

TRM reinforced tuff and fired clay brick masonry: experimental and analytical investigation on their in-plane and out-of-plane behavior

Jacopo Donnini^a, Gianluca Maracchini^{b*}, Stefano Lenci^b, Valeria Corinaldesi^a, Enrico Quagliarini^b

^a Department of Materials, Environmental Sciences and Urban Planning (SIMAU), Polytechnic University of Marche, via Breccie Bianche, 60131, Ancona, Italy

^b Department of Civil and Building Engineering and Architecture (DICEA), Polytechnic University of Marche, via Breccie Bianche, 60131, Ancona, Italy

*corresponding author. E-mail address: g.maracchini@staff.univpm.it
E-mail addresses: j.donnini@univpm.it; g.maracchini@staff.univpm.it; s.lenci@staff.univpm.it;
v.corinaldesi@staff.univpm.it; e.quagliarini@staff.univpm.it

ABSTRACT

The paper presents the results of an experimental campaign aimed at investigating the in-plane and out-of-plane behavior of tuff and fired clay brick masonry panels reinforced by a commercial, not yet tested, Textile Reinforced Mortar (TRM) system. The TRM, consisting of hydraulic lime mortar coupled with a bidirectional glass fabric (total thickness of 30 mm), was applied on both sides of the panels and connected through stainless-steel helical bars. First, a mechanical characterization of the TRM system was carried out, according to recent guidelines (AC434 and CNR-DT215) by means of tensile tests and single-shear bond tests on the two different substrates. Then, the effectiveness of the reinforcement was investigated through uniaxial compression, diagonal compression and three-point bending tests on masonry panels. After that, experimental results were compared to the predictions of recent guidelines and analytical models available in the literature, by using the mechanical parameters obtained from the TRM mechanical characterization. TRM showed to be very effective in increasing the in-plane shear strength of tuff panels, of about 170%, whereas in the case of fired clay brick ones the increase was limited to 33%. Besides, the diagonal compression strength of reinforced specimens seemed to be strongly influenced by the mortar matrix, instead of by the fabric reinforcement, as it usually happens i.e. for Fabric Reinforced Cementitious Matrix (FRCM) systems. Thus, novel simplified analytical formulations to predict the shear strength of reinforced panels by only considering the contribution of the TRM matrix were provided, discussed and compared to current analytical approaches for FRCM systems. As regards the out-of-plane behavior, the TRM system significantly increased the out-of-plane bending strength and ductility of both tuff and fired clay brick panels. The applicability of current analytical approaches, as well as possible amendments, was also investigated. Finally, the compressive behavior of the walls was not substantially modified by the reinforcement, reasonably due to the low axial stiffness of the connectors and, to a minor extent, because they were dry inserted within the masonry and simply folded over the glass fabric.

KEYWORDS

TRM; FRCM; CRM; tuff; masonry; glass; out-of-plane; diagonal compression; uniaxial compression; analytical investigation.

LIST OF SYMBOLS

AVG	Subscript for average values
exp	Subscript for experimental values
ACI	Subscript for ACI 549-13 analytical predictions
ASTM	Subscript for analytical values computed according to ASTM E519 and D'Antino et al. [6]
CNR	Subscript for CNR-DT215 analytical predictions
GB	Subscript for analytical values computed according to Gattesco and Boem [32]
RM	Subscript for reinforced masonry
URM	Subscript for unreinforced masonry
TRM	Subscript for Textile Reinforced Mortar
A_y	Resistant area of single yarn
A_{fabric}	Fabric cross-sectional area
A_w	Net cross-sectional area of the masonry wall (reinforcement layers are not considered)
b_w	Width of the masonry wall
b_f	Length of the reinforcement measured orthogonally to the applied force in diagonal compression tests
E_{fabric}	Elastic modulus obtained by tensile tests on the bare fabric
E_m	Secant modulus of elasticity of masonry wall
$E_{u,TRM}$	Cracked stiffness modulus of TRM coupons (tensile tests according to AC434)
f_c	Mortar compressive strength
$f_{c,M}$	TRM matrix compressive strength

54	$f_{\text{curtains separation}}$	Compressive stress corresponding to curtains separation in uniaxial compression tests
55	f_f	Mortar flexural strength
56	f_m	Masonry compressive strength
57	$F_{\text{max,S}}$	Maximum force recorded in shear bond tests
58	$F_{\text{max,TRM}}$	Peak tensile force from TRM tensile tests
59	$f_{t,M}$	Tensile strength of the TRM matrix
60	f_{TRM}	Tensile strength of the TRM
61	G	Shear stiffness
62	h_b	Depth of the beam specimen used in the flexural test
63	L	Distance between horizontal supports and applied load
64	M_{cr}	Bending moment at first cracking
65	M_u	Ultimate bending moment
66	N	Axial force
67	n	Number of TRM layers
68	n_f	Number of fabric layers
69	n_y	Number of GFRP tensed yarns
70	P	Applied load
71	P_{cr}	Load at first cracking (in bending tests)
72	P_{max}	Maximum applied load
73	s	Slip at peak load in single-shear bond tests
74	t_w	Thickness of the masonry wall
75	t_f	Equivalent thickness of the fabric
76	t_{TRM}	Thickness of the TRM matrix
77	T_y	Tensile resistance of one glass yarn
78	V	Shear strength (in kN)
79	x	Neutral axis depth
80	α	First stress block coefficient
81	α_t	Reduction factor in CNR-DT215 shear strength estimation
82	β_1	Second stress block coefficient
83	δ_{cr}	Horizontal displacement in correspondence of first cracking
84	δ_u	Maximum deflection
85	$\epsilon_{\text{lim,conv}}$	Design strain value according to CNR-DT215 ($\epsilon_{\text{lim,conv}} = \sigma_{\text{lim,conv}}/E_{\text{fabric}}$)
86	ϵ_h	Horizontal strain obtained by averaging the measurements of two LVDTs placed in opposite masonry sides
87	$\epsilon_{u,\text{fabric}}$	Ultimate tensile strain from tensile tests on glass fabric
88	$\epsilon_{u,\text{TRM}}$	Ultimate tensile strain of TRM coupons (tensile tests according to AC434)
89	ϵ_v	Vertical strain obtained by averaging the measurements of two LVDTs placed in opposite masonry sides
90	γ	Shear strain
91	γ_{cr}	Shear strain at cracking
92	γ_u	Shear strain at failure
93	σ_{fabric}	Tensile stress from tensile tests on glass fabric
94	$\sigma_{\text{lim,conv}}$	Design stress value according to CNR-DT215 ($\sigma_{\text{lim,conv}} = F_{\text{max,S}}/A_{\text{fabric}}$)
95	$\sigma_{t,M}$	Tensile stress on the TRM matrix at first cracking (tensile tests according to AC434)
96	$\sigma_{u,\text{fabric}}$	Tensile strength of glass fabric (tensile tests on bare fabric)
97	$\sigma_{u,\text{TRM}}$	Tensile strength of TRM coupons (tensile tests according to AC434)
98	ρ	Material density
99	ρ_f	TRM textile mechanical ratio
100	τ	Shear stress
101	τ_{max}	Shear strength

102 1 INTRODUCTION

103 The preservation and improvement of the structural safety of our architectural heritage are certainly some of the greatest
104 challenges today in the construction sector. The last seismic events in Italy have clearly highlighted the high vulnerability of
105 existing buildings, and especially of masonry structures, which are often characterized by low mechanical performance
106 materials, poor connections between walls and other structural lacks.

107 In the last years, several techniques have been proposed to improve the mechanical behavior and the seismic performance
108 of masonry structures. Along with the use of traditional methods, innovative composite systems are increasingly used, due to
109 their high efficacy, lightness and ease of application [1–3]. In particular, inorganic-based composite materials have been
110 continuously more and more used to reinforce existing masonry structures [4–7]. The advantages of using inorganic-based
111 systems rely on the capability of the matrix to ensure a good vapor permeability [8], resistance to high temperatures [9–13],
112 aggressive environments [14,15] and partial reversibility of the intervention, which could not be guaranteed, for example, in
113 the case of Fiber Reinforced Polymer (FRP) strengthening interventions, although some recent studies have shown, especially
114 for the confinement of masonry columns, the possibility of using reversible techniques also with FRPs [16].

The term Textile Reinforced Mortar (TRM) has been first introduced to describe a novel composite material that combines advanced fibers in the form of open mesh textiles with inorganic matrices [17,18]. Within this category, Fabric Reinforced Cementitious Matrix (FRCM) and Composite Reinforced Mortar (CRM) systems, specifically designed for the strengthening of existing structures, are the two most widespread today. FRCMs usually have a thickness of between 6 and 15 mm and the reinforcement grid is made of dry or partially coated fibers [19,20]. CRMs have a greater thickness, usually between 30 and 50 mm, the fibers are fully impregnated with a thermosetting polymer matrix to form a relatively stiff grid (low flexibility) and the spacing between yarns is higher than 30 mm [6,21–23]. Furthermore, CRMs provide for the use of mechanical anchors (usually made of stainless steel or GFRP) to connect the reinforcement to the substrate, whereas FRCMs can also be applied without connectors.

In the last two decades, research works on TRM systems have been mainly focused on demonstrating their effectiveness on different types of structural elements, and on validating or proposing analytical formulations for the design of the interventions. Concerning experimental works on masonry structural elements that can be found in the literature, the effectiveness of different types of TRM systems, characterized by different types of fabrics, inorganic matrices and thicknesses, has been mainly assessed either in terms of in-plane shear strengthening (see e.g. [21,22,24–31]) or in terms of out-of-plane bending reinforcement (see e.g. [18,32–36]), mainly using quasi-static tests.

A state-of-the-art on the use of TRM systems as strengthening/retrofitting materials for masonry structures has been carried out by Kouris and Triantafillou [37]. Papanicolaou et al. [18,31] compared the use of TRM and FRP systems both for in-plane and out-of-plane strengthening of masonry panels. It is concluded that TRM systems provide a substantial increase in strength and deformability and, if compared with their epoxy-resin counterparts (FRP), TRM may result in generally higher effectiveness in terms of strength and deformability. Wang et al. [38] performed an experimental campaign on the use of three different FRCM systems (reinforced with steel, carbon or basalt textiles and having a thickness between 10 and 15 mm) to reinforce grey clay brick masonry panels. FRCM systems significantly enhanced the performance of masonry panels subject to diagonal compression tests, in terms of both shear strength and ductility. Parisi et al. [24] showed through diagonal compression tests that an increase in the peak shear strength of about 90% and 210% for single-side and double-side strengthened specimens, respectively, can be obtained by applying 10 mm thick FRCM layers on tuff masonry walls. Gattesco and Boem [22] carried out diagonal compression tests on different types of masonry, strengthened with 30 mm thick mortar coatings reinforced with glass fiber reinforced polymer (GFRP) grids, obtaining a significant increase both in strength and ductility. Del Zoppo et al. [21] applied two 40 mm thick CRM layers on tuff masonry panels strengthened, varying the type of inorganic matrix and grid properties, obtaining an increase of the shear capacity ranging from 42% up to 288%. In [32], the authors carried out four-point bending tests on different masonry walls (solid brick, rubble stone and cobblestone) reinforced with CRM using composite glass fiber reinforced polymer grids, obtaining an out-of-plane bending moment almost 5 times greater and a pseudo-ductility 25 times higher than that of plain specimens. Babaeidarabad et al. [33] evaluated the out-of-plane behavior of clay brick masonry panels reinforced with one or four plies of carbon FRCM on the tension face of the wall, showing that the flexural strength of reinforced specimens was between 2.8 and 7.5 greater than that of the control specimen. Bellini and coworkers [34] conducted an experimental study on masonry walls reinforced with glass FRCM and subject to axial force and out-of-plane horizontal actions: the strengthening systems proved to be effective, exploiting the maximum tensile strength of the fibers.

However, to the authors' best knowledge, there are still few studies that provide a comprehensive experimental evaluation of the mechanical performance of a masonry structural element in terms of in-plane and out-of-plane behavior when strengthened with a TRM system [39,40]. Moreover, even if TRM systems are designed to counteract the low tensile resistance of masonry elements, in some cases (e.g. when TRM layers applied on both sides of the walls are connected through mechanical anchors, or characterized by a significant mortar thickness) their application could also influence the masonry wall compressive behavior, which, consequently, should be accurately investigated. Furthermore, there are still no studies that investigated the uniaxial compressive behavior of masonry panels reinforced with TRM systems.

Concerning the analytical formulations provided for the design of strengthening interventions, to date the only technical documents for TRM systems are those for FRCM, i.e. the American Concrete Institute (ACI 549.4R-13) [19] and Italian CNR (CNR-DT215/2018) [20], while acceptance criteria and indications on characterization methods are given by the ICC Evaluation Service (AC434), the Italian "Consiglio Superiore dei Lavori Pubblici", and Rilem TC 250 [41–43]. These guidelines provide different characterization procedures and different formulas to predict the FRCM contribution to the shear strength and bending resistance of masonry walls. The AC434 [41], for example, proposes to carry out tensile tests on FRCM coupons, while the Italian guideline CNR-DT215 [20] recommends a qualification method based on single-shear bond tests and tensile tests on the bare fabric.

Several works have tried to evaluate the predictive capabilities of these analytical models for FRCM, and also to suggest amendments or new approaches. For example, concerning the in-plane shear capacity, Del Zoppo et al. [44], used the experimental results of diagonal compression tests, collected from the literature, to evaluate the accuracy of the ACI 549.4R-13 design formulation [19], obtaining a safe estimation of the experimental results even without using any safety factor. Similarly, D'Antino et al. [45] compared the results of out-of-plane tests on different types of masonry walls, collected from the literature [32,33,46,47], with those computed according to different analytical approaches, such as the ACI 549.4R-13 approach [19], a method proposed by the authors (similar to that recently proposed by CNR-DT215 for inadequate bond length [20]), and the procedure developed by Ascione et al. [48]. Results showed a general underestimation of the experimental maximum bending moment according to ACI 549.4R-13 [19] and to the procedure developed by Ascione et al. [48], attributed, in the first case, to the underestimation of the composite effective tensile stress and the upper bound limit imposed on the

composite effective tensile strain and, in the second case, to the procedure adopted to compute the secant modulus of the FRM. However, most of the works collected by D'Antino et al. [45] did not provide the required materials characterization parameters needed for analytical predictions. Consequently, the validation of the design protocols was made by referring to mechanical parameters derived from the literature, highlighting the need for further characterization studies to accurately validate the existing analytical formulations.

In this work, the use of an inorganic matrix composite reinforcing system, currently available on the market, is investigated. It consists of a bidirectional glass fabric (coated with PVA) embedded in a 30 mm thick layer of hydraulic lime-based mortar, applied on both the two faces of the masonry and connected by dry inserted stainless steel helical bars.

While the spacing between yarns, about 25 mm, and the fabric stiffness (quite low being the PVA layer extremely flexible) would make such system to fall within the category of FRM systems, the thickness of the mortar (related to the presence of the steel anchors, which required an extra-thickness of mortar to be fully covered) brings it closer to the CRM systems. In this way, it is not really an FRM nor a CRM. For this reason, the authors decided to use the more general term TRM to classify it in the following.

In particular, a wide experimental campaign is carried out, aimed at:

- investigating weaknesses and strengths of such TRM system to reinforce masonry panels under uniaxial compression, diagonal compression and out-of-plane bending, since it is a commercially available product usually offered in post 2016-Centre Italy earthquake reconstruction, and very limited studies are present in literature;
- validating/amending current analytical formulations available from guidelines [19,20] and from the literature [6,32] to predict the diagonal compression strength and the out-of-plane bending strength of reinforced masonry, since no indications can still be found in current codes for masonry panels strengthened by thicker matrices than those used for FRM.

Two different masonry types, tuff and fired clay brick masonry, have been investigated, being these characterized by very different physical and mechanical properties and both widespread in Italy and many other European countries [49,50].

2 MATERIALS CHARACTERIZATION

2.1 Masonry components

Masonry walls were built by using tuff blocks with dimensions of 370x250x110 mm³ and fired clay bricks of 250x120x55 mm³, while a commercially available hydraulic lime mortar was used for the 10 mm thick joints with mechanical properties. Even if the mortar used is a commercial one, its characteristics may be similar in some cases to those found in some existing masonry structures and its mechanical properties (Table 1) are comparable (or even lower) to those used in other studies from the literature on tuff or historic clay brick masonry walls [6,21,27]. Uniaxial compression tests were carried out on 6 cubic tuff specimens with an edge of 70 mm, following the UNI EN 1926:2007 [51], and on 10 cylinders extracted from five fired clay bricks with a nominal diameter and a height of 50 mm, according to EN 772-1 [52]. Average compressive strengths of 4.5 MPa (CoV= 12%) and 30.5 MPa (CoV = 11%) were obtained for tuff and fired clay bricks, respectively.

Finally, the compressive (f_c) and flexural (f_f) strength of the mortar used for the joints were evaluated on three prismatic specimens (40x40x160 mm³) for each type of mortar, according to UNI EN 1015-11 [53]. The elastic modulus was evaluated on three prismatic specimens with the same dimensions, according to UNI EN 12504-4 [54]. Experimental results are reported in Table 1.

Table 1. Average mechanical properties of mortars. CoV in round brackets.

Material	Compressive strength f_c [MPa]	Flexural strength f_f [MPa]	Elastic modulus [GPa]
Masonry mortar	4.78 (4.7%)	1.48 (0.9%)	9.25 (1.3%)
TRM matrix	8.35 (2.9%)	2.97 (3.9%)	12.32 (1.1%)

2.2 TRM strengthening system

The TRM strengthening system adopted in this work comprises a bidirectional glass fabric made of alkali-resistant (AR) glass fibers partially coated with a layer of polyvinyl alcohol (PVA) embedded in a 30mm thick hydraulic lime-based mortar. The peculiarities of this system are due to the PVA coating applied on the fabric yarns to improve the adherence at the interface with the inorganic matrix and to the flexibility of the fabric, which can adapt to the complex shapes of the elements to be reinforced (see Fig. 1). The compressive (f_c) and flexural (f_f) strength of the TRM matrix were evaluated on three prismatic specimens (40x40x160 mm³) for each type of mortar, according to UNI EN 1015-11 [53], while the elastic modulus was evaluated on three prismatic specimens with the same dimensions, according to UNI EN 12504-4 [54]. Experimental results are reported in Table 1. The physical and geometrical properties of the glass fabric, as reported by the manufacturer, are listed in Table 2.

The TRM system is applied on both sides of the masonry walls. Stainless steel helical bars with a nominal diameter of 8 mm and tensile strength of 830 MPa (reported by the manufacturer, according to UNI EN ISO 6892-1:2016 [55]) are used to connect the two layers of TRM, passing through the masonry and folded over the glass fabric for a length of about 100 mm. The bars are inserted by simply screwing them within the holes made on the wall before the application of the TRM. Specimens

dimensions, number and position of the helical bars are detailed in the following for each type of test. It should be noted that the TRM system employed is a commercially available product and was applied following the recommendations of the manufacturer.

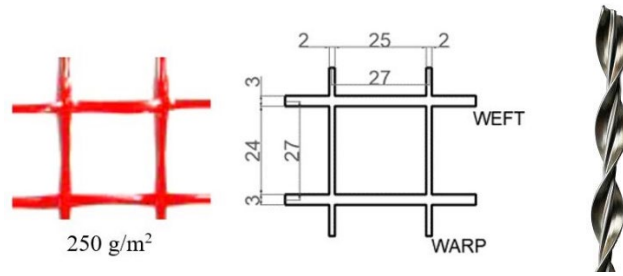


Fig. 1 – Glass fabric employed and stainless-steel helical bar used as a connection system.

Table 2. Physical and geometrical properties of glass fiber fabric (from manufacturer technical sheets).

Density	Equivalent thickness (for each direction)	Resistant area of single yarn
[g/m ²]	t _f [mm]	A _y [mm ²]
250	0.0314	0.789

2.2.1 TRM characterization according to AC434

Following the AC434 instructions (Annex A) [41], the TRM system was characterized through tensile tests, in order to obtain the characterization parameters needed for the analytical formulations proposed in ACI549 [19]. A total of five prismatic coupons with dimensions of 400x70x12 mm³ were prepared by placing a glass fabric strip between two layers of mortar (total thickness of 12 mm). It should be noted that the thickness of the specimens used for tensile characterization tests, according to AC434, is lower than the thickness of the reinforcement applied on masonry panels (30 mm). This could affect the mechanical response of the TRM system, especially in terms of stress at cracking, cracks distance and tension stiffening effect. As a consequence, the use of these mechanical parameters to predict the behavior of masonry panels reinforced with TRM of greater thickness (while having the same amount of fabric reinforcement) must be carefully evaluated.

Specimens were cured at laboratory conditions (about 20 °C and 70% relative humidity RH) for 28 days before testing. Metal tabs with the same width of the coupons and thickness of 3 mm were epoxy glued at the ends of each TRM specimen. Through this type of configuration (clevis grip), the tensile load is transferred to the TRM coupon only through adhesive shear stresses, avoiding applying normal forces at the specimen's ends [41,56,57]. An effective length equal to 100 mm was assumed, as also made in [58–61], which allowed to measure the axial strain over the entire free length of the specimen by using a macro extensometer with a gauge length of 100 mm, directly anchored at the ends of the metal tabs (see Fig. 2). It is worth pointing out that, although the AC434 does not establish a well-defined effective length to be adopted during the tests, the TRM mechanical characterization could be influenced by the choice of this parameter and this aspect certainly deserves to be better investigated in future studies. The load was applied under displacement control at 0.5 mm/min.

The results of TRM tensile tests are provided in terms of peak force ($F_{\max, \text{TRM}}$), ultimate stress ($\sigma_{u, \text{TRM}}$), ultimate strain ($\epsilon_{u, \text{TRM}}$), cracked stiffness modulus ($E_{u, \text{TRM}}$) and tensile stress on the mortar at first cracking ($\sigma_{t, \text{M}}$), together with the failure mode observed (Table 3). Stress-strain curves are reported in Fig. 3. For the determination of the ultimate stress $\sigma_{u, \text{TRM}}$, the measured maximum force ($F_{\max, \text{TRM}}$) is divided by the cross-section of the textile reinforcement ($3A_y$) in the load direction. The ultimate strain $\epsilon_{u, \text{TRM}}$ is defined as the ultimate deformation experienced by the TRM coupons before failure. $E_{u, \text{TRM}}$ is calculated as the slope of the segment of the response curve between two points equal to $0.60 \sigma_{u, \text{TRM}}$ and $0.90 \sigma_{u, \text{TRM}}$, as recommended by AC434 Annex A [41]. Finally, $\sigma_{t, \text{M}}$ is calculated by dividing the load corresponding to the appearance of the first crack on the mortar by the gross section area of the TRM specimen (about 70x12 mm²).

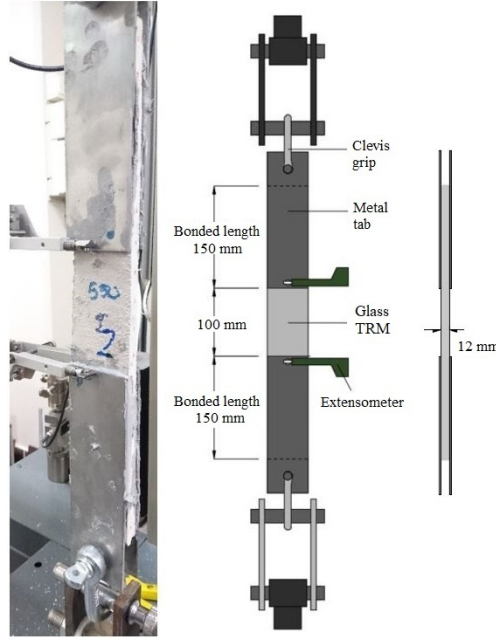


Fig. 2 – Tensile test setup according to AC434 [41].

The typical stress-strain behavior of TRM specimens tested by using clevis grip anchors, consisting of two phases (before and after cracking), has been observed [57,62]. The first part of the curve, controlled by the mortar mechanical properties, is linear up to the formation of the first crack. Once the matrix has cracked, the load is transferred through the fabric reinforcement. At this transition point, a significant load drop is observed, probably due to the modest cross-sectional area of the fabric reinforcement. Then, the slope of the curve decreases and the second branch continues until the ultimate capacity of the specimen is reached.

The slope of the second part of the stress-strain curve, which is always lower than that of the first branch, depends on the quality of the bond between fabric and matrix as well as on the volume portion of the fibers activated for load transfer [62–64]. Usually, at least three visible cracks appear in the free length of the specimen (see Fig. 3b). The presence of “jumps” on the stress-strain curve is due to the formation of further small horizontal cracks within the matrix (not visible in the picture), which causes a redistribution of stress and deformations over the specimen length.

However, the second part of the stress-strain curves can be approximated as linear up to failure, as suggested by AC434 [41]. For all tested specimens the tensile failure always occurred due to breakage of the internal glass fabric, with an average ultimate tensile stress $\sigma_{u,TRM}$ of 1275 MPa. Finally, it should be noted that, since a clevis grip system is adopted, which did not apply compression forces at the specimen ends, some slippage of the fabric within the TRM matrix occurred before failure, even if the TRM coupons failed due to fabric breakage.

Table 3. Results of tensile tests on TRM coupons according to AC434, Annex A [41]. CoV in round brackets.

$F_{max,TRM}$ [kN]	$\sigma_{i,M}$ [MPa]	$\sigma_{u,TRM}$ [MPa]	$\varepsilon_{u,TRM}$ [-]	$E_{u,TRM}$ [GPa]	Failure mode
3.02 (6.0%)	1.67 (8.9%)	1275 (6.0%)	0.049 (13.8%)	25.25 (8.47%)	Fabric breakage

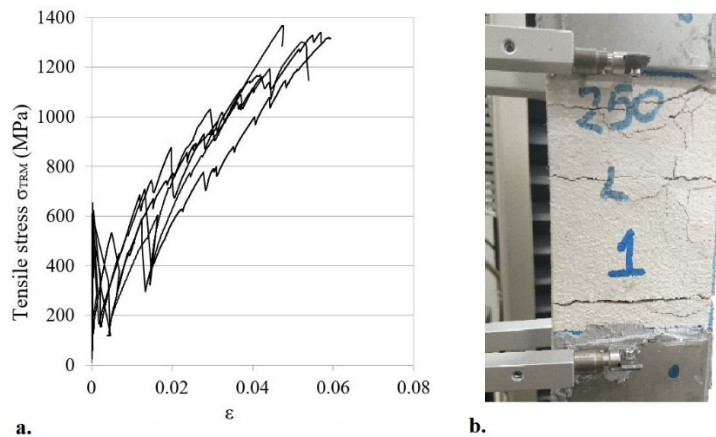


Fig. 3 – (a) Stress-strain curves of TRM coupons according to AC434 [41] and (b) typical failure mode observed.

2.2.2 TRM characterization according to CNR-DT215

The contribution of the TRM reinforcement, according to CNR-DT215 [20], not only depends on the mechanical characteristics of the composite system, but also on the type of support on which it is applied [20,48,65]. This is especially true when an adequate bonded length of the TRM system is not guaranteed. For this reason, two types of tests, i.e. single-shear tests and tensile tests on the bare fabric, are necessary to determine the design parameters ($\sigma_{lim,conv}$, $\epsilon_{lim,conv}$) according to the procedure described in §3.1 of CNR-DT215 [20]. In particular, for a specific TRM system and a specific support, $\sigma_{lim,conv}$ corresponds to the characteristic value of the maximum force $F_{max,S}$ recorded in shear bond tests (Fig. 4b) divided by the fabric cross-sectional area A_{fabric} ($\sigma_{lim,conv} = F_{max,S}/A_{fabric}$), while the conventional limit strain $\epsilon_{lim,conv}$ is determined as $\epsilon_{lim,conv} = \sigma_{lim,conv}/E_{fabric}$, where E_{fabric} is the elastic modulus of the fabric obtained by tensile tests on the bare fabric (Fig. 4a).

In this study, four tensile tests were carried out on glass fabric strips, considering three yarns in the warp direction (Fig. 4a). Glass FRP tabs ($80 \times 100 \text{ mm}^2$) were applied at the ends of each fabric strip, to guarantee a better grip to the testing machine and load redistribution between the 3 yarns. The specimen free length was about 200 mm while the axial strain was measured by a macro extensometer, with a gauge length of 80 mm, applied to the central yarn of the fabric (Fig. 4a). Tensile stress σ_{fabric} has been calculated by dividing the total force by the area of the 3 yarns ($3A_y$). $\epsilon_{u,fabric}$ is the ultimate deformation of the fabric at failure. The elastic modulus E_{fabric} has been calculated as the secant modulus between 10 and 50% of the ultimate tensile stress.

Then, four tuff blocks, with dimensions of $300 \times 120 \times 150 \text{ mm}^3$ (Fig. 4c), and four masonry blocks, manufactured with five half-bricks joined by five 10 mm-thick mortar layers, with dimensions of $315 \times 120 \times 150 \text{ mm}^3$ (Fig. 4d), were used as support for single-shear tests. All the substrates were saturated before the application of TRM strips on one side of each block. TRM strips, having a thickness of 12 mm, comprised three longitudinal glass yarns and were bonded to the substrate for a length of 250 mm. The free length of the bare fabric at the beginning of the test was about 300 mm. All the specimens were cured at laboratory conditions (about 20°C and 70% RH) for 28 days before testing.

The adopted single-shear test setup is showed in Fig. 4b. The block was restrained against movement by a steel frame fixed to the base of the testing machine. The upper end of the fabric was clamped and connected to the load cell of the tensile machine. Tests were conducted under displacement control, with a load speed of 0.5 mm/min. Axial displacement of the fabric at the upper end was measured by a potentiometer directly applied to the load cell of the testing machine.

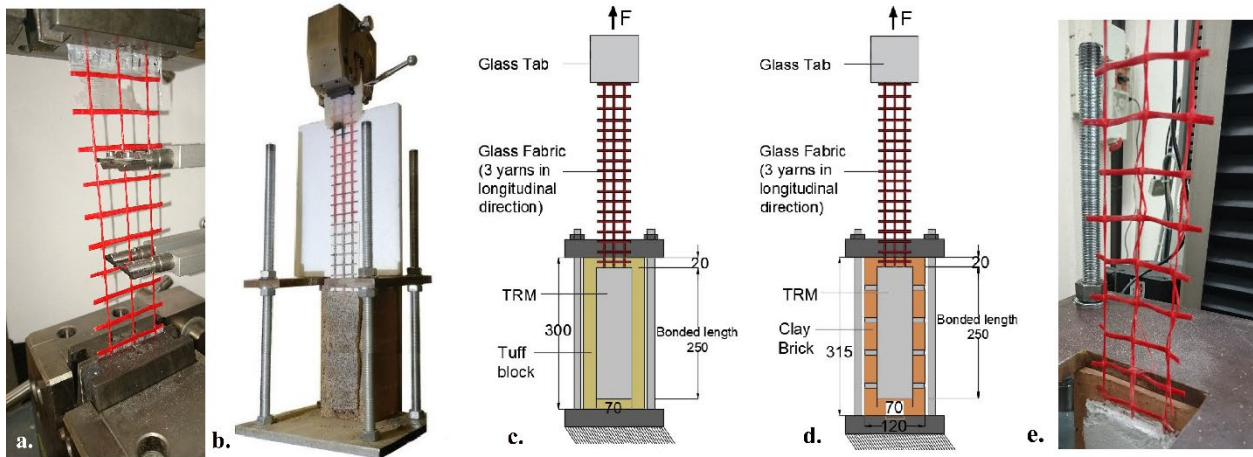


Fig. 4 – (a) Tensile test on glass fabric, (b) single-shear bond test setup, specimen dimensions and TRM bonded length for (c) tuff blocks, (d) fired clay brick masonry blocks and (e) typical failure mode observed due to breakage of glass yarns (both for tuff and fired clay brick substrates).

Table 4 reports the results of tensile tests on the glass fabric in terms of ultimate tensile stress and strain ($\sigma_{u,fabric}$ and $\epsilon_{u,fabric}$, respectively) and elastic modulus E_{fabric} , while the stress-strain curves are shown in Fig. 5. Some differences in terms of elastic modulus and ultimate strain were obtained from tensile tests on TRM coupons (according to AC434, see Table 3) and from tests on the bare fabric (Table 4), mainly due to the different methods of anchoring the specimens at their ends. In fact, while tensile tests on the bare fabric were carried out by clamping the fabric strips at their ends, tensile tests on TRM coupons adopted a clevis grip system, which did not apply compression forces at the specimen ends and allowed possible slippage of the fabric within the TRM matrix. For this reason, even if the TRM coupons failed due to fabric breakage, some slippage of the fabric within the mortar occurred before failure. This phenomenon led to a greater deformation of the specimen in the free part between metal tabs, thus lowering the elastic modulus with respect to tensile tests carried out on the bare fabric.

330

Table 4. Results of tensile tests on glass fabric. CoV in round brackets.

Ultimate tensile stress $\sigma_{u,fabric}$ [MPa]	Ultimate tensile strain $\epsilon_{u,fabric}$ [-]	Elastic modulus E_{fabric} [GPa]
1319 (8.5%)	0.0193 (9.2%)	67 (13.2%)

331

332

333

334

335

336

Experimental results of single-shear bond tests, varying the type of support, are reported in Table 5 in terms of peak load $F_{max,S}$, peak stress in the textile $\sigma_{f,max}=F_{max,S}/3A_y$, and displacements corresponding to the peak load. Failure always occurred out of the bonded area, with the breakage of two or three yarns of the glass fabric (Fig. 4e), indicating a good adhesion both at the fabric-matrix and matrix-substrate interfaces. Furthermore, results indicated that a bonded length of 250 mm can guarantee the full exploitation of the TRM composite tensile strength, both for tuff and fired clay brick substrates.

337

Table 5. Results of single-shear bond tests. CoV in round brackets.

Specimen	Peak Load $F_{max,S}$ [N]	Peak stress in the textile $\sigma_{f,max}$ [MPa]	Displacement at peak load s [mm]
TRM_Tuff	2921 (10.1%)	1234 (10.1%)	8.65 (12.3%)
TRM_Fired clay brick	2909 (11.4%)	1229 (11.4%)	7.61 (13.0%)

338

339

340

341

342

343

344

345

346

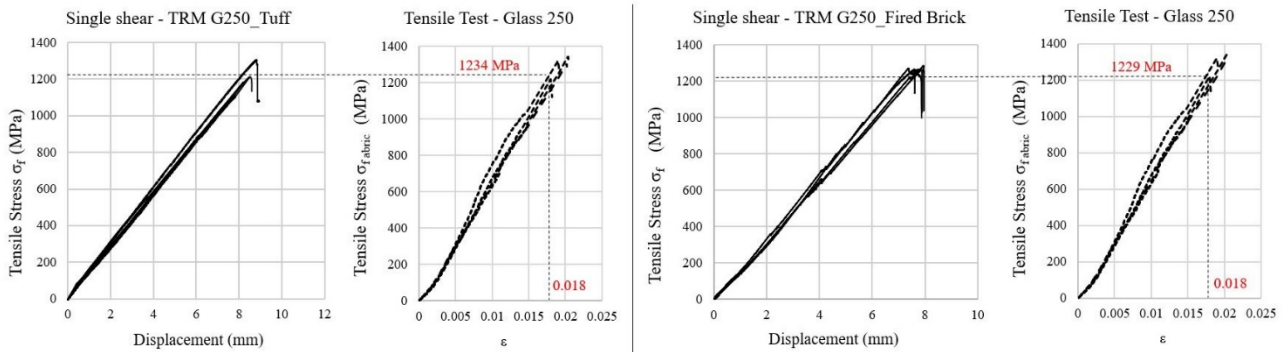
Finally, results obtained from single-shear bond tests and tensile tests on the glass fabric have been combined to obtain the qualification parameters $\sigma_{lim,conv}$ and $\epsilon_{lim,conv}$ of the TRM system [20,48]. In this study, $\sigma_{lim,conv}$ has been taken equal to the average value of the peak stress in the textile, while $\epsilon_{lim,conv}$ is the corresponding strain in the bare fabric evaluated through tensile tests (see Fig. 5).

347

It can be observed that, since in single-shear bond tests no slippage of the fabric neither debonding at the matrix-to-substrate interface were observed, the ultimate tensile stress obtained in single-shear tests is almost the same of that of tensile tests (the slight difference is probably due to the experimental dispersion and different boundary conditions). Furthermore, due to the failure mode that always involved the fabric outside the bonded area, no substantial differences were observed by changing the type of support.

348

349



348

349

Fig. 5 – Determination of $\sigma_{lim,conv}$ and $\epsilon_{lim,conv}$ of the TRM system applied on tuff or fired clay brick substrates, according to CNR-DT215.

350

3 UNIAXIAL COMPRESSION TESTS

351

3.1 Test description

352

353

354

355

356

357

358

359

The contribution of TRM systems to the compressive strength of masonry panels is usually considered negligible in the literature [19,20]. However, in particular cases, e.g. when TRM layers applied on both sides of the walls are connected through mechanical anchors or characterized by a significant mortar thickness (as it occurs in our study), it is reasonable that TRM systems could also influence the compressive strength and the stiffness of a wall. In fact, some recent guidelines suggest the use of approximated improvement coefficients to masonry mechanical properties (including compressive strength) in case of application of different reinforced plasters (including TRM systems) [66]. Then, the uniaxial compressive behavior of masonry panels reinforced with TRM systems should be accurately investigated to avoid unsafe overestimations of the mechanical properties.

In this study, uniaxial compression tests were carried out on tuff (T) and fired clay brick (M) masonry panels. In particular, for each masonry type, uniaxial compression tests were carried out on two unreinforced and two TRM reinforced panels. The geometrical dimensions of the panels and the layout of the connector (as suggested by the manufacturer) are reported in Fig. 6.

The vertical load was applied by using six hydraulic jacks (total compression capacity of 3000 kN), with a loading rate of approximately 2.0 kN/sec. To have a uniform load on the top of each specimen, a stiff steel plate with dimensions of 1200x400x20 mm³ was placed between the 6 hydraulic jacks and the masonry (see Figures 6). Two neoprene sheets (10 mm thick) were positioned, respectively, on the top between the steel plate and the masonry and at the bottom of each specimen, to reduce the friction between the contact surfaces and to avoid that local asperities or irregularities could cause punctual loads. Besides, in the case of reinforced panels, they let the load be applied on the masonry by keeping unchanged the load application area (1 x 0.25 m²) and to avoid to directly load the two TRM layers.

By doing so, the vertical load is transferred from the masonry to the TRM layers only through shear stresses (at the interface) or by the mechanical connectors, simulating a more realistic loading condition. In a real scenario, in fact, the TRM reinforcement is applied to masonry walls that are already subject to compression stress (i.e.: floor or roofing loads). Any increase in loads, usually transmitted by beams to the masonry, is unlikely to directly load the reinforcement in compression, while it is possible that the reinforcement is loaded by shear stresses at the interface between TRM and masonry (or that the confinement effect is activated by the presence of transversal connectors). Thus, by adopting this test setup, it is possible to assess whether the reinforcement could absorb any compression stress only through the adhesion developed at the reinforcement-to-masonry interface, or through the confinement provided by the steel anchors.

Two linear variable displacement transducers (LVDTs), with a gauge length of 300 mm, were applied to the wall surface at mid-height (LVDTs 1 and 2), to measure vertical displacements on the two sides of the wall. In the case of reinforced masonry panels, the LVDTs 1 and 2 were directly applied on the TRM surface, while LVDTs 3 and 4 were applied on the short sides of the panel, that is the unreinforced sides. A view of the experimental setup is reported in Fig. 6.

At the end of the tests, compressive stress was calculated by dividing the applied load (P) for the load application area (note that the load application area has remained unchanged for all specimens, and equal to 250x1000 mm², nominally). Vertical strain (ϵ_v) was calculated as the average between the values measured by LVDT 1 and 2 for unstrengthened panels and by means of LVDTs 3 and 4 in the case of strengthened panels. The secant modulus of elasticity (E_m) was then evaluated by considering a compressive stress equal to 1/3 of the maximum stress, as indicated in EN 1052-1 [67].

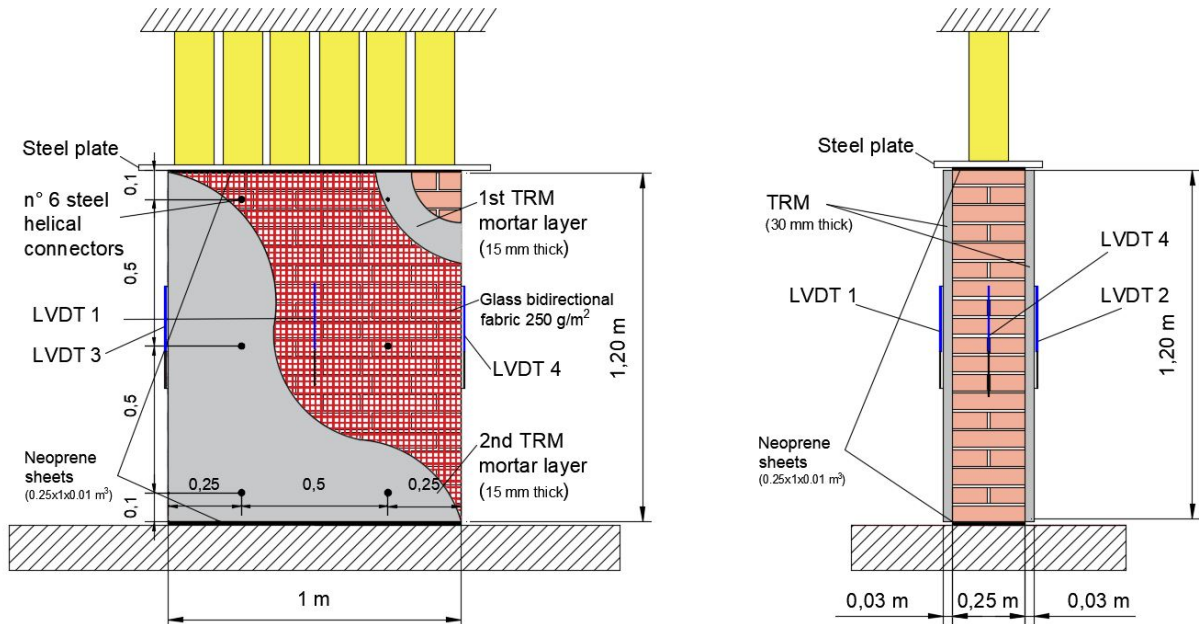


Fig. 6 – Compression test setup.

3.2 Experimental results and discussion

Experimental results of compression tests are summarized in Table 6 in terms of maximum load (P_{max}), compressive strength (f_m) and elastic modulus (E_m). Average values of compressive strength ($f_{m,AVG}$) and elastic modulus ($E_{m,AVG}$) are also reported in Table 6. Compressive stress-vertical strain curves of unstrengthened and strengthened tuff and brick masonry panels are reported in Fig. 7a and Fig. 7b, respectively. All the experimental curves follow a linear trend both for unreinforced and reinforced specimens until the maximum compressive stress is reached. Then, a quick decrease in the stress level is measured. The post-peak behavior has to be considered as indicative, mainly due to the load control testing procedure.

397

Table 6. Experimental results of compression tests on masonry panels.

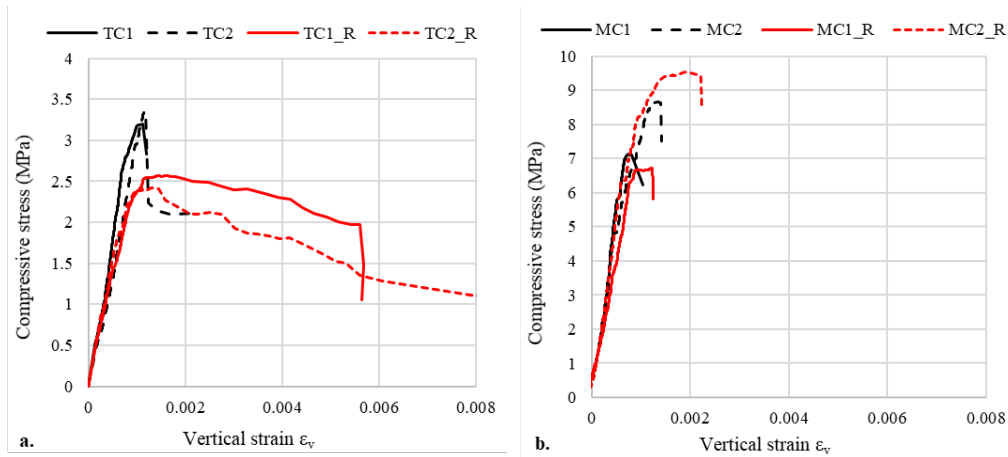
Specimen	P_{max} [kN]	f_m [MPa]	$f_{m,AVG}$ [MPa]	$f_{curtains}$ separation [MPa]	E_m [GPa]	$E_{m,AVG}$ [GPa]
TC1	798	3.19	3.28	-	3.23	2.88
TC2	840	3.36		-	2.53	
TC1_R	642	2.57	2.50	-	2.84	2.93
TC2_R	604	2.42		-	3.03	
MC1	1781	7.12	7.89	5.41	9.63	9.67
MC2	2163	8.65			9.71	
MC1_R	1676	6.70	8.07	6.32	8.29	9.10
MC2_R	2360	9.44			9.90	

398

399

Specimen	P_{max} [kN]	f_m [MPa]	$f_{m,AVG}$ [MPa]
T1	798	3.19	3.28
T2	840	3.36	
T1R	642	2.57	2.50
T2R	604	2.42	

400



401

Fig. 7 – Compressive stress-vertical strain of (a) tuff and (b) fired clay brick masonry panels unreinforced (TC1, TC2, MC1, MC2) and strengthened with TRM (TC1_R, TC2_R, MC1_R, MC2_R).

402

403

404

405

406

407

408

409

410

411

412

413

414

415

416

417

418

419

420

421

422

423

Concerning the tuff panels, the unreinforced ones showed the first vertical crack at about 1.0 MPa and an average compressive strength ($f_{m,AVG}$) of 3.28 MPa (see Table 6), which is in very good agreement with the estimate proposed by NTC2018 (§11.10.3.1.2) [68] for masonry with compressive strength of the constitutive elements of 5 MPa and M5 joint mortar (equal to 3.3 MPa). At the end of the test, several sub-vertical cracks were noticed together with crushing at the panel base (see Fig. 8a).

Reinforced tuff panels showed an elastic modulus in compression quite similar to that obtained for the unreinforced specimens. Conversely, an average compressive strength (about 2.50 MPa) lower than that obtained for unreinforced ones (3.28 MPa) was obtained, with failure occurring due to the collapse of the internal masonry and without a significant contribution of the TRM reinforcement. Furthermore, once the maximum compressive load was almost reached, the TRM curtains detached from the substrate (see Fig. 8b). In this way, helical steel connectors showed to be ineffective probably due to their low axial stiffness and, to a minor extent, because they were dry inserted within the masonry (as suggested by the manufacturer) and simply folded over the glass fabric for a length of 100 mm. The higher stiffness of the TRM matrix, if compared to that of tuff masonry, could also have played a role. However, the detachment only occurred for high values of the compression load and vertical strain. The decrease in the compressive strength may be attributable to the presence and the layout of the steel connectors. In fact, the presence of helical steel connectors, dry inserted within the masonry and aligned along two vertical lines, probably caused stress concentration and notch effect, also reducing the resistant section and, therefore, the load-bearing capacity of the wall. This constituted a preferential way for cracking start. Once the TRM was removed, it was possible to observe the formation of two vertical cracks on the wall right near the steel connectors, and in one case even the detachment and collapse of the relative corner (Fig. 8c).



Fig. 8 – Failure modes of the unstrengthened (a) and strengthened (b,c) tuff masonry walls.

Concerning the fired clay brick specimens, the average compressive strength of unreinforced masonry walls was found to be of about 7.9 MPa, which is in good agreement with the estimate proposed by NTC2018 (§11.10.3.1.2) [68] for fired clay brick masonry with compressive strength of 30 MPa and M5 joint mortar (equal to 8.6 MPa). Several cracks formed in both unreinforced and reinforced specimens before reaching compressive stress ranging between 5 and 6 MPa. Once reached this stress level, the walls were divided into two separate curtains due to the formation of a vertical crack on the short side of the panel (see Fig. 9b). In particular, in the unreinforced case, this vertical crack occurred at about 5.4 MPa (see $f_{\text{curtains separation}}$ in Table 6).

Differently, in the reinforced case, the presence of TRM and steel connectors slightly delayed the curtains separation, which happened at a compressive stress of about 6.3 MPa. In any case, from this stress level, the walls continued to take load, although being constituted by two separate curtains, and further vertical cracks formed on the TRM mortar (Fig. 9c) until the collapse was reached. The ultimate average compressive strength was found to be of about 8 MPa. However, this result has high variability, due to the limited number of specimens tested. Finally, even in this case, the elastic modulus in compression seemed not to be affected by the presence of the TRM reinforcement.



Fig. 9 – Failure modes of the unstrengthened (a) and strengthened (b,c) fired clay brick masonry walls.

The helical steel bars were not able to avoid the separation of the wall in two different curtains, even in this case, probably due to their low axial stiffness and, to a minor extent, to the poor bond between bars and masonry (since they were dry inserted, as suggested by the manufacturer). Thence, the confining effect of the TRM reinforcement was modest, as well as the stress transfer between masonry and reinforcement, as underlined by the few cracks appeared on the TRM reinforcing layers, which, however, and differently from the tuff case, remained well adherent to the substrate for the whole duration of the test (Fig. 9b and c), probably thanks to the better adhesion and the greater compatibility, in terms of elastic modulus, between the TRM matrix and the support.

4 DIAGONAL COMPRESSION TESTS

4.1 Test description

It is well recognized that the diagonal compression test does not necessarily reproduce a field condition. However, it can be used to identify the shear mechanical parameters of structural elements [69–72], which are necessary for design purposes and numerical modeling [73–75], as well as to evaluate the effectiveness of different reinforcement systems on the shear behavior of masonry panels [76].

In this study, diagonal compression tests were performed according to ASTM E519-15 [76] to determine the diagonal compression strength of tuff and fired clay brick masonry panels and to evaluate the effectiveness of the TRM strengthening system. For each masonry type, a total of 6 walls with nominal dimensions of 25x120x120 cm³ (excluding the reinforcement thicknesses), were tested, the half of which were left unreinforced and used as control (TD1, TD2, TD3; MD1, MD2, MD3), and the other half were strengthened with the adopted TRM system considering the layout of the connectors reported in Fig. 10a (TD1_R, TD2_R, TD3_R; MD1_R, MD2_R, MD3_R).

A monotonic diagonal compression load, at approximately 1.0 kN/sec, was applied on the diagonally opposite corners using two loading steel shoes and six hydraulic jacks with a total load capacity of 3000 kN [77–79] (see Fig. 10b).

Four sheets of neoprene (15x25x1 cm³), placed between the panels and the steel shoes (two for each corner), were used to reduce friction between the contact surfaces and to avoid possible stress concentration. Moreover, in the case of reinforced walls, the use of neoprene sheets allowed to apply the load on the masonry by keeping unchanged the load application area (30x25 cm²), avoiding to directly load the two reinforcing TRM layers, just as done for the uniaxial compression tests.

Finally, four inductive LVDTs were installed along the panel diagonals (two for each side, see Fig. 10b). All data were recorded by using a Spider8 data acquisition system operating Catman software at a frequency of 2 Hz.

According to ASTM E519-15 [76], the shear stress is computed as $\tau = 0.707 P/A_w$, where P is the applied load and A_w is the net cross-sectional area of the masonry wall (that is, the cross-section because the reinforcement is not considered). The shear strains are instead computed as $\gamma = \epsilon_v + \epsilon_h$, where ϵ_v and ϵ_h are the average strains, in absolute value, along the compression and tensile diagonals of the panels, measured by LVDT1,2 and LVDT3,4, respectively. It should be noted that the application of the ASTM E519-15 formula for τ calculation may not be completely correct in the case of reinforced panels since the reinforced specimens consist of two different coupled materials (masonry and TRM). However, it can be used to provide a measure of the increase in shear strength provided by the reinforcing TRM layers to the unreinforced masonry panel, as also carried out in many other studies in this field (see e.g. [6,21,24,49,80–82]).

For each test, the shear strain corresponding to the shear stress at cracking γ_{cr} (cracking of the masonry in unreinforced panels or cracking of the TRM in reinforced panels according to several studies in the literature [24,27,82]) and the ultimate shear strain at failure γ_u are also computed. In particular, in the case of unreinforced panels, γ_{cr} is expected to be equal to γ_u due to the brittle behavior of unreinforced masonry panels. In the case of TRM reinforced panels, instead, γ_u is computed as the largest strain experienced during the test or, conventionally, the strain at a level of shear stress 20% below the peak in case of a shear stress-strain diagram with a post-peak descending branch [21,82,83].

Finally, the shear stiffness modulus of the panels (G) is also calculated. In the literature, G is generally computed as a tangent modulus, secant modulus, or chord modulus for different stress levels [24,84]. In this study, the shear modulus is computed as the secant modulus between 10 and 40% of the maximum strength on the experimental τ - γ curve [26]. Even if, in the case of reinforced panels, the LVDTs were directly applied on the external surface of the TRM layer, strains recorded (and so the shear stiffness modulus G) can be considered reliable, since no detachment at the interface between TRM and masonry were observed for this load level ($\leq 0.4 \tau_{max}$) and a substantially perfect bond at the interface can be assumed at least up to the cracking of the TRM mortar.

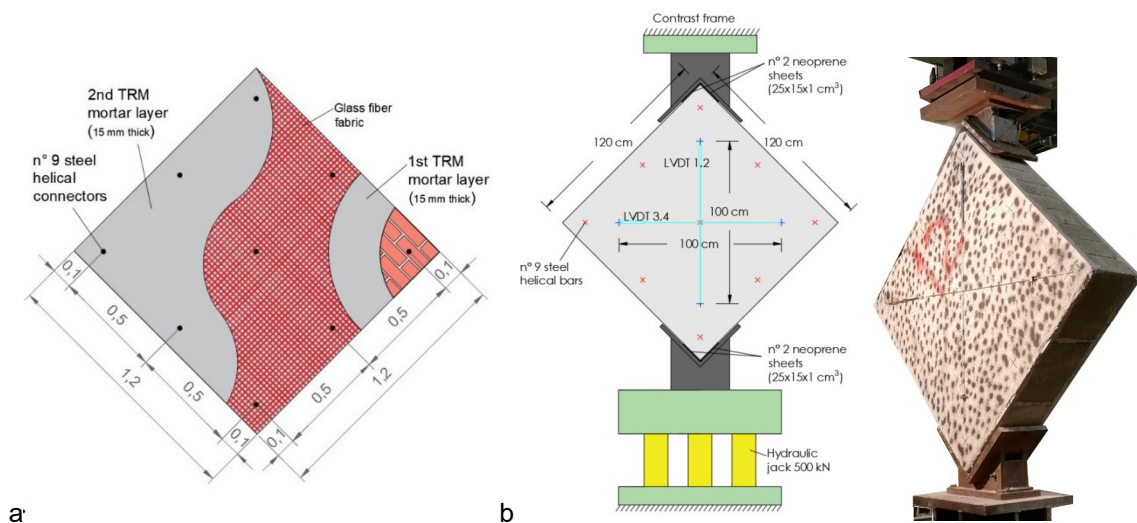


Fig. 10 – (a) Layout of the connectors and (b) diagonal compression test setup.

4.2 Experimental results and discussion

In Fig. 11, the experimental results in terms of shear stress-strain curves are shown, while Table 7 reports, for each tested panel, the results in terms of maximum load V_{max} , shear strength τ_{max} , shear strain at cracking γ_{cr} , ultimate shear strain γ_u and shear stiffness G . The shear strength and stiffness average values ($\tau_{max,AVG}$ and G_{AVG} , respectively) and the respective ratios between reinforced (RM) and unreinforced (URM) masonry panels were also reported.

As expected, all the unreinforced panels (TD1, TD2, TD3 and MD1, MD2, MD3) showed an almost linear-elastic behavior up to reaching the maximum shear stress τ_{max} (see Fig. 11). Some tuff panels (in particular TD2 and TD3) showed a gradual stiffness degradation before reaching the maximum shear stress, due to cracks formation (see Fig. 11). Then, once the principal tensile stresses reached the tensile strength of the wall (in particular at the interfaces between blocks and mortar [83,84]) a brittle failure occurs (expulsion of unconfined corners, see Fig. 12a and c).

Clearly, a significant difference between unreinforced tuff and fired clay brick masonry panels can be observed in terms of both shear stiffness G (342 and 2933 MPa, respectively, see Table 7), due to the different mechanical properties of the blocks, and shear strength $\tau_{max,AVG}$ (0.20 and 0.93 MPa, respectively, see Table 7), due to the different bond strengths at the interface between blocks and mortar joints.

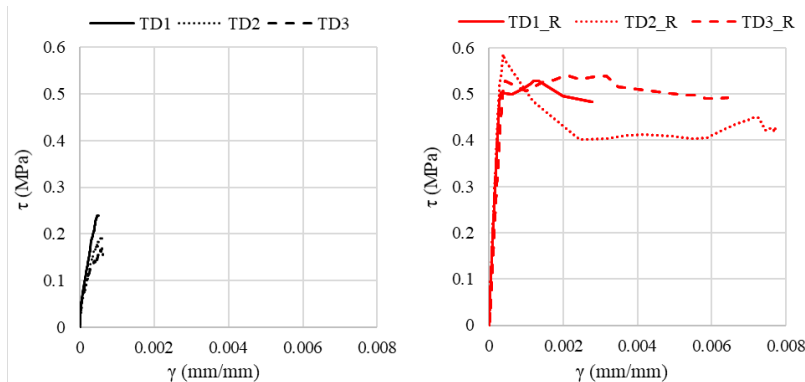
Concerning the reinforced specimens (TD1_R, TD2_R, TD3_R and MD1_R, MD2_R, MD3_R), for both masonry types, the shear stress-strain curves (Fig. 11) showed an initial linear-elastic behavior (G values equal to 1383 and 2895 MPa for tuff and fired clay brick masonry cases, respectively) up to the maximum shear stress τ_{max} (equal to 0.54 and 1.24 MPa, respectively), corresponding to the appearance of the first visible crack on the TRM layers (Fig. 12b and d). During the elastic phase, no cracking in the TRM mortar neither detachment at the TRM-to-substrate interface were observed, indicating a good bond between the inorganic mortar and the two masonry substrates.

A significant increase in G is observed in reinforced tuff panels (about +304%), while in the case of reinforced fired clay brick panels the stiffness was not substantially affected by the TRM reinforcement. These results are in line with those obtained for similar TRM systems applied to other types of masonry, where it is observed that the lower the stiffness of the unreinforced panel, the higher the stiffness increase of the strengthened one (see e.g. [29]).

Concerning τ_{max} , TRM reinforced panels exhibited an increase of about 2.7 and 1.3 times for tuff and fired clay brick masonry panels, respectively, with respect to unreinforced panels (see Table 7). It should be noted that these increases are quite different from the multiplicative coefficients provided by CNR-DT215 [20] for FRCM systems, which indicate strength values 2 times and 1.7 times higher than that of unreinforced masonry panels for tuff and fired clay brick masonry, respectively. As a result, these latter values may provide an inaccurate evaluation of the ultimate shear strength of the reinforced panel (even on the unsafe side for the fired clay brick masonry) and should be carefully evaluated before applying for the design of TRM systems. However, it is interesting to observe that the increase in terms of diagonal load was almost the same both for tuff and fired clay brick masonry panels (about 130-140 kN), thus indicating that the reinforcement provided its maximum contribution in terms of mechanical strength in both cases.

Table 7. Experimental results of diagonal compression tests. CoV in round brackets.

Specimen	V_{max} [kN]	$V_{max,AVG}$ [kN]	τ_{max} [MPa]	$\tau_{max,AVG}$ [MPa]	γ_{cr} [mm/mm]	γ_u [mm/mm]	G [MPa]	G_{AVG} [MPa]	$\frac{\tau_{RM}}{\tau_{URM}}$	$\frac{G_{RM}}{G_{URM}}$
TD1	101.37		0.24		0.00055	0.00055	431			
TD2	81.96	85.57	0.19	0.20	0.00059	0.00059	309	342	-	-
TD3	73.38	(16%)	0.17	(16%)	0.00063	0.00063	285	(23%)		
TD1_R	213.58		0.50		0.00032	0.00277	1394			
TD2_R	248.89	228.72	0.59	0.54	0.00038	0.00117	1555	1383	2.70	4.04
TD3_R	223.68	(8%)	0.53	(8%)	0.00044	0.00673	1199	(13%)		
MD1	383.05		0.90		0.00038	0.00038	2368			
MD2	362.37	396.50	0.85	0.93	0.00024	0.00024	3542	2933	-	-
MD3	444.07	(11%)	1.04	(11%)	0.00036	0.00036	2889	(20%)		
MD1_R	509.89		1.20		0.00029	0.00376	2667			
MD2_R	586.55	526.20	1.38	1.24	0.00039	0.00060	3366	2895	1.33	0.99
MD3_R	482.15	(10%)	1.14	(10%)	0.00022	0.00141	2651	(14%)		



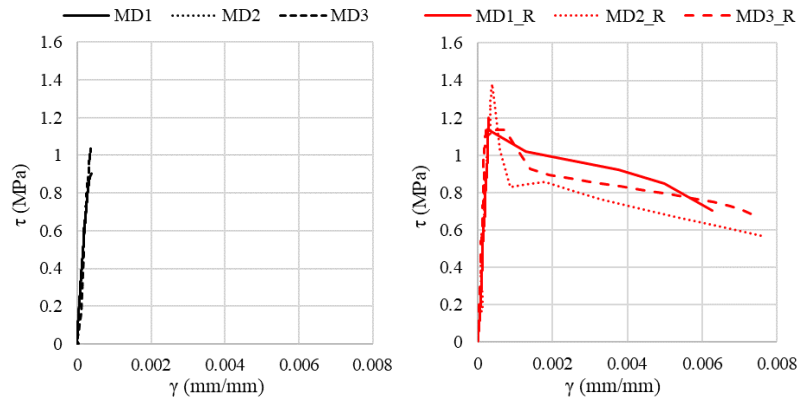


Fig. 11 – Diagonal shear stress (τ) – shear strain (γ) diagrams of tuff (I) and fired clay brick (M) masonry panels.

Despite the post-peak behavior has to be intended as qualitative due to the test method under load control, as made in other studies (see e.g. [5,22,27,82]), a post-peak and pseudo-ductile behavior due to the presence of the fabric was surely observed in all reinforced specimens and some considerations on the observed behavior can be made. In particular, once the TRM has cracked, a slightly different post-cracking behavior is observed in tuff and fired clay brick masonry.

In reinforced tuff panels, the load remained almost constant while further cracks appeared along the vertical diagonal and cracks opening increased. This could be ascribable to the glass yarns layout that is at about 45° with respect to the main direction of the cracks, so that the fabric, after the crack of the mortar, can deform rigidly until its yarns align themselves perpendicularly to the crack direction. With increasing displacement, the stresses in the wall are redistributed in the reinforcing fabric, and the load is maintained approximately constant up to reach fibers breakage (Fig. 13a). Thus, the glass fabric plays an important role in the post-cracking phase, contrasting the rapid strength decrease and guaranteeing a considerable increase in the deformation capacity of the panels. At the end of the pseudo-ductility phase, detachment of TRM mortar from the tuff substrate at the corners of the panels (Fig. 13b) and crushing of the internal masonry at the masonry toes were also observed in some tuff panels, probably due to the presence of the dry inserted bar that weakens the masonry toe bearing capacity (Fig. 13c).

Differently, in reinforced fired clay brick masonry, a slow load drop in the stress-strain curves is observed after the formation of the first crack in the TRM matrix. The greatest load drop observed in this case is probably due to the greater energy release upon reaching the cracking of the TRM matrix. As the cracks opening increases, failure of some glass yarns was also observed at the center of the panels in correspondence with the main crack.



Fig. 12 – Failure modes of tuff and fired clay brick masonry panels: (a) tuff unreinforced; (b) tuff reinforced with TRM; (c) fired clay brick unreinforced; (d) fired clay brick reinforced with TRM.



Fig. 13 – Details of the failure modes observed: (a) fabric failure, (b) detachment of the TRM curtains (tuff panels), (c) masonry crushing (tuff panels).

4.3 Analytical formulations

Since no analytical formulations to predict diagonal compression strength can still be found in current codes for masonry panels strengthened with matrices thicker than 15 mm, as well as, for CRMs, in this study the recent approaches provided by ACI549 [19] and CNR-DT215 [20] guidelines for the design of strengthening interventions with FRCM were the starting point.

Accordingly, the shear strength of the reinforced wall (V_{RM}) can be calculated as the sum of the contribution of the unreinforced masonry (V_{URM}) and that of the reinforcement (V_{TRM}):

$$V_{RM} = V_{URM} + V_{TRM} \quad (1)$$

where V_{URM} can be assumed as the average of the maximum load obtained from the diagonal shear tests of unreinforced tuff or fired clay brick masonry panels (TD1, TD2, TD3; MD1, MD2, MD3), and V_{TRM} is computed according to the different formulations provided by ACI549 [19] and CNR-DT215 [20] (see Section 4.3.1 and 4.3.2).

Then, to better investigate the role of the TRM matrix (that is currently neglected in the ACI549 [19] and CNR-DT215 [20] approaches), novel analytical procedures based on ASTM E519 formulas, including the TRM matrix tensile strength, were also considered (see Sections 4.3.3).

4.3.1 ACI 549

According to ACI549 [19], the mechanical parameters needed to design a strengthening intervention are those obtained through uniaxial tensile tests on prismatic coupons (AC434 [41]), using a clevis grip anchor mechanism (see Section 2.2.1). The analytical approach provided by ACI549 is calibrated on FRCM systems, that concern very thin thickness of the mortar coating, thus it may lead to inaccurate evaluations of the reinforcement contribution when higher mortar thicknesses are used. However, since there are no proven analytical models for the reinforcement system used in this study, it is worth using this approach to evaluate its predictive capability in comparison with other analytical approaches described in the following sections.

According to [19], the TRM contribution to the shear resistance capacity of the wall V_{TRM} is calculated with Eq. (2):

$$V_{TRM,ACI} = 2 \cdot n_f \cdot t_f \cdot b_w \cdot f_{TRM} \quad (2)$$

where t_f is the equivalent thickness of one fabric layer with yarns arranged in a direction parallel to the shear force, n_f is the number of fabric layers and b_w is the width of the masonry panel. The tensile strength in the TRM shear reinforcement, f_{TRM} , is calculated with Eq. (3):

$$f_{TRM} = E_{u,TRM} \cdot \varepsilon_{u,TRM} \quad (3)$$

where $E_{u,TRM}$ is the tensile modulus of elasticity of TRM specimens in the cracked phase. ACI 549 [19] also imposes a limit on the ultimate strain of the TRM system $\varepsilon_{u,TRM}$, which should not exceed 0.004 in the calculation. The TRM contribution has been also evaluated without considering the strain limitation, by using the effective ultimate deformation and elastic modulus ($\varepsilon_{u,TRM}$, $E_{u,TRM}$) obtained by tensile tests on TRM coupons (reported as $V_{TRM,ACI}^I$ in Table 8).

4.3.2 CNR-DT215

CNR-DT215 [20] suggests two approaches to evaluate the increase in the shear capacity due to the TRM application. The first one is a simplified approach, based on empirical amplification factors. For instance, in the case of tuff masonry walls, a design coefficient $V_{RM}/V_{URM} = 2$ is suggested, while in the case of fired clay brick masonry the coefficient is equal to 1.7. The second approach, used in our work, provides a predictive equation to compute the TRM contribution on the shear strength capacity of masonry panels:

$$V_{TRM,CNR} = 0.5 \cdot n_f \cdot t_f \cdot b_f \cdot \alpha_t \cdot \varepsilon_{lim,conv} \cdot E_{fabric} \quad (4)$$

where 0.5 is a safety factor (set equal to 1 in our study to allow a useful comparison with other formulations); b_f is the dimension of the reinforcement measured orthogonally to the shear force (equal to 1200 mm) and α_t is a coefficient that takes into account the reduced extensional strength of the fibers when subject to shear stresses (in the absence of proven experimental results, it can be assumed equal to 0.80) [20]. E_{fabric} is the elastic modulus of the bare fabric strip (see Table 3). Finally, the strain $\varepsilon_{lim,conv}$ is the conventional deformation obtained in Section 2.2.2. The TRM contribution has been also evaluated without considering the reduction factor α , and assuming $\varepsilon_{lim,conv} = \varepsilon_{u,fabric}$ (reported as $V_{TRM,CNR}^I$ in Table 8).

4.3.3 ASTM E519 [85]

A simplified analytical formulation to predict the diagonal compression resistance of reinforced panels can be conducted considering the inorganic matrix contribution only, i.e. assuming that the fabric does not play a significant role before the cracking of the TRM matrix and considering the hypothesis of no-slip between masonry substrate and TRM matrix. These assumptions seem plausible since the first visible cracks on the TRM matrix only appeared when the maximum diagonal compression load was almost reached. Therefore, for this TRM system, the inorganic matrix plays a fundamental role in determining the maximum compression load of reinforced panels whereas the fabric deforms up to failure guaranteeing a good pseudo-ductile behavior in the post-cracking phase, as also observed by other authors for CRM systems [6,22].

According to that, it is necessary to find out and to validate analytical models able to predict the peak load observed in the diagonal shear tests considering the resistant contribution of the TRM matrix only. D'Antino et al. [6] proposed an analytical formulation, based on ASTM E519-15 [85], where the contribution of the TRM matrix to the shear strength of the panels can be computed as follows:

$$V_{TRM,ASTM} = \frac{L}{0.707} \cdot f_{t,M} \cdot n \cdot t_m \quad (5)$$

where L is the width of the square panel (equal to b_w), n is the number of TRM layers, $f_{t,M}$ is the tensile strength of the TRM matrix and t_m is the thickness of the TRM matrix (equal to 30 mm in this study). Similar formulations have been also proposed by Rilem TC 76-LUM [86] and Gattesco and Boem [22]. However, there are still no standardized methods to determine the tensile strength of the TRM matrix $f_{t,M}$. A first attempt would be to use the tensile stress of the TRM mortar at first cracking $\sigma_{t,M}$ (see section 2.2.1). According to that, the shear strength of reinforced panels has been reported in Table 8 as $V_{RM,ASTM}$. Then, the formula provided by Model Code 2010 [87] to estimate the concrete tensile strength from its flexural strength has been also used. This formula can be adapted to evaluate the TRM tensile strength as follows:

$$f_{t,M} = \frac{f_f \cdot 0.06 \cdot h_b^{0.7}}{1 + 0.06 \cdot h_b^{0.7}} \quad (6)$$

where h_b is the depth of the beam specimen used in the flexural test (40 mm in our case). For this study, $f_{t,M}$ calculated with Eq. (6) is equal to 1.31 MPa, thus the ratio $f_f/f_{t,M}$ is about 2.27. Bellini et al. [88] performed tensile tests on TRM coupons of different thicknesses and flexural tests on prismatic specimens (40x40x160 mm³) and found out that the ratio between the tensile strengths resulting from the two different tests methods ($f_f/f_{t,M}$) was about 2 for lime-based mortars and about 3 for cementitious mortars. Therefore, the value obtained with Eq. 6 can be considered within an acceptable range. Analytical predictions of the shear strength of reinforced panels, where the TRM tensile strength was calculated with Eq. 6, have been reported in Table 8 as $V_{RM,ASTM}^I$.

4.4 Analytical results and discussion

In this section, the analytical estimations of the shear strength of reinforced (RM) masonry panels, calculated according to recent guidelines (ACI549 [19], CNR-DT215 [20]) or considering novel approaches [6,85] have been compared with the average experimental peak shear force sustained by reinforced panels (V_{RM}) and reported in Table 8.

Analytical results suggest that the ACI and CNR formulations generally provide conservative estimations of the shear strength of reinforced panels (see Table 8). In particular, the strain limitation $\epsilon_{u,TRM} \leq 0.004$ provided by ACI549 [19] seems to be very conservative. This is probably because ACI549 [19] was calibrated on FRCM systems reinforced with carbon or PBO fibers, thus considerably limiting the design performance of TRM systems reinforced with glass fabrics (higher deformation at failure than carbon or PBO fibers and lower elastic modulus). For this reason, the shear strength is also evaluated without considering the strain limitation, by using the effective ultimate deformation and the elastic modulus ($\epsilon_{u,TRM}$, $E_{u,TRM}$) obtained from tensile tests on TRM coupons (see Section 2.2.1), and reported in Table 8 as $V_{RM,ACI}^I$. By doing so, the analytical predictions are closer to the experimental values. However, $V_{RM,ACI}^I$ is still about 22% and 7% lower than the experimental shear strength in the case of tuff and fired clay brick reinforced panels, respectively. Probably, the greater thickness of the TRM matrix applied to the masonry panels (30 mm), with respect to that used for characterization tests (12 mm), may have led to some underestimation of the TRM contribution.

CNR-DT215 provided higher values of the analytical shear strength, although this is always lower than the experimental outcomes. If reduction factors are not considered ($V_{RM,CNR}^I$ in Table 8) the analytical values are about 20% and 6% lower than the experimental ones for tuff and fired clay brick panels, respectively. These results confirm that the analytical predictions of ACI549 and CNR-DT215 tend to underestimate the experimental values when reinforcements of greater thickness than those usually employed for FRCM are used, being however on the safe side. Thus, for these latter cases, some different predictive formulations have to be considered, too.

In this study, the procedure suggested by D'Antino et al. [6] for CRMs, according to ASTM E519-15 [85], was followed, by taking into account the influence of the inorganic matrix on the mechanical behavior of reinforced panels. However, since there are no standardized procedures to uniquely determine the tensile strength of the TRM matrix, the results are rather influenced by the test method used. When the tensile stress of the TRM matrix at first cracking has been used ($\sigma_{t,M}$) experimental results are overestimated of about 12% and 8% for tuff and fired clay brick panels, respectively (see $V_{RM,ASTM}$ in Table 8). This may be in part attributed to the different thickness of the TRM matrix adopted in characterization tests (equal to 12 mm) which may have slightly overestimated the tensile strength. On the other hand, when the formula proposed by the Model Code 2010 [89] has been used, analytical predictions are much closer to the experimental results ($V_{RM,ASTM}^I$ in Table 8).

These outcomes confirm that the matrix thickness and its mechanical properties may considerably affect the effectiveness of the strengthening system, especially when the thickness of the mortar is not typical of FRCMs (i.e. equal to or greater than 30 mm) and should be taken into account in predictive models. This may happen when TRM systems are connected by means of steel or GFRP anchors, therefore a greater thickness of mortar is needed to fully cover such connectors. When this happens, the TRM matrix seems to assume a major role in determining the maximum shear strength of the reinforced masonry panel, while the internal fabric guarantees the ductile behavior in the post-cracking phase. These analytical approaches should be however refined, gathering a large number of experimental results, to improve their predictive accuracy.

Table 8. Analytical estimations of the shear strength according to ACI549 [19], CNR-DT215 [20] and ASTM E519 [85]. Percentage differences with average experimental values in round brackets.

Specimen	$V_{URM,exp}$ [kN]	V_{TRM} [kN]	$V_{RM,exp}$ [kN]	$V_{RM,ACI}$ [kN]	$V_{RM,ACI}^I$ [kN]	$V_{RM,CNR}$ [kN]	$V_{RM,CNR}^I$ [kN]	$V_{RM,ASTM}$ [kN]	$V_{RM,ASTM}^I$ [kN]
Tuff	85.57	143.15	228.72	93.18 (-59.3%)	178.81 (-21.8%)	158.28 (-30.8%)	183.02 (-20%)	255.64 (+11.8%)	218.98 (-4.3%)
Fired clay brick	396.5	129.7	526.20	404.11 (-23.2%)	489.74 (-6.9%)	469.21 (-10.8%)	493.95 (-6.1%)	566.57 (+7.7%)	529.91 (+0.7%)

5 THREE-POINT BENDING TESTS

5.1 Test description

To evaluate the effectiveness of the TRM system under out-of-plane actions, three-point bending tests were carried out on a total of 6 walls for each masonry type, the half of which was left unreinforced and used as control (TB1, TB2, TB3; MB1, MB2, MB3), while the other half was strengthened with TRM on both the two faces of the panels (TB1_R, TB2_R, TB3_R; MB1_R, MB2_R, MB3_R). The geometrical dimensions of the panels, the layout of the connectors and the test setup are reported in Fig. 14.

The panels and the reinforcement were preliminary designed so that undesired failure modes, due to shear in masonry or TRM delamination, did not anticipate the out-of-plane collapse of reinforced specimens. As a result, the shear span to masonry cross-section depth ratio (where the shear span is the distance between the support and the closest point of application of load) was equal to 2.26 for unreinforced panels, similar to those observed in other experimental works, where values ranging from 1.3 up to 3.7 were employed (see e.g. [45]). The TRM textile mechanical ratio (ρ_f), computed according to the Eq. (7) (where b_w and t_w are the width and thickness of the masonry wall), is equal to 0.051 and 0.021 for tuff and fired clay brick masonry panels, respectively.

$$\rho_f = \frac{A_{fabric} \cdot \sigma_{u,fabric}}{b_w \cdot t_w \cdot f_m} \quad (7)$$

The test was carried out by applying a horizontal force at mid-height of the walls, with a direction perpendicular to the wall surface. The reaction frame is constituted by two horizontal steel tubes fixed to an external structure (see Fig. 14a). The horizontal load was applied by a hydraulic jack (maximum capacity of 100 kN) controlled by a 700 bar ENERPAC electric hydraulic unit equipped with a load regulator. No vertical compression load was applied. To evaluate displacements and deflections, two LVDTs were placed close to the top and bottom restraints (LVDTs 1 and 3) and one at mid-height (LVDT 2), on the wall face stressed in tension (as represented in Fig. 14a). Some additional transducers (LVDTs 4 and 5) were placed on the compressed face to measure possible horizontal translations and/or rotations of the upper and bottom constrained sections. All the LVDTs were fixed on steel tripods, independent from the experimental apparatus. Tests were conducted in force control, by applying a monotonic load with a loading rate of about 100 N/s.

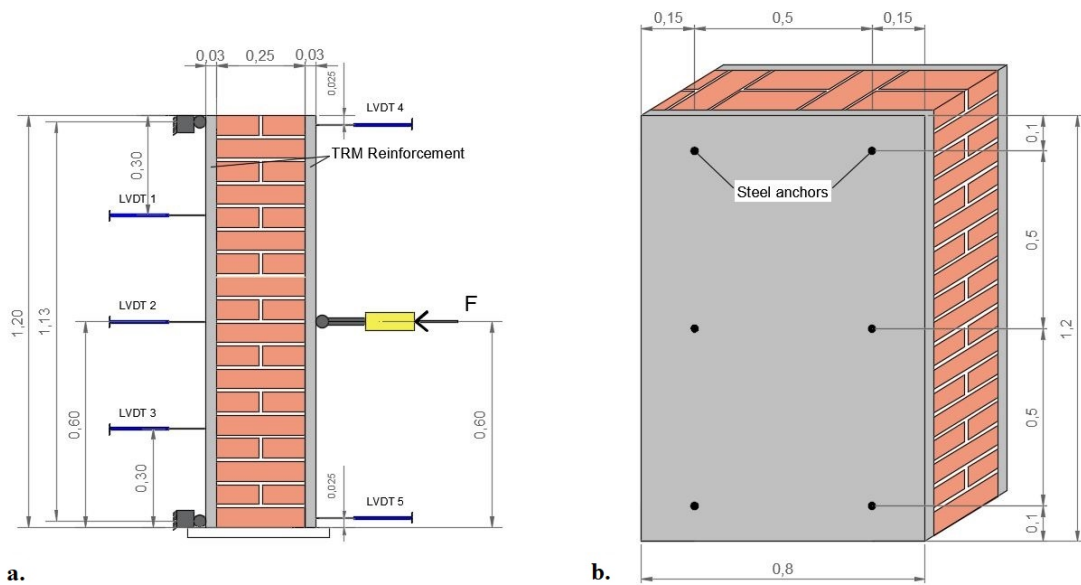


Fig. 14 – (a) Experimental setup for three-point bending tests on tuff and fired clay brick masonry panels and (b) position of helical steel anchors.

5.2 Experimental results and discussion

Experimental curves in terms of horizontal load and midspan displacement are reported in Fig. 15. Load-displacement curves of all unreinforced specimens (Fig. 15a,c) showed a linear-elastic branch followed by a brittle failure, caused by loss of adhesion at the masonry-joint mortar interface.

Unreinforced tuff panels failed at a low load level, after developing a horizontal crack at about three-quarters of the total height, corresponding to the third or fourth mortar joint (Fig. 16a). This failure was expected, since the resistant bending moment cannot be considered constant along the height of the panel, due to the different adherence developed at the joint mortar-tuff block interfaces, thanks to the different vertical loads (masonry own weight) at each interface. This means that failure does not take place at mid-height of the panel but rather at about $\frac{3}{4}$ of the total height of the specimen. Furthermore, a complete detachment at the interface between horizontal joint mortar and tuff blocks was observed in all unreinforced specimens, as a consequence of the poor adherence between the two materials.

Unreinforced fired clay brick walls, instead, failed at higher load levels (horizontal load of about 11 kN) after developing a horizontal crack located at the mortar joints near the midpoint (Fig. 17a). This difference was probably due to the adherence developed at the joint mortar-brick interface, which in the case of fired clay brick is greater than for tuff panels.

Concerning the reinforced specimens, both for tuff and fired clay brick panels, TRM proved to be extremely effective in improving the out-of-plane behavior of the walls, as expected. Load-displacement curves of strengthened specimens (Fig. 15b,d) showed a linear-elastic behavior up to the formation of the first (and unique) crack at midspan on the external TRM layer subject to tensile stresses, both for reinforced tuff and fired clay brick panels. Once the external mortar layer cracked, the internal fabric was still sound and able to carry the load, significantly increasing the deformation capacity of the panel. The presence of the PVA coating seemed to guarantee a great adhesion at the fabric-to-mortar interface, avoiding the slippage of the fabric within the TRM matrix up to the failure of the fabric yarns. The improved adhesion between coated fabrics and inorganic matrices has been investigated in other specific studies from the literature [61,90–92]. The collapse occurred when the vertical tensed yarns of the glass fabric broke almost simultaneously in correspondence of the horizontal crack (Fig. 16c,d and Fig. 17c). No detachment of the two TRM layers neither significant damage on the compressed face were observed during the tests.

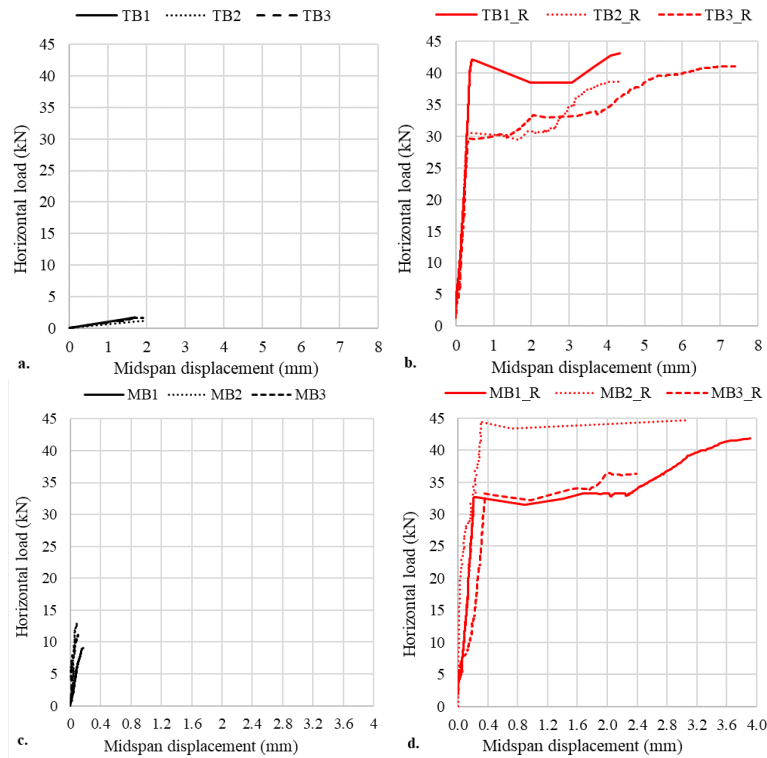


Fig. 15 – Out of plane bending tests: horizontal load-midspan displacement curves of (a) unreinforced tuff masonry (TB1, TB2, TB3), (b) reinforced tuff masonry (TB1_R, TB2_R, TB3_R), (c) unreinforced fired clay brick masonry (MB1, MB2, MB3) and (d) reinforced fired clay brick masonry (MB1_R, MB2_R, MB3_R).



Fig. 16 – Failure modes of the unstrengthened (a) and strengthened (b,c,d) tuff masonry walls.



Fig. 17 – Failure modes of the unstrengthened (a) and strengthened (b,c,d) fired clay brick masonry walls.

Experimental results in terms of first cracking load (P_{cr}), maximum horizontal load (P_{max}), maximum horizontal displacement in correspondence of first cracking (δ_{cr}) and maximum deflection (δ_u) are reported in Table 9 for each masonry wall, together with the bending moments associated to the first cracking (M_{cr}) and the ultimate bending resistance (M_u). Resistant moments were evaluated by assuming a simplified static scheme for three-point bending tests, using the following relationship:

$$M_{u,exp} = P \cdot \frac{L}{4} \quad (9)$$

where L is the distance between the horizontal supports ($L = 1.13$ m) and P is the applied load.

The average maximum horizontal load ($P_{max,AVG}$) for each category of specimens has been reported together with the corresponding CoV. The ratio between the averaged bending resistance of reinforced ($M_{u,RM,AVG}$) and unreinforced ($M_{u,URM,AVG}$) masonry walls has been also reported in the last column of Table 9.

The masonry bending resistance of reinforced panels increased drastically, of about 28 times in tuff panels and 3.7 times in those made of fired clay bricks. It is interesting to note that the ultimate average bending resistance of reinforced walls ($M_{u,exp,AVG}$) is almost the same (about 11.5 kNm), regardless of the type of masonry. Flexural failure is in fact completely controlled by the TRM layer applied on the tensed wall side, which is the same in both cases.

The wall stiffness in the elastic phase was considerably increased by the presence of the TRM, especially in the case of tuff masonry panels. The horizontal displacement at cracking (δ_{cr}) was almost the same for both tuff and fired clay brick reinforced panels, once again demonstrating the limited contribution of the internal masonry to the flexural strength of reinforced panels.

Finally, the ultimate displacement capacity (δ_u) is considerably increased by the presence of the TRM reinforcement. No detachment or sliding between the reinforcement and the support was ever observed, thus it is reasonable to assert that the load on the side subject to tensile stresses was mainly transferred by adhesion and then helical bars did not substantially work.

Table 9. Experimental results of bending tests (coefficient of variations of average values are reported in round brackets).

Specimen	P_{cr} [kN]	P_{max} [kN]	$P_{max,AVG}$ [kN]	δ_{cr} [mm]	δ_u [mm]	M_{cr} [kNm]	$M_{cr,AVG}$ [kNm]	$M_{u,exp}$ [kNm]	$M_{u,exp,AVG}$ [kNm]	$M_{u,RM,AVG}/M_{u,URM,AVG}$
TB1	1.66	1.66	1.45 (21%)	1.71	1.71	-	-	0.47	0.41 (21%)	28.19
TB2	1.10	1.10		1.85	1.85	-		0.31		
TB3	1.60	1.60		1.91	1.91	-		0.45		
TB1_R	42.09	43.00	40.93 (5%)	0.45	4.41	11.89	9.63 (20%)	12.15	11.56 (5%)	3.71
TB2_R	30.48	38.74		0.27	4.34	8.61		10.94		
TB3_R	29.70	41.05		0.36	7.40	8.39		11.60		
MB1	9.10	9.10		0.17	0.17	-		2.57	3.12	

MB2	12.95	12.95	11.05	0.09	0.09	-	-	3.64	(17%)
MB3	11.16	11.16	(17%)	0.10	0.10	-	-	3.15	
MB1_R	32.63	41.85	41.01	0.25	3.91	9.22	10.39	11.82	11.58
MB2_R	44.47	44.72	(10%)	0.22	2.98	12.56	(18%)	12.63	(10%)
MB3_R	33.21	36.47		0.36	2.38	9.38		10.30	

5.3 Analytical formulations

The experimental out-of-plane ultimate bending moments have been compared with the predictions of the different analytical formulations prescribed in the literature for both FRMC and CRM systems, in order to investigate their applicability to the adopted TRM system and/or propose possible amendments. In particular, four different formulations have been adopted in this study, i.e. those proposed by ACI 549-13 [19] ($M_{u,ACI}$) and by the Italian guideline CNR-DT215 [20] for FRCM systems ($M_{u,CNR}$ and M_{u,CNR^I} in case of adequate or inadequate bond length, respectively), and that used in Gattesco and Boem [32] for CRM systems ($M_{u,GB}$). These formulations are all based on the cross-section equilibrium, considering that plane sections remain plane during loading and that there is a perfect bond at all material interfaces [19,20,45,65]. In particular, assuming that failure occurs due to the breakage of the fiber textile, with no detachment of the composite from the substrate, nor debonding or slippage of the fabric within the matrix, the maximum resisting bending moment M_u can be computed through the following general relationship (masonry and TRM matrix tensile strengths are neglected and all partial and safety factors are set equal to unity):

$$M_u = \alpha f_c b_w \beta_1 x \left(\frac{t_w + t_{TRM}}{2} - \beta_1 \frac{x}{2} \right) + A_{fabric} f_{TRM} \left(\frac{t_w + t_{TRM}}{2} + \frac{t_{TRM}}{2} \right) \quad (8)$$

$$\text{with } x = \frac{N + A_{fabric} f_{TRM}}{\alpha f_c \beta_1 b_w}$$

where b_w is the masonry cross-section width, t_w is the masonry cross-section thickness, t_{TRM} is the thickness of the TRM system, A_{fabric} is the cross-sectional area of the fabric, x is the neutral axis depth, α and β_1 are the stress block coefficients, f_c is the compressive strength of the compressed part of the section, f_{TRM} is the ultimate tensile stress of the reinforcement and N is the compressive axial force computed by considering the whole self-weight of the specimen ($q=13$ kN/m³ for tuff masonry, $q=18$ kN/m³ for fired clay brick masonry and $q=15.2$ kN/m³ for the reinforced mortar coating).

The considered formulations mainly differ from each other in terms of stress block coefficients (α and β_1), compressive strength (f_c) and tensile strength of the reinforcement (f_{TRM}). The different assumptions are summarized in Table 10.

In particular, $M_{u,ACI}$ considers an f_{TRM} equal to the product of the TRM ultimate strain ($\epsilon_{u,TRM}$) and the elastic modulus in the cracked stage ($E_{u,TRM}$, see Section 2.2.1), then neglecting the uncracked stage of the tensile bilinear stress-strain curve of the TRM. As a result, f_{TRM} does not correspond to the maximum tensile strength of the TRM coupon. Moreover, $\epsilon_{u,TRM}$ should not exceed 0.012.

Differently, the CNR-DT215 assumes an f_{TRM} equal to the maximum tensile stress of the fibers ($\sigma_{u,fabric}$) in case of adequate bond length from support ($M_{u,CNR}$) and to the maximum tensile stress obtained from single-shear bond tests $\sigma_{lim,conv}$ (as also suggested by D'Antino et al. [45]) in case of inadequate bond length (M_{u,CNR^I}).

Finally, $M_{u,GB}$ computes f_{TRM} as $T_y n_y / A_{fabric}$, where T_y is the tensile resistance of one GFRP yarn of the fabric (computed as $T_y = \sigma_{u,fabric} A_y$, in our study) and n_y is the number of GFRP tensed yarns in the cross-section (equal to 31 in our case).

Concerning f_c , $M_{u,ACI}$ [19], $M_{u,CNR}$ and M_{u,CNR^I} [20] neglect the contribution in compression of the TRM by considering f_c equal to the compressive strength of the masonry f_m . Differently, $M_{u,GB}$ [32] considers f_c equal to the compressive strength of the TRM matrix $f_{c,M}$.

Table 10. Stress block coefficients (α and β_1), compressive strength (f_c) and ultimate tensile stress of the reinforcement (f_{TRM}) assumed for each analytical formulation considered in this study.

Formulations	α [-]	β_1 [-]	f_c	f_{TRM}
$M_{u,ACI}$ [19]	0.7*	0.7*	f_m	$\epsilon_{u,TRM} E_{u,TRM}$ with $\epsilon_{u,TRM} < 0.012$
$M_{u,CNR}$ [20]	0.85	0.7	f_m	$\sigma_{u,fabric}$
M_{u,CNR^I} [20]				$\sigma_{lim,conv}$
$M_{u,GB}$ [32]	1	0.8	$f_{c,M}$	$T_y n_y / A_{fabric}$

* in case of TRM failure in tension.

5.4 Analytical results and discussion

In this section, the out-of-plane ultimate bending moments are analytically computed and compared with experimental values in order to validate and/or propose amendments to the different analytical formulations prescribed in standards or adopted in the literature for FRCM and CRM systems.

In Table 11, the analytical predictions are reported and compared with experimental results. As expected, similar results have been obtained between the two masonry types regardless of the adopted analytical formulation, confirming that the failure is mainly governed by the reinforcing system, while the masonry type assumes a secondary role.

As also seen in previous studies [45], the ACI analytical formulation ($M_{u,ACI}$) significantly underestimates the experimental values (-77%), mainly due to the upper bound limit on $\epsilon_{u,TRM}$ (see Table 10). In this study, in fact, $\epsilon_{u,TRM}$ reaches a value equal to 0.049, which is significantly higher than the ACI limitation (0.012). As already stated in Section 4, this is probably because the ACI limitation was calibrated on FRCM systems reinforced with carbon or PBO fibers, thus considerably limiting the design performance of those TRM systems that are reinforced with glass fabrics, generally characterized by a higher deformation capacity than carbon or PBO fibers (due to the lower elastic modulus).

The CNR and the Gattesco and Boem analytical approaches, instead, provide more similar results to experimental ones, with percentage differences ranging between 12% ($M_{u,CNR}$) and 18% (M_{u,CNR^I}). Among these, the worst results are obtained from M_{u,CNR^I} , which is based on the hypothesis of inadequate bond length from support, and then simulating a detachment or debonding of the TRM from the support that was not noticed during the test.

Table 11. Comparison between experimental (“exp”) results and analytical predictions in terms of ultimate bending moments M_u according to the approaches proposed in the literature. Percentage differences in round brackets. $M_{u,GB}$ refers to Gattesco and Boem formulation [32].

Specimen	$M_{u,exp,AVG}$ [kNm]	$M_{u,ACI}$ [kNm]	$M_{u,CNR}$ [kNm]	M_{u,CNR^I} [kNm]	$M_{u,GB}$ [kNm]
Tuff + TRM	11.56	2.58 (-78%)	9.93 (-14%)	9.50 (-18%)	9.71 (-16%)
Fired clay brick + TRM	11.58	2.68 (-77%)	10.17 (-12%)	9.69 (-16%)	9.79 (-15%)

In order to improve the accuracy of the analytical formulations, some possible amendments to the ACI and CNR analytical formulas are here proposed and evaluated.

Firstly, the upper bound limitation on $\epsilon_{u,TRM}$ prescribed by ACI formulation is removed (M_{u,ACI^*}). Secondly, since in our work the neutral axis depth x is always lower than the TRM thickness, for both the ACI and CNR formulations f_c is assumed to be equal to the compressive strength of the TRM matrix $f_{c,M}$ (obtaining M_{u,ACI^+} , $M_{u,ACI^{++}}$, M_{u,CNR^+} and $M_{u,CNR^{I+}}$) as also made in [45] and in $M_{u,GB}$. This latter approach could be particularly important in the case of a TRM layer thicker than 20 mm (provided that proper anchorages of the TRM to the masonry is ensured to prevent spalling of the composite on the compressed side) and a compressive strength of the masonry significantly different from that of the TRM matrix (such as in the tuff masonry case). The amended formulations are summarized in Table 12 in terms of stress block coefficients, compressive and tensile strength to be applied to Eq. (8).

Table 12. Stress block coefficients (α and β_1), compressive strength (f_c) and tensile strength of the reinforcement (f_t) assumed for each modified analytical formulation. Amended values are reported in italics.

Formulations	α [-]	β_1 [-]	f_c	f_t
M_{u,ACI^*}			f_m	$\epsilon_{u,TRM} E_{u,TRM}$
M_{u,ACI^+}	0.7	0.7	$f_{c,M}$	$\epsilon_{u,TRM} E_{u,TRM}$ with $\epsilon_{u,TRM} < 0.012$
$M_{u,ACI^{++}}$			$f_{c,M}$	$\epsilon_{u,TRM} E_{u,TRM}$
M_{u,CNR^+}	0.85	0.7	$f_{c,M}$	$\sigma_{u,fabric}$
$M_{u,CNR^{I+}}$				$\sigma_{lim,conv}$

The results of the modified analytical formulations are reported in Table 13. In general, the amendments have provided a slightly better match with experimental results. Removing the $\epsilon_{u,TRM}$ upper bound limitation in the ACI formulation (M_{u,ACI^*}) has allowed obtaining a better match between experimental and analytical values (with a percentage difference passing from -78% to -18% and from -77% to -16% for the tuff and fired clay brick masonry cases, respectively).

Concerning the modification made on the f_c parameter (assumed equal to $f_{c,M}$), only a quite small increase in accuracy is observed for the ACI case for the tuff case only, passing from -18% (M_{u,ACI^*}) to -16% ($M_{u,ACI^{++}}$). Similarly, for the CNR formulations, as can be noted by comparing $M_{u,CNR}$ (Table 11) with M_{u,CNR^+} (Table 13) and M_{u,CNR^I} (Table 11) with $M_{u,CNR^{I+}}$ (Table 13), the highest improvement is observed for the tuff case, with percentage differences between experimental and analytical results passing from -14% to -13% for $M_{u,CNR}$ and from -18% to -15% for M_{u,CNR^I} . A smaller improvement is instead observed for the fired clay brick case, with unchanged percentage differences for $M_{u,CNR}$ and passing from -16% to -14% for M_{u,CNR^I} . The higher improvement for the tuff case is clearly due to the high difference between the compressive strength of the tuff masonry (3.28 MPa) used in $M_{u,CNR}$ and M_{u,CNR^I} and the compressive strength of the TRM (8.35 MPa) used in M_{u,CNR^+} and $M_{u,CNR^{I+}}$. For the same reason, a lower improvement is obtained in

the fired clay brick case, due to the similarity between the TRM and the masonry compressive strengths (8.35 and 7.89 MPa, respectively). In any case, it can be concluded that neglecting the compressive strength of the TRM matrix does not affect significantly the analytical predictions. Thus, also the variation of stress block coefficients can be considered not so relevant.

In conclusion, the analytical formulations prescribed by the standards for FRCM systems, slightly modified to consider the contribution of the TRM matrix in compression generally neglected in the FRCM case, provide a quite good estimate of the out-of-plane ultimate bending moments of the adopted TRM system. In particular, the $M_{u,CNR+}$ amended formulation provided the best results (-12%) while the ACI formulation ($M_{u,ACI++}$), although modified, provide the worst one. The higher error can be attributable to the more conservative assumption made for f_{TRM} . This value, in fact, is equal to 1319 for $M_{u,CNR+}$ which is higher than the f_{TRM} value considered in the ACI modified analytical procedure $M_{u,ACI++}$ (1237 MPa). This underestimation can be attributed to the conservative use of the elastic modulus in the cracked stage ($E_{u,TRM}$) for the f_{TRM} calculation in the ACI formulation, which neglects the uncracked part of the bilinear curve [45].

Table 13. Comparison between experimental (“*exp*”) results and analytical (“*calc*”) predictions obtained from modified analytical formulations in terms of ultimate bending moments M_u . Percentage differences in round brackets.

Specimen	$M_{u,AVG,exp}$ [kNm]	$M_{u,ACI+}$ [kNm]	M_{u,ACI^*} [kNm]	$M_{u,ACI++}$ [kNm]	$M_{u,CNR+}$ [kNm]	$M_{u,CNR^{I+}}$ [kNm]
Tuff + TRM	11.56	2.60 (-78%)	9.47 (-18%)	9.66 (-16%)	10.10 (-13%)	9.86 (-15%)
Fired clay brick + TRM	11.58	2.68 (-77%)	9.73 (-16%)	9.74 (-16%)	10.18 (-12%)	9.92 (-14%)

* without limiting the effective tensile strain $\varepsilon_{u,TRM}$ to 0.012.

+ f_c is the compressive strength of the TRM matrix ($f_{c,M}$).

6 CONCLUSIONS

In this work, a wide experimental campaign is carried out to evaluate the effectiveness of a glass TRM reinforcing system, with a total thickness of 30 mm, applied to two different masonry types under in-plane and out-of-plane actions. The experimental campaign consisted of 8 uniaxial compression tests, 12 diagonal compression tests and 12 bending tests on tuff and fired clay brick masonry panels strengthened with a glass TRM system. TRM mechanical characterization was carried out according to both the recent guidelines AC434 and CNR-DT215, in order to determine the parameters needed for design purposes and analytical predictions. Moreover, the different analytical formulations used in the literature to predict the in-plane shear and the out-of-plane bending strength of TRM strengthened masonry panels are compared and discussed.

The results showed that the application of a TRM layer on both faces of masonry panels did not substantially modify its mechanical behavior when subject to pure compressive stresses. However, the presence of helical steel bars, aligned on the same vertical, led to crack localization on such vertical and a slight compression strength reduction (especially on tuff masonry panels). This is an important result, also considering that some recent guidelines suggest the adoption of approximated improvement coefficients for the compressive strength of masonry panels in case of strengthening with different types of reinforced plaster (including TRM systems), that would lead to an unsafe overestimation of the mechanical behavior of the panel in our case [62].

Thus, the system failed in being effective in compression due to the low axial stiffness of the connectors and, to a minor extent, because they were dry inserted within the masonry and simply folded over the glass fabric (as suggested by the manufacturer). To the authors' knowledge, this was never underlined from past studies. In this way, it is recommended to use connectors having a higher axial stiffness and fix them within the masonry (through chemical anchors or inorganic matrices), avoiding the close alignment of the bars along the same vertical. Moreover, helical steel bars may cause localized damages and premature failure of the reinforcing fabric. Therefore, there is a need to develop and test more efficient and performing connection systems (e.g. with a fan shape such as those for FRP systems), to allow stress path continuity between substrate and fabric reinforcement. Further studies should be focused on testing different types of connectors, as well as, their layout.

Experimental results confirmed, instead, the great capacity of TRM systems to improve the in-plane shear resistance of masonry panels, especially in the case of tuff panels. The presence of TRM increased the ultimate shear strength capacity of about 2.7 times in the case of tuff masonry panels, and 1.3 times in fired clay brick masonry panels. Deformation capacity in the post-cracking phase was also particularly improved, especially in the case of tuff panels. Furthermore, reinforced panels have never shown brittle failure modes, as in the case of the unreinforced ones, since the TRM always provided a sort of effective confining. The presence of steel helical anchors did not generate a significant benefit to the increase of the panel's mechanical performances, also due to the application method (dry insertion) which could have weakened the wall section along the compressed diagonal. However, mechanical anchors avoided premature detachment of the TRM at the matrix-substrate interface, preventing the masonry from crumbling after cracking.

TRM also improved the bending resistance and the horizontal displacement capacity of both tuff and fired clay brick walls subjected to three-point bending tests. The flexural strength of TRM reinforced panels was about 28 and 3.7 greater than that of unreinforced tuff and fired clay brick masonry walls, respectively. The observed failure mode, regardless of the type of support, always involved the TRM layer subject to tensile stresses, which failed due to the breakage of the internal glass fabric. The ultimate average bending resistance of reinforced walls was almost the same, regardless of the type of masonry, thus indicating that flexural failure is completely controlled by the TRM layer applied on the tensed wall side.

Finally, the analytical investigation showed that current recommendations provided by ACI549 and CNR-DT215, calibrated on FRCM systems with lower thickness, may provide too conservative results when thicker TRM systems are used.

This is particularly true for the in-plane shear strength of reinforced panels, where more accurate predictions can be obtained by introducing additional parameters in the formulations, such as the TRM thickness and the tensile properties of the inorganic matrix. The analytical approach derived from ASTM E519, which only considers the mechanical contribution of the TRM matrix, showed good predictive capabilities. In particular, when the TRM matrix tensile strength was determined according to Model Code 2010, the analytical results were the most accurate. However, some slight overestimations could be attributable to the use of the mortar tensile strength, instead of its shear strength, which is typically lower. Such an approach could be the starting point for the next studies, to further deepen (even with numerical simulations) the contribution of the inorganic matrix.

Regarding the out-of-plane bending strength, the mortar thickness plays a minor role, since failure is always controlled by fabric breakage in the tensed face of the wall. Thus, the formulations from literature for FRCM systems provide a quite good estimate of the out-of-plane ultimate bending moments of the adopted TRM system. In particular, the best results are obtained by the CNR-DT215 formulation, slightly modified to consider the TRM contribution in compression (generally neglected in FRCM cases).

All these analytical approaches should be however refined, gathering a large number of experimental results, to check their predictive accuracy. Finally, further tests will have to consider the possibility of applying the TRM on different masonry substrates, also considering the possible influence of loading-unloading cycles (as well as a dynamic action) on the test results.

ACKNOWLEDGMENT

This paper is based on part of the results of the research project “*Convezione finalizzata allo studio ed alla caratterizzazione meccanica di sistemi FRCM applicati su murature in mattoni pieni e tufo*” (prot.n. 0000389 19/06/2018, UOR:SI000119 – Classif:III/19) financed by Biemme s.r.l., Lucrezia di Cartoceto, Italy. The authors gratefully acknowledge the useful help provided by the technicians Andrea Conti and Franco Rinaldi.

REFERENCES

- [1] A. Nanni, A New Tool for Concrete and Masonry Repair, Concrete International. (2012).
- [2] S. Babaeidarabad, D. Arboleda, G. Loreto, A. Nanni, Shear strengthening of un-reinforced concrete masonry walls with fabric-reinforced-cementitious-matrix, Construction and Building Materials. 65 (2014) 243–253. doi:10.1016/j.conbuildmat.2014.04.116.
- [3] S. Casacci, C. Gentilini, A. Di Tommaso, D. V. Oliveira, Shear strengthening of masonry wallettes resorting to structural repointing and FRCM composites, Construction and Building Materials. (2019). doi:10.1016/j.conbuildmat.2019.02.044.
- [4] G. de Felice, S. De Santis, L. Garmendia, B. Ghiassi, P. Larrinaga, P.B. Lourenço, D. V. Oliveira, F. Paolacci, C.G. Papanicolaou, Mortar-based systems for externally bonded strengthening of masonry, Materials and Structures. 47 (2014) 2021–2037. doi:10.1617/s11527-014-0360-1.
- [5] Y. Yardim, O. Lalaj, Shear strengthening of unreinforced masonry wall with different fiber reinforced mortar jacketing, Construction and Building Materials. 102 (2016) 149–154. doi:10.1016/j.conbuildmat.2015.10.095.
- [6] T. D’Antino, F.G. Carozzi, C. Poggi, Diagonal shear behavior of historic walls strengthened with composite reinforced mortar (CRM), Materials and Structures. 52 (2019) 114. doi:10.1617/s11527-019-1414-1.
- [7] A. Bilotta, F. Ceroni, E. Nigro, M. Pecce, Experimental tests on FRCM strengthening systems for tuff masonry elements, Construction and Building Materials. 138 (2017) 114–133. doi:10.1016/j.conbuildmat.2017.01.124.
- [8] V. Corinaldesi, J. Donnini, G. Mazzoni, Experimental study of adhesion between FRCM and masonry support, 2015. doi:10.4028/www.scientific.net/KEM.624.189.
- [9] S.M. Raoof, D.A. Bournas, TRM versus FRP in flexural strengthening of RC beams : Behaviour at high temperatures, Construction and Building Materials. 154 (2017) 424–437. doi:10.1016/j.conbuildmat.2017.07.195.
- [10] T. Trapko, The effect of high temperature on the performance of CFRP and FRCM confined concrete elements, Composites Part B: Engineering. (2013). doi:10.1016/j.compositesb.2013.05.016.
- [11] J. Donnini, F. De Caso y Basalo, V. Corinaldesi, G. Lancioni, A. Nanni, Fabric-reinforced cementitious matrix behavior at high-temperature: Experimental and numerical results, Composites Part B: Engineering. 108 (2017) 108–121. doi:10.1016/j.compositesb.2016.10.004.
- [12] J. Michels, D. Zwicky, J. Scherer, Y.E. Harmanci, M. Motavalli, Structural Strengthening of Concrete with Fiber Reinforced Cementitious Matrix (FRCM) at Ambient and Elevated Temperature — Recent Investigations in Switzerland, Advances in Structural Engineering. (2014). doi:10.1260/1369-4332.17.12.1785.
- [13] F.D.A. Silva, M. Butler, S. Hempel, R.D. Toledo Filho, V. Mechtcherine, Effects of elevated temperatures on the interface properties of carbon textile-reinforced concrete, Cement and Concrete Composites. (2014). doi:10.1016/j.cemconcomp.2014.01.007.
- [14] E. Franzoni, C. Gentilini, M. Santandrea, C. Carloni, Effects of rising damp and salt crystallization cycles in FRCM-masonry interfacial debonding: Towards an accelerated laboratory test method, (2018). doi:10.1016/j.conbuildmat.2018.04.164.
- [15] J. Donnini, Durability of glass FRCM systems: Effects of different environments on mechanical properties, Composites Part B: Engineering. (2019) 107047. doi:10.1016/j.compositesb.2019.107047.
- [16] A. Cascardi, R. Dell’Anna, F. Micelli, F. Lionetto, M.A. Aiello, A. Maffezzoli, Reversible techniques for FRP-confinement of masonry columns, Construction and Building Materials. (2019).

doi:10.1016/j.conbuildmat.2019.07.124.

- [17] T.C. Triantafillou, C.G. Papanicolaou, Shear strengthening of reinforced concrete members with textile reinforced mortar (TRM) jackets, *Materials and Structures/Materiaux et Constructions*. (2006). doi:10.1617/s11527-005-9034-3.
- [18] C.G. Papanicolaou, T.C. Triantafillou, M. Papathanasiou, K. Karlos, Textile reinforced mortar (TRM) versus FRP as strengthening material of URM walls: out-of-plane cyclic loading, *Materials and Structures*. 41 (2007) 143–157. doi:10.1617/s11527-007-9226-0.
- [19] ACI 549.4R, Guide to Design and Construction of Externally Bonded Fabric- Reinforced Cementitious Matrix (FRCM) Systems for Repair and Strengthening Concrete and Masonry Structures, 2013.
- [20] CNR-DT 215(2018), Guide for the Design and Construction of Fiber Reinforced Inorganic Matrix Systems for Strengthening Existing Structures, Italian Council of Research (CNR). (2018).
- [21] M. Del Zoppo, M. Di Ludovico, A. Balsamo, A. Prota, In-plane shear capacity of tuff masonry walls with traditional and innovative Composite Reinforced Mortars (CRM), *Construction and Building Materials*. 210 (2019) 289–300. doi:10.1016/j.conbuildmat.2019.03.133.
- [22] N. Gattesco, I. Boem, Experimental and analytical study to evaluate the effectiveness of an in-plane reinforcement for masonry walls using GFRP meshes, *Construction and Building Materials*. 88 (2015) 94–104. doi:10.1016/j.conbuildmat.2015.04.014.
- [23] S.T.C. Consiglio Superiore dei Lavori Pubblici, Linea Guida per la identificazione, la qualificazione ed il controllo di fibrorinforzati a matrice polimerica da utilizzarsi per il consolidamento strutturale di costruzioni esistenti con la tecnica dell'intonaco armato CRM, (2019) 1–38.
- [24] F. Parisi, I. Iovinella, A. Balsamo, N. Augenti, A. Prota, In-plane behaviour of tuff masonry strengthened with inorganic matrix-grid composites, *Composites Part B: Engineering*. (2013). doi:10.1016/j.compositesb.2012.09.068.
- [25] C. Faella, E. Martinelli, E. Nigro, S. Paciello, Shear capacity of masonry walls externally strengthened by a cement-based composite material: An experimental campaign, *Construction and Building Materials*. 24 (2009) 84–93. doi:10.1016/j.conbuildmat.2009.08.019.
- [26] G. Marcari, M. Basili, F. Vestroni, Experimental investigation of tuff masonry panels reinforced with surface bonded basalt textile-reinforced mortar, *Composites Part B: Engineering*. 108 (2017) 131–142. doi:10.1016/j.compositesb.2016.09.094.
- [27] M. Shabdin, M. Zargaran, N.K.A.K.A. Attari, Experimental diagonal tension (shear) test of Un-Reinforced Masonry (URM) walls strengthened with textile reinforced mortar (TRM), *Construction and Building Materials*. 164 (2018) 704–715. doi:10.1016/j.conbuildmat.2017.12.234.
- [28] X. Wang, C.C. Lam, V.P. Iu, C. Chiu Lam, V. Pan Iu, Experimental investigation of in-plane shear behaviour of grey clay brick masonry panels strengthened with SRG, *Engineering Structures*. 162 (2018) 84–96. doi:10.1016/j.engstruct.2018.02.027.
- [29] N. Gattesco, I. Boem, A. Dudine, Diagonal compression tests on masonry walls strengthened with a GFRP mesh reinforced mortar coating, *Bulletin of Earthquake Engineering*. 13 (2015) 1703–1726. doi:10.1007/s10518-014-9684-z.
- [30] T. D'Antino, C. Papanicolaou, Mechanical characterization of textile reinforced inorganic-matrix composites, *Composites Part B: Engineering*. 127 (2017) 78–91. doi:10.1016/j.compositesb.2017.02.034.
- [31] C. Papanicolaou, T. Triantafillou, K. Karlos, M. Papathanasiou, Textile-reinforced mortar (TRM) versus FRP as strengthening material of URM walls: in-plane cyclic loading, *Materials and Structures*. (2006).
- [32] N. Gattesco, I. Boem, Out-of-plane behavior of reinforced masonry walls: Experimental and numerical study, *Composites Part B: Engineering*. 128 (2017) 39–52. doi:10.1016/j.compositesb.2017.07.006.
- [33] S. Babaeidarabad, F. De Caso, A. Nanni, Out-of-Plane Behavior of URM Walls Strengthened with Fabric-Reinforced Cementitious Matrix Composite, *Journal of Composites for Construction*. 18 (2014) 04013057. doi:10.1061/(ASCE)CC.1943-5614.0000457.
- [34] A. Bellini, A. Incerti, M. Bovo, C. Mazzotti, Effectiveness of FRCM Reinforcement Applied to Masonry Walls Subject to Axial Force and Out-Of-Plane Loads Evaluated by Experimental and Numerical Studies, *International Journal of Architectural Heritage*. 12 (2018) 376–394. doi:10.1080/15583058.2017.1323246.
- [35] C. D'Ambra, G.P. Lignola, A. Prota, E. Sacco, F. Fabbrocino, Experimental performance of FRCM retrofit on out-of-plane behaviour of clay brick walls, *Composites Part B: Engineering*. 148 (2018) 198–206. doi:10.1016/j.compositesb.2018.04.062.
- [36] A.S. Mosallam, Out-of-plane flexural behavior of unreinforced red brick walls strengthened with FRP composites, *Composites Part B: Engineering*. 38 (2007) 559–574. doi:10.1016/j.compositesb.2006.07.019.
- [37] L.A.S. Kouris, T.C. Triantafillou, State-of-the-art on strengthening of masonry structures with textile reinforced mortar (TRM), *Construction and Building Materials*. 188 (2018) 1221–1233. doi:10.1016/j.conbuildmat.2018.08.039.
- [38] X. Wang, C.C. Lam, V.P. Iu, Comparison of different types of TRM composites for strengthening masonry panels, *Construction and Building Materials*. (2019). doi:10.1016/j.conbuildmat.2019.05.179.
- [39] N. Ismail, J.M. Ingham, In-plane and out-of-plane testing of unreinforced masonry walls strengthened using polymer textile reinforced mortar, *Engineering Structures*. 118 (2016) 167–177. doi:10.1016/j.engstruct.2016.03.041.
- [40] C. Papanicolaou, T. Triantafillou, M. Lekka, Externally bonded grids as strengthening and seismic retrofitting materials of masonry panels, *Construction and Building Materials*. 25 (2011) 504–514.

doi:10.1016/j.conbuildmat.2010.07.018.

- [41] ICC-ES, AC434.13 - Acceptance criteria for masonry and concrete strengthening using fabric reinforced cementitious matrix (FRCM) composite systems, (2013).
- [42] Consiglio Superiore dei Lavori Pubblici, Linea Guida per la identificazione, la qualificazione ed il controllo di accettazione di compositi fibrorinforzati a matrice polimerica (FRP) da utilizzarsi per il consolidamento strutturale di costruzioni esistenti, (2018) 1–17. doi:10.1109/ChiCC.2015.7260208.
- [43] RILEM TC 250-CSM, Composites for sustainable strengthening of masonry, (2012).
- [44] M. Del Zoppo, M. Di Ludovico, A. Prota, Analysis of FRCM and CRM parameters for the in-plane shear strengthening of different URM types, *Composites Part B: Engineering*. 171 (2019) 20–33. doi:10.1016/j.compositesb.2019.04.020.
- [45] T. D’Antino, F.G. Carozzi, P. Colombi, C. Poggi, Out-of-plane maximum resisting bending moment of masonry walls strengthened with FRCM composites, *Composite Structures*. 202 (2018) 881–896. doi:10.1016/j.compstruct.2018.04.054.
- [46] E. Bernat-Maso, C. Escrig, C.A. Aranha, L. Gil, Experimental assessment of Textile Reinforced Sprayed Mortar strengthening system for brickwork wall, (2014). doi:10.1016/j.conbuildmat.2013.09.031.
- [47] M.R. Valluzzi, F. da Porto, E. Garbin, M. Panizza, Out-of-plane behaviour of infill masonry panels strengthened with composite materials, *Materials and Structures*. 47 (2014) 2131–2145. doi:10.1617/s11527-014-0384-6.
- [48] L. Ascione, G. De Felice, S. De Santis, A qualification method for externally bonded Fibre Reinforced Cementitious Matrix (FRCM) strengthening systems, *Composites Part B: Engineering*. (2015). doi:10.1016/j.compositesb.2015.03.079.
- [49] A. Prota, G. Marcari, G. Fabbrocino, G. Manfredi, C. Aldea, Experimental In-Plane Behavior of Tuff Masonry Strengthened with Cementitious Matrix–Grid Composites, *Journal of Composites for Construction*. 10 (2006) 223–233. doi:10.1061/(ASCE)1090-0268(2006)10:3(223).
- [50] A. Penna, P. Morandi, M. Rota, C.F. Manzini, F. da Porto, G. Magenes, Performance of masonry buildings during the Emilia 2012 earthquake, *Bulletin of Earthquake Engineering*. 12 (2014) 2255–2273. doi:10.1007/s10518-013-9496-6.
- [51] EN 1926:2007, Natural stone test methods - determination of uniaxial compressive strength, (2007).
- [52] EN 772-1, Methods of Test for Masonry Units Part 1: Determination of Compressive Strength, 2015. doi:10.1016/j.jradnu.2011.06.002.
- [53] EN 1015-11, Methods of Test for Mortar for Masonry: Part 11: Determination of Flexural and Compressive Strength of Hardened Mortar, (2006).
- [54] CSN EN 12504-4: 2005, EN 12504-4 Testing concrete Part 4: Determination of ultrasonic pulse velocity, 2005.
- [55] BS EN 10002-1:2001 / ISO 6892-1, Metallic materials - Tensile testing - Part 1: Method of test at room temperature., Deutsche Norm. (2009).
- [56] J. Donnini, V. Corinaldesi, Mechanical characterization of different FRCM systems for structural reinforcement, *Construction and Building Materials*. 145 (2017) 565–575. doi:10.1016/j.conbuildmat.2017.04.051.
- [57] J. Donnini, G. Chiappini, G. Lancioni, V. Corinaldesi, Tensile behaviour of glass FRCM systems with fabrics’ overlap: Experimental results and numerical modeling, *Composite Structures*. 212 (2019) 398–411. doi:10.1016/j.compstruct.2019.01.053.
- [58] S. Mazzuca, H.A. Hadad, L. Ombres, A. Nanni, Mechanical Characterization of Steel-Reinforced Grout for Strengthening of Existing Masonry and Concrete Structures, *Journal of Materials in Civil Engineering*. (2019). doi:10.1061/(ASCE)MT.1943-5533.0002669.
- [59] D. Campanini, H.A. Hadad, C. Carloni, C. Mazzotti, A. Nanni, Mechanical characterization of SRG composites according to AC434, in: *Key Engineering Materials*, 2019. doi:10.4028/www.scientific.net/KEM.817.458.
- [60] U. Ebead, K.C. Shrestha, M.S. Afzal, A. El Refai, A. Nanni, Effectiveness of Fabric-Reinforced Cementitious Matrix in Strengthening Reinforced Concrete Beams, *Journal of Composites for Construction*. (2017). doi:10.1061/(ASCE)CC.1943-5614.0000741.
- [61] J. Donnini, V. Corinaldesi, A. Nanni, Mechanical properties of FRCM using carbon fabrics with different coating treatments, *Composites Part B: Engineering*. 88 (2016). doi:10.1016/j.compositesb.2015.11.012.
- [62] D. Arboleda, F.G. Carozzi, A. Nanni, C. Poggi, Testing procedures for the uniaxial tensile characterization of fabric-reinforced cementitious matrix composites, *Journal of Composites for Construction*. (2016). doi:10.1061/(ASCE)CC.1943-5614.0000626.
- [63] M. Butler, V. Mechtcherine, S. Hempel, Durability of textile reinforced concrete made with AR glass fibre: Effect of the matrix composition, *Materials and Structures/Materiaux et Constructions*. (2010). doi:10.1617/s11527-010-9586-8.
- [64] J. Hartig, U. Häußler-Combe, K. Schicktan, Influence of bond properties on the tensile behaviour of Textile Reinforced Concrete, *Cement and Concrete Composites*. (2008). doi:10.1016/j.cemconcomp.2008.08.004.
- [65] M. Leone, M.A. Aiello, A. Balsamo, F.G. Carozzi, F. Ceroni, M. Corradi, M. Gams, E. Garbin, N. Gattesco, P. Krajewski, C. Mazzotti, D. Oliveira, C. Papanicolaou, G. Ranocchiai, F. Roscini, D. Saenger, Glass fabric reinforced cementitious matrix: Tensile properties and bond performance on masonry substrate, *Composites Part B: Engineering*. 127 (2017) 196–214. doi:10.1016/j.compositesb.2017.06.028.
- [66] Circolare 21.01.2019 n. 7, Istruzioni per l’applicazione dell’aggiornamento delle norme tecniche per le costruzioni di

cui al decreto ministeriale 17 gennaio 2018, (2019).

[67] EN 1052-1:2001, Methods of test form masonry - Part 1: Determination of compressive strength, (2001).

[68] Ministero delle Infrastrutture e dei Trasporti, NTC 2018 - Aggiornamento delle «Norme tecniche per le costruzioni», (2018).

[69] C. Calderini, S. Cattari, S. Lagomarsino, The use of the diagonal compression test to identify the shear mechanical parameters of masonry, *Construction and Building Materials*. 24 (2010) 677–685. doi:10.1016/j.conbuildmat.2009.11.001.

[70] F. Stazi, M. Serpilli, G. Maracchini, A. Pavone, An experimental and numerical study on CLT panels used as infill shear walls for RC buildings retrofit, *Construction and Building Materials*. 211 (2019) 605–616. doi:10.1016/j.conbuildmat.2019.03.196.

[71] A. Borri, G. Castori, M. Corradi, Determination of Shear Strength of Masonry Panels Through Different Tests, *International Journal of Architectural Heritage*. (2015). doi:10.1080/15583058.2013.804607.

[72] A. Brignola, S. Frumento, S. Lagomarsino, S. Podestà, S. Podestà, Identification of shear parameters of masonry panels through the in-situ diagonal compression test, *International Journal of Architectural Heritage*. 3 (2009) 52–73. doi:10.1080/15583050802138634.

[73] M. Kržan, S. Gostič, S. Cattari, V. Bosiljkov, Acquiring reference parameters of masonry for the structural performance analysis of historical buildings, *Bulletin of Earthquake Engineering*. 13 (2015) 203–236. doi:10.1007/s10518-014-9686-x.

[74] E. Quagliarini, G. Maracchini, F. Clementi, Uses and limits of the Equivalent Frame Model on existing unreinforced masonry buildings for assessing their seismic risk: A review, *Journal of Building Engineering*. 10 (2017) 166–182. doi:10.1016/j.jobbe.2017.03.004.

[75] G. Maracchini, E. Quagliarini, F. Clementi, F. Monni, Historic masonry buildings and global modelling through the EFM: comparison between numerical simulation and real behaviour through a case study (in Italian), in: G. Bernardini, E. Di Giuseppe (Eds.), *Colloqui.AT.e 2017. DEMOLITION OR RECONSTRUCTION?*, Edicom edizioni, Monfalcone, Italy, 2017.

[76] ASTM, ASTM E519/E519M 2010. Standard Test Method for Diagonal Tension (Shear) in Masonry Assemblages, 2007. doi:10.1520/E0519.

[77] E. Quagliarini, S. Lenci, M. Iorio, Mechanical properties of adobe walls in a Roman Republican domus at Suasa, *Journal of Cultural Heritage*. 11 (2010) 130–137. doi:10.1016/j.culher.2009.01.006.

[78] E. Quagliarini, S. Lenci, Q. Piattoni, F. Bondioli, I. Bernabei, G. Lepore, M. Zaccaria, Experimental Analysis of Romanesque Masonries Made by Tile and Brick Fragments Found at the Archaeological Site of S. Maria in Portuno, *International Journal of Architectural Heritage*. 8 (2014) 161–184. doi:10.1080/15583058.2012.683132.

[79] E. Quagliarini, G. Maracchini, Experimental and FEM Investigation of Cob Walls under Compression, *Advances in Civil Engineering*. 2018 (2018) 1–13. doi:10.1155/2018/7027432.

[80] J.A.P.P. Almeida, E.B. Pereira, J.A.O. Barros, Assessment of overlay masonry strengthening system under in-plane monotonic and cyclic loading using the diagonal tensile test, *Construction and Building Materials*. 94 (2015) 851–865. doi:10.1016/j.conbuildmat.2015.07.040.

[81] C. Shermi, R.N. Dubey, In-plane behaviour of unreinforced masonry panel strengthened with welded wire mesh and mortar, *Construction and Building Materials*. 178 (2018) 195–203. doi:10.1016/j.conbuildmat.2018.04.081.

[82] S. Babaeidarabad, F. De Caso, A. Nanni, URM Walls Strengthened with Fabric-Reinforced Cementitious Matrix Composite Subjected to Diagonal Compression, *Journal of Composites for Construction*. 18 (2014) 04013045. doi:10.1061/(ASCE)CC.1943-5614.0000441.

[83] T. Li, N. Galati, J.G. Tumialan, A. Nanni, Analysis of unreinforced masonry concrete walls strengthened with glass fiber-reinforced polymer bars, *ACI Structural Journal*. (2005).

[84] M.H. Mann W, Failure of shear-stressed masonry—an enlarged theory, tests and application to shear walls., *Proc., British Ceramic Soc.* (1982) 223–235.

[85] American Society for Testing Materials, ASTM E519/E519M 2015. Standard Test Method for Diagonal Tension (Shear) in Masonry Assemblages, ASTM International, West Conshohocken, PA, 2015. doi:10.1520/E0519_E0519M-15.

[86] Rilem TC 76-LUM, Diagonal tensile strength tests of small wall specimens, E&EN SPON, London, UK, 1994.

[87] The International Federation for Structural Concrete (fib), Model Code 2010, Fib Model Code for Concrete Structures 2010. (2011). doi:10.1002/9783433604090.ch6.

[88] A. Bellini, M. Bovo, A. Incerti, C. Mazzotti, An alternative approach for frcm matrix tensile strength evaluation, *Key Engineering Materials*. 817 KEM (2019) 365–370. doi:10.4028/www.scientific.net/KEM.817.365.

[89] FIB, Model Code 2010, 2011. doi:10.1002/9783433604090.ch6.

[90] J. Donnini, G. Lancioni, V. Corinaldesi, Failure modes in FRCM systems with dry and pre-impregnated carbon yarns: Experiments and modeling, *Composites Part B: Engineering*. 140 (2018) 57–67. https://www.sciencedirect.com/science/article/pii/S1359836817325222 (accessed November 11, 2018).

[91] C. Signorini, A. Nobili, A. Sola, M. Messori, Designing epoxy viscosity for optimal mechanical performance of coated Glass Textile Reinforced Mortar (GTRM) composites, *Construction and Building Materials*. (2020). doi:10.1016/j.conbuildmat.2019.117325.

[92] M. Messori, A. Nobili, C. Signorini, A. Sola, Mechanical performance of epoxy coated AR-glass fabric Textile

1131 Reinforced Mortar: Influence of coating thickness and formulation, Composites Part B: Engineering. (2018).
1132 doi:10.1016/j.compositesb.2018.05.023.
1133