysteretic behaviour. For sake of brevity, only its main features are summarized below.

Two rigid links represent the part common with other structural members, while the central part of the element is assumed with axial, flexural and shear flexibilities (Fig. 2). The deformable element is assumed to behave elastically under axial and shear forces. Instead, its flexural behaviour is assumed elastic-perfectly plastic; the yield condition is checked at the end sections of the deformable element, accounting for the axial force-bending moment interaction. At each step of the analysis the initial state is known and the nodal displacement components are given: after determining the incremental elastic response, the elastic-plastic flexural solution is easily obtained by the procedure described in detail in the paper by Aristodemo *et al.*, 1982.

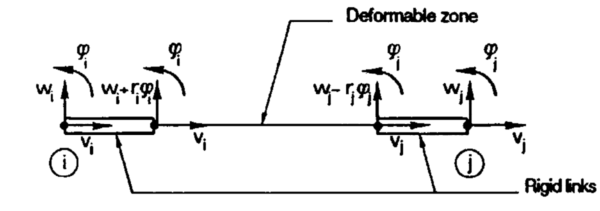


Fig. 2. Adopted line element model.

In this study, the stiffness and the strength properties of the deformable element when modeling longitudinal girders, columns and central wall (as an EBM) are assumed basically according to criteria adopted by Bertero *et al.*, 1984. In particular, a T-shaped cross-section is considered for longitudinal girders assuming as effective at the yielding point the intermediate slab width (i.e., 600 mm for longitudinal girders of frame B). To approximately account for the stiffness degradation due to cracking, according to the different effect of the axial force in the structural members, the flexural stiffness of the gross section (EI_g) may be differently reduced for girders, columns and wall ($EI = r\ EI_g$, where r is a suitable reduction factor).

Wall models

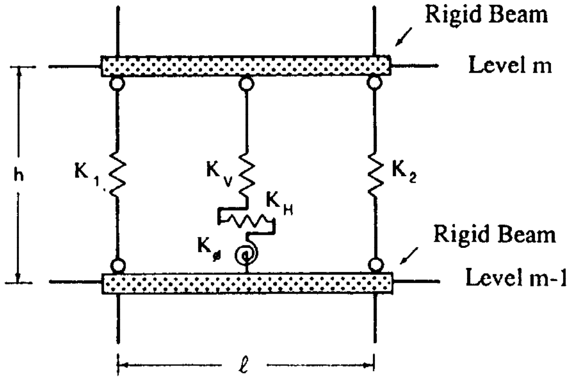
The incorporation of a suitable r.c. wall model in the PTDM is considered crucial. In previous papers (Vulcano *et al.*, 1987 and 1988; Vulcano, 1992) features and limitations of wall models were discussed in detail, giving suggestions for improving model effectiveness and/or reliability. As noted in these papers, macroscopic wall models are more practical than FE models (microscopic approach) - requiring larger storage and computational effort - for incorporation in nonlinear analysis of multistorey r.c. structures. However, attention must be paid to reliability of macroscopic models, depending on the conditions on which the models are derived. For sake of brevity, only main aspects of the wall modeling are summarized with regard to EBM, TVLEM and MCPM.

A current modeling considers a wall member replaced by an EBM. The main limitation of such a model is that rotations occur around points of the wall centroidal axis. Thus, important observed phenomena (i.e., fluctuation of the cross-section neutral axis, wall rocking, etc.) are disregarded and consequent effects in a frame-wall structure (i.e., spatial interaction, etc.) are not accounted for adequately. On the other hand, TVLEM in Fig. 3a (Kabeyasawa *et al.*, 1984) and MCPM in Fig. 3b (Vulcano *et al.*, 1988) account for fluctuation of the neutral axis of the cross-section and permit adequate simulation of the spatial interaction. In this paper the original TVLEM and MCPM are considered after introducing some modification.

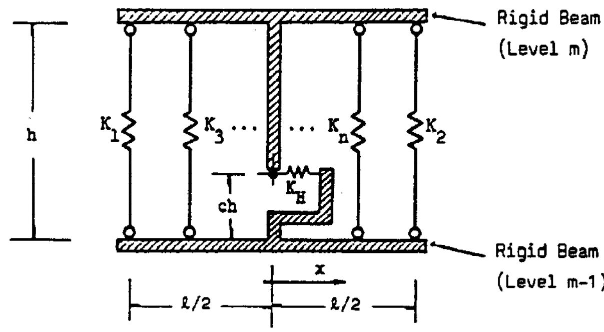
As shown in Fig. 3a, the TVLEM idealized a wall member under uniform bending as three vertical-line elements with infinitely rigid beams at the top and bottom floor levels: two outside truss elements represented the axial stiffnesses K_1 and K_2 of the boundary columns, while the central element was a one-component model consisting of vertical, horizontal and rotational springs at the base with stiffnesses K_v, K_H and K_ϕ, respectively. The axial-stiffness hysteresis model (ASHM) in Fig. 4a was proposed to simulate the response of the truss elements. The origin-oriented hysteresis model (OOHM) in Fig. 4b was used for both the rotational and horizontal springs.

To limit as much as possible the empirical assumptions, Vulcano and Bertero, 1987 modified the TVLEM. Subsequently, to achieve a more refined description of the wall flexural behaviour, Vulcano *et al.*, 1988 proposed the MCPM in Fig. 3b, whose basic idea is similar to that of a fiber model for simulating the flexural behaviour (e.g., see Park *et al.*, 1987). The relative rotation of a wall member was intended around the point placed on the central axis at height ch, assuming a suitable value for c on the basis of the expected curvature distribution along the inter-storey height h. The two-axial-element-in-series model (AESM) shown in Fig. 4c was adopted for simulating the hysteretic response of the vertical axial components: two elements in series represented the axial stiffness of the column segments in which the bond remained active (element 1) and those segments for which the bond was negligible (element 2); each element consisted of two parallel components to account for the mechanical behaviour of the concrete (C) and the steel (S); a suitable law for the dimensionless parameter λ defining the length of the two elements provided with an accurate description of the measured tension-stiffening effect. Refined constitutive laws were adopted to idealize the hysteretic behaviour of the materials and the tension-stiffening effect.

To improve the effectiveness of the MCPM without renouncing reasonable accuracy, schematic constitutive laws may be suitable as well. In this paper a simplified version of the AESM is adopted: the tension-stiffening effect is neglected (λ=1) and the constitutive laws in Fig. 5 are assumed for the materials. As shown in Fig. 5a, the tensile strength and the stiffness degradation when unloading and reloading are considered for concrete (α_c=0.2; β_c accounts for the confinement), while the bilinear law in Fig. 5b is considered for steel (β_s=0.001).

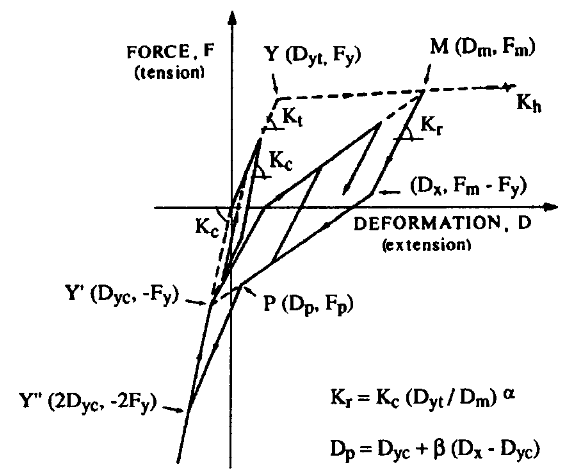


(a) Three-vertical-line-element model (Kabeyasawa *et al.*, 1984)

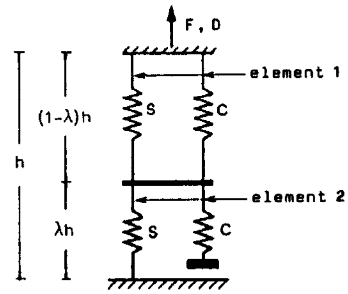


(b) Multi-component-in-parallel model (Vulcano *et al.*, 1988)

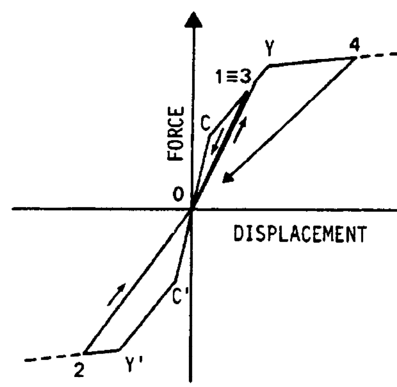
Fig. 3. Multiple-vertical-line-element models.



(a) ASHM (Kabeyasawa *et al.*, 1984)



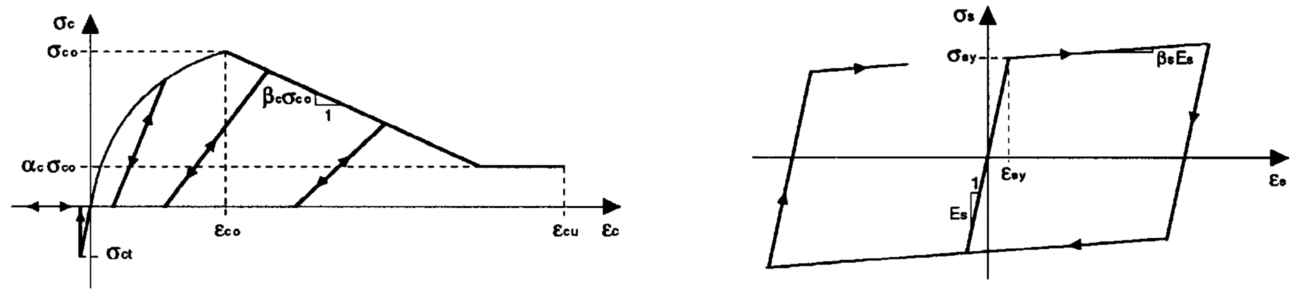
(c) AESM (Vulcano *et al.*, 1988)



(b) OOHM (Kabeyasawa *et al.*, 1984) for horizontal spring (TVLEM and MCPM) and rotational spring of TVLEM

Fig. 4. Hysteresis models for components of the wall models in Fig. 3.

As already mentioned, the wall is idealized using in turn EBM, TVLEM and MCPM. The EBM is a column line element as described above, while some modification is introduced in the original TVLEM and MCPM. For the last models the relative rotation is considered around a point at height ch, where c is selected according to an effective curvature distribution along the inter-storey height h (e.g., c=0.5 for a constant distribution). When using the TVLEM, the strain-hardening ratio in the OOHM for the rotational spring is neglected to account in some way for the kinematic compatibility between central panel and boundary elements, which as noted in a previous paper (Vulcano and Bertero, 1987), was completely disregarded in the original TVLEM; moreover, the value α=0.6 is assumed for the degradation parameter of ASHM (Fig. 4a), in spite of the value 0.9 in the original TVLEM, which, as noted in the above paper, may lead to unrealistic simulation.



(a) Concrete (b) Steel

Fig. 5. Constitutive laws adopted for AESM components in Fig. 4c.

Transverse girders

To account for the interaction effect due to the differential vertical displacement between a boundary column of the wall and the node of the lateral frames connected by the transverse girders at each floor, these girders are idealized as vertical spring elements (Fig. 6). The constitutive law of these springs is idealized as elastic-perfectly plastic: the elastic stiffness is defined taking into account the flexural deformability of the columns (of lateral frames), while the yielding force is determined as the shear force acting in a transverse girder when both the ends yield simultaneously. A T-shaped cross-section is considered for transverse girders, assuming as effective the full-slab width (550 mm).

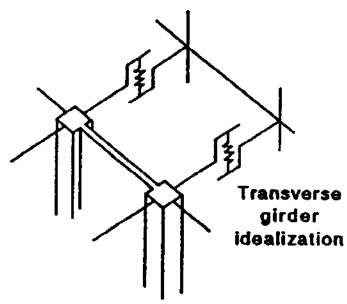


Fig. 6. Modeling of transverse girders.

RESULTS

To check effectiveness and reliability of the PTDM when using in turn EBM, TVLEM and MCPM described above, a numerical investigation has been carried out considering the nonlinear dynamic response of the 1/5th-scale test structure in Fig. 1a. The step-by-step dynamic procedure adopted in the paper by Aristodemo *et al.*, 1982 mentioned above has been used. All the results have been obtained using as input the Miyagi-Oki (M.O.) acceleration record after modification introduced by Japanese researchers in the joint cooperative research program already mentioned. In particular, for the 1/5th-scale test structure it was necessary to scale the time by a factor $1/5^{1/2}$ (i.e., the time scale was compressed). To contain the computational effort and accurately describe the dynamic response, in this study the time step is assumed equal to $0.01/5^{1/2}$ sec.

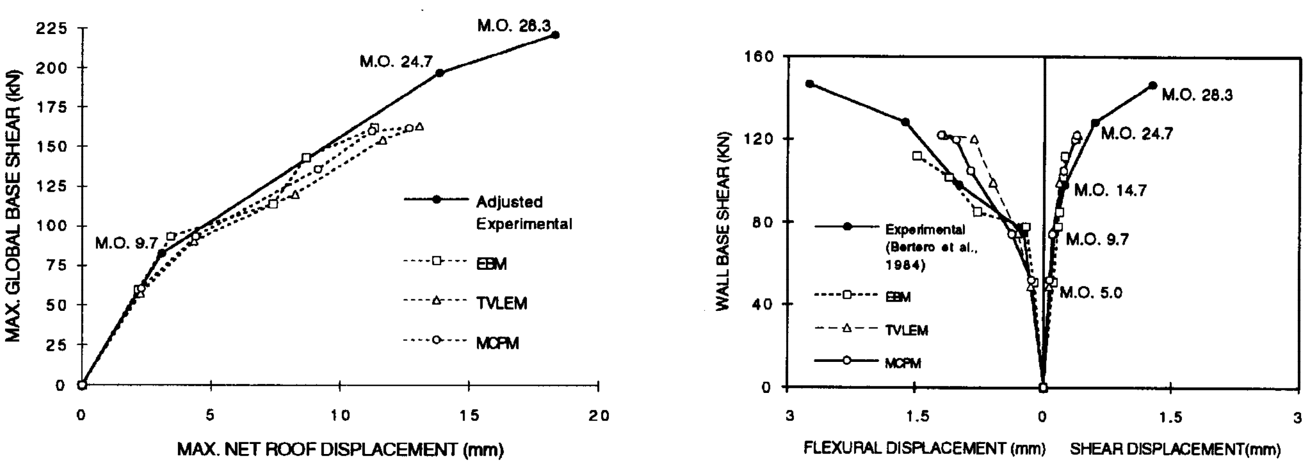
It should be noted that during dynamic testing the base acceleration measured on the platform of the earthquake simulator differed from the input signal. Therefore, it was necessary to use a scale factor for the input signal such that the scaled input signal would have the same value of the intensity coefficient - according Arias - as the measured table acceleration. As a result, the peak ground acceleration (PGA) of the scaled input signal was consistently less than that of the measured signal. In this study the scaled input signal is adopted, so as suggested by Bertero *et al.*, 1984: thus, to the input signals labeled as M.O. 5.0, M.O. 9.7, M.O. 14.7, M.O. 24.7 and M.O. 28.3 correspond, respectively, the values 0.05, 0.09, 0.14, 0.192 and 0.235 of the ratio PGA/g (g= gravity acceleration).

As shown by the authors in the paper mentioned above (1994), the analytical response can be appreciably affected by the choice of different values for the parameters needed for modeling: e.g., the factor r adopted for reducing the geometric flexural stiffness EI_g ($r=EI/EI_g$) of the line elements (including the wall idealized by EBM), the parameter c defining the position of the center of relative rotation for a wall member idealized by TVLEM and MCPM, the effective slab width b of longitudinal and transverse girders, the viscous-damping factor (mass-dependent) ν, etc.. In spite of forcing the analytical results by a "trial and error" procedure, the following assumptions are made which can be considered reasonable on the basis of the experience.

For lower signal intensities (up to M.O. 9.7), i.e. those before yielding, it is assumed: r=1; b=300 mm for longitudinal girders (according to technical codes); ν=2%. For higher signal intensities (M.O. 14.7, M.O. 24.7 and M.O. 28.3) it is assumed: r=0.4, 0.6 and 0.8, respectively for girders, central wall (EBM) and columns to account for the different influence of the axial force on the member cracking; b=600 mm for longitudinal girders (intermediate value between b=300 mm and the full-slab value); ν=5%. In all the cases, when using the TVLEM and MCPM for idealizing the central wall, it is assumed c=0.4 for the lower three stories and c=0.5 for the other stories, to account for the curvature distribution expected on the average.

In Fig. 7 analytical and experimental envelope curves are compared. Exactly, in Fig. 7a the maximum value attained by the global base shear is shown versus the maximum net roof displacement. To make the experimental envelope curve comparable with the analytical curves, it was necessary to adjust the results observed by Bertero *et al.*, 1984 subtracting, from the total diplacement, the contribution due to the fixed-end-rotation at the base of the wall (however, the measured contribution is available only for points M.O. 9.7, M.O. 24.7 and M.O. 28.3) not taken into account by the adopted analytical models. It should be noted that analytical curves are reported also for the other points M.O. 5.0 and M.O. 14.7, whose labels are not reported in Fig. 7a for sake of clearness. As shown, the slope of the adjusted experimental curve is similar to that of the analytical curves and the corresponding results are comparable for lower signal intensities (see, point M.O. 9.7). However, for higher signal intensities (see points M.O. 24.7 and M.O. 28.3) the use of different wall models, especially the EBM, produces an underestimation of the measured response. Results analogous to those in Fig. 7a, omitted for sake of brevity, have been obtained with reference to the central wall.

A further comparison between experimental and numerical results is shown in Fig. 7b, where the wall base shear versus the flexural and shear displacement contributions for the wall at the first floor is shown with reference to the instant when the maximum global base shear is attained. Conclusions analogous to those illustrated above with reference to Fig. 7a can be drawn.

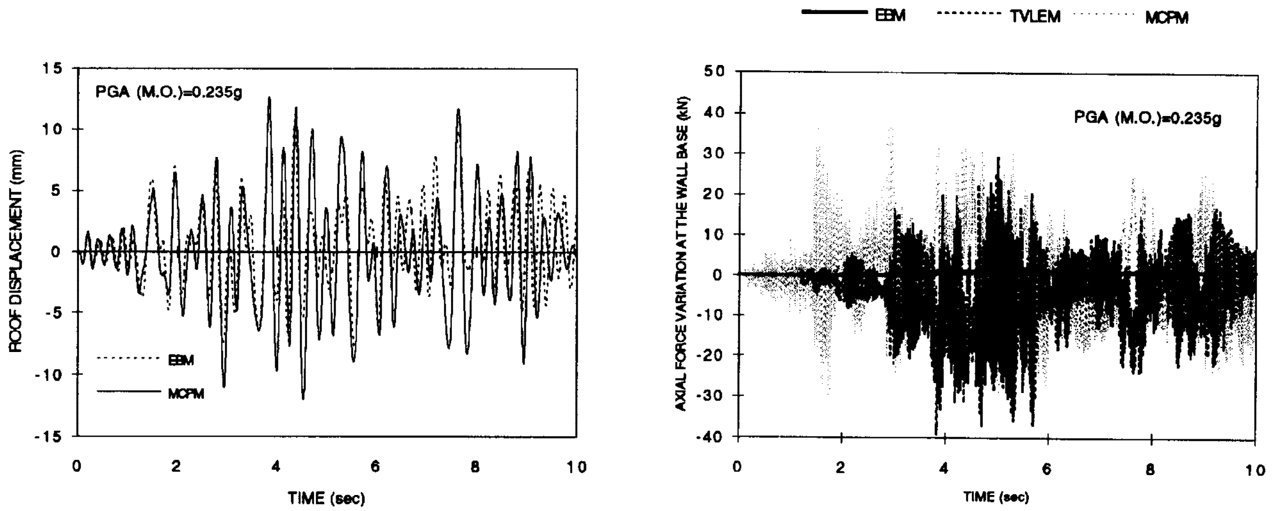


(a) Max. global base shear vs. max. net roof displacement

(b) Shear at wall base vs. first-floor displacement components at time of max. global shear.

Fig. 7. Comparison of analytical and experimental (Bertero *et al.*, 1984) envelope curves.

The underestimation, by the analytical models, of the measured response due to higher signal intensities can be mainly ascribed to the progressive degradation of the hysteretic capacity of the test structure due to the repeated cycles of loading undergone by the structure during the experimental program of dynamic testing. Nevertheless, the formulation of the analytical models should be revised, besides improving the accuracy in the description of the observed hysteretic behaviour (e.g., modifying hysteretic properties of the elements constituting the analytical models and by a suitable calibration of the parameters), simulating as well observed phenomena not described by the models used in this study (e.g., the fixed-end-rotation at the base of the wall and at the connection of the structural members) and so on.



(a) Roof displacement vs. time

(b) Axial force variation at the wall base vs. time

Fig. 8. Comparison of analytical curves.

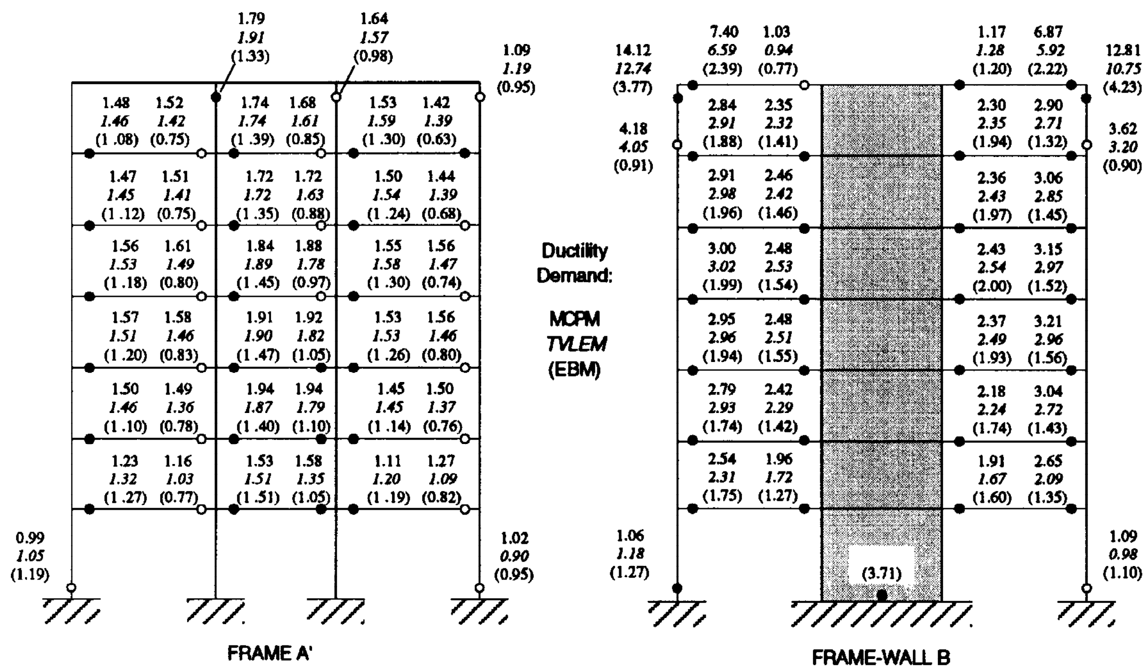


Fig. 9. Ductility demand for critical sections of members idealized by the line element model (PGA=0.235g).

The numerical investigation has furnished similar results with regard to the time-history of the roof displacement and that of the (global and wall) base shear when using TVLEM and MCPM, whereas the difference with the results obtained by idealizing the wall by the EBM has been more evident: e.g., see the results in Fig. 8a, where the time-history of the roof displacement is represented and, for sake of clearness, the curve obtained by TVLEM is omitted. However, as shown in Fig. 8b, where the time-history of the axial force variation at wall base is represented, the results for TVLEM and MCPM exhibit a clear difference in the time, even though the maximum variation of the axial force is comparable. In any case, when comparing these results with those obtained by the EBM the difference is particularly evident: while the axial force in the wall is simulated as practically constant by the EBM, it becomes markedly variable - as, on the other hand, observed during dynamic testing - when using the TVLEM and MCPM.

This last result is a consequence of the fact that these last models, unlike EBM, are capable of describing the wall rocking, leading to a more realistic evaluation of the spatial interaction. As a result, the use of TVLEM and MCPM led to values of the ductility demand for girders and columns (particularly those at the upper storey of the frame containing the wall) more higher than the analogous values obtained by EBM (Fig. 9).

CONCLUSIONS

The results obtained in this study permit to conclude that the PTDM is adequate for simulating the response of a r.c. frame-wall structure, particularly the effects of the spatial interaction. However, a multiple-vertical-line-element wall model (i.e., TVLEM or MCPM) should be used, because, unlike EBM, it accounts for the fluctuation of the cross-section neutral axis and can simulate wall rocking. The analytical response is sensitive to the choice of different parameters (e.g., flexural-stiffness reduction factor r for EBM and height ch of the relative-rotation centre for TVLEM and MCPM). The PTDM in combination with the wall models considered in this study, but particularly with EBM, underestimates base shear and displacements under strong ground motions. The use of the EBM for the wall leads to a wrong description of the spatial interaction and, then, of the axial-force in the wall (unrealistically described as almost constant) as well as of the ductility demand in columns and girders. The TVLEM and MCPM substantially exhibit a similar response, even though can present some different behaviour (e.g., time-history of the axial-force variation in the wall).

The accuracy in the description of the measured response by using PTDM in combination with TVLEM and MCPM should be improved following different guidelines: in particular, by selecting more refined constitutive laws for model components (especially, for simulating the shear behaviour of the wall when high shear stresses are expected), by adequate calibration of parameters affecting the analytical response (e.g., the effective slab width for girders, etc.) as well as by incorporation of observed phenomena not simulated by the wall models adopted in this study (e.g., fixed-end-rotation at the base of the wall and at the connection of frame members, etc.). Further studies are needed to investigate also on the effectiveness and reliability in prediction of the nonlinear dynamic response of r.c. frame-wall structures by using other wall models available in the literature.

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