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FABRIC-REINFORCED CEMENTITIOUS MATRIX BEHAVIOR AT HIGH-TEMPERATURE: EXPERIMENTAL AND NUMERICAL RESULTS Jacopo Donnini¹, Francisco De Caso y Basalo², Valeria Corinaldesi³, Giovanni Lancioni⁴, Antonio Nanni⁵

⁶ ¹ Università Politecnica delle Marche, Engineering Faculty, Ancona, Italy,

7 <u>j.donnini@univpm.it</u>

- 8 ² University of Miami, Dept. of Civil, Arch. & Environ. Engineering, Miami, U.S.A.,
- 9 <u>f.decasoybasalo@umiami.edu</u>
- ³ Università Politecnica delle Marche, Engineering Faculty, Ancona, Italy,
- 11 <u>v.corinaldesi@univpm.it</u>
- ⁴ Università Politecnica delle Marche, Engineering Faculty, Ancona, Italy,
- 13 <u>g.lancioni@univpm.it</u>
- ⁵ University of Miami, Dept. of Civil, Arch. & Environ. Engineering, Miami, U.S.A.,
- 15 <u>nanni@umiami.edu</u>
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18 ABSTRACT

The use of externally applied composite systems to upgrade, strengthen or rehabilitate masonry or concrete structures is well established. However, structural strengthening with organic type composites, such as fiber-reinforced polymer (FRP) systems, may be impractical when the element is exposed to high-temperature service conditions, due to significant degradation of the organic resin. Instead, the use of an inorganic matrix, as in the case of fabric-reinforced cementitious matrix (FRCM) composites, may overcome this problem.

25 The purpose of this study is to evaluate the mechanical behavior under high-temperature conditions of FRCM systems. Different FRCM composites are evaluated and include carbon 26 27 fabrics ranging from dry to highly-impregnated with an organic resin. The experimental spectrum is comprised of uniaxial tensile and double-shear bond tests performed under 28 29 temperatures ranging from 20 to 120°C to determine the influence of temperature over the 30 FRCM mechanical properties. Furthermore, SEM analysis was used to study the damage 31 processes at the fiber-matrix interface post tensile testing. Experimental results show variations in the FRCM mechanical properties if tested at high temperature conditions (caused by the 32 33 deterioration of the resin coating at the interface fiber-matrix) while residual performance after exposure to elevated temperatures remains unchanged. FRCM reinforced with dry fabrics has 34 35 proven not to be affected by temperatures up to 120°C.

A numerical model using a fracture variational approach, based on incremental energy
 minimization, was also developed to simulate the FRCM behavior in double shear tests under
 different temperatures exposition.

3940 Keywords:

41 FRCM, Temperature, Organic Coating, Strengthening, Damage Mechanics, Variational
42 Modelling.

- 43
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- 46

1 Nomenclature

	Geometrical parameters		Materials parameters
A	sectional area of the yarn	G	fracture toughness
b_{I}	thickness of the 1 st layer of mortar	В	size of the process zone
b_2	thickness of the yarn in numerical simulation	q	exponent of the interface damage energy
b_3	width of the FRCM in numerical simulations	E_m	elastic modulus of the mortar
h	FRCM anchorage length in double shear tests	E_2	FRCM elastic modulus in the cracked phase
l_1	mean length of the internal isostatic lines	E_f	tensile elastic modulus of the yarn
l_2	mean length of the external isostatic lines	μ, λ	Lamé's coefficients
l_m	internal length of the damage surface	k	elastic coefficient of interface springs
β	geometrical ratio		
Ω_{f}	yarn domain		
Ω_m	matrix domain		Forces and stresses
$ \Lambda $	interface domain	f_{max}	peak load in double shear tests
		σ	tensile normal stress in the yarn
	Variables	$ au_e$	maximum shear stress at the interface
α	damage parameter	$ au_r$	residual shear stress at the interface
u	displacement field	σ_{e}	tensile strength of the mortar
		σ_u	FRCM ultimate tensile strength
	Displacements and strains	$ au_{I}$	shear stresses along the internal isostatic lines
s	fabric total displacement	$ au_2$	shear stresses along the ext. isostatic lines
δ	yarn displacement in numerical simulations	f	longitudinal forcein the yarn
ε_2	FRCM ultimate tensile strain		

2 3

4 **1. Introduction**

5 The use of fiber-reinforced polymer (FRP) systems to reinforce masonry or concrete structures 6 may be impractical when the element is exposed to high-temperature service conditions. FRP 7 mechanical properties can be drastically reduced if the temperature exceeds the glass transition 8 temperature (T_{v}) of the organic resin used for fiber impregnation and bond to the substrate. 9 Experimental studies showed a severe reduction of FRP tensile strength, stiffness and bond 10 properties to the substrate when exposed to elevated temperatures due to a rapid deterioration of 11 the FRP-substrate adhesion when the temperature exceeds the Tg of the resin (typically around 12 60 to 80 °C), resulting in delamination of the composite and loss of efficacy of the reinforcement 13 [1-5]. 14 Studies conducted by Bisby and co-workers [6] on the mechanical characterization of FRP

15 materials at high temperature showed that half of the tensile strength of the FRP was lost when

16 tested near the T_g of the epoxy matrix. Lap-splice tests showed that the FRP-to-FRP bond 17 strength was affected over more by temperature exposure near T with 00% less in lap arrive

- strength was affected even more by temperature exposure near T_g with 90% loss in lap-splice strength.
- Al-Salloum and co-workers [7] suggested to not exceed the FRP T_g in order to avoid serious

20 consequences. In case temperature is allowed to reach up to 200 °C (thus greatly higher than T_g),

21 the ultimate capacity of FRP-strengthened members should be kept at less than 25% of its

- 22 corresponding value at room temperature.23 For this reason, the use FRP composites in construction, where high temperature exposure is
- critical, may be limited and additional information is needed when selecting strengthening
- 25 solutions. For example, the use of insulation to maintain the temperature on the FRP surface

1 below T_g is possible, although not always practicable [8,9]. Hence, alternative solutions need to 2 be explored and evaluated.

3 FRCM systems use inorganic matrices, which are less susceptible to high temperature and may 4 result cost effective compared to FRPs [10-15]. However, the bond between fibers and inorganic 5 matrix is a critical issue in FRCM composites and is strongly influenced by the ability of the 6 cementitious matrix to saturate dry fiber yarns; also affected is the bond between internal and 7 external fibers within the yarns and between external fibers and matrix in case of dry fabrics and 8 possibly between coating and matrix in case of coated fabrics. FRCM for structural 9 strengthening applications is a relatively new material and durability is an important aspect to be 10 considered. Factors affecting the durability of FRCM composite systems must consider the environmental performance of each of its components and their interfaces: the cementitious 11 matrix, the fabric reinforcement, the fabric-matrix interface and the matrix-substrate interface are 12 13 the different elements that need to be considered in relation to the service environments in which 14 they are expected to perform [16]. FRCM is expected to overcome some of the issues that are 15 typically found in FRP because of the better performance of the cementitious matrix to high 16 temperatures. However, limited experimental and analytical studies have been conducted to 17 evaluate the behavior of FRCM under high temperatures and the residual performances after

18 exposure to elevated temperatures.

19 An experimental study on the effect of high temperature on the performance of carbon fiber-20 reinforced polymer (CFRP) and FRCM confined concrete element was conducted by Trapko [17], using concrete cylinders reinforced with CFRP sheets and FRCM mesh and exposed to 21 22 temperature ranging from 40 to 80 °C. In the case of polymer jackets, 40 °C increase in 23 temperature resulted in 20% decrease of the load-bearing capacity. The compressive strain of 24 specimens tested in 80 °C was approximately half of the strain in specimens tested at 40 and 60 25 °C. Load-bearing capacity decrease by 5-10% was observed for FRCM confined elements upon temperature increase from 40 to 80 °C. Also, compressive strain decrease by approximately 11% 26 27 was observed upon temperature increase from 40 to 60 and 80 °C.

FRCM performances at high temperature exposure may change when fabrics are preimpregnated with polymeric resins. Experimental studies showed that the use of a polymer coating applied on carbon fabric may significantly increase the mechanical capacity of FRCM systems for both tensile and shear bond strengths when applied to masonry or concrete supports [18-20]. However, when the textile reinforcement is coated with a polymer, the bond performance between fibers and matrix is strongly affected by temperature [21].

Recent studies by Silva and co-workers [22] using FRCM reinforced with carbon fibers showed a polymer interlocking mechanism between filaments and matrix when heating the polymer coated fibers up to 150 °C. This mechanism results in significant increases in the maximum pullout load. Krüger and Reinhardt [23] performed fire tests on four different I-shaped mortar beams reinforced with AR-glass and carbon textiles. The investigation was focused on the load bearing capacity of the composite during a fire test under constant load. In one of the cases a SBR (Butadien-Styrol) thermoplastic resin was used as coating in the fiber. The results showed

41 to be very dependent on the fire behavior of the used fibers. Due to the softening of the SBR

42 coating (around 90 °C) the fiber-matrix interface rapidly deteriorated, resulting in fiber pullout
 43 and, subsequent, failure.

44 Michels and Motovalli [24] presented experimental results of the tensile strength decrease of

45 coated carbon fiber yarns after high temperature exposure up to 1000°C. The investigation was

46 performed at room temperature on carbon fiber yarns after having been thermally subjected to

1 constant temperature of 300, 500, 700 and 1000 °C in a tube furnace for 30 minutes. It was 2 observed that an exposure at 300°C for 30 minutes does not affect the mechanical properties of 3 the analyzed reinforcement. However, a further increase in temperature results in significant 4 damage to the material performance at 500 °C and no residual strength at 700 °C. Tests on a 5 reinforced concrete slab strip strengthened with a shotcrete layer including a composite mesh as 6 tensile reinforcement was also investigated. Under a constant service load, the slab was exposed 7 to fire with a temperature rise according to a European standard curve (ETK) for two hours. The 8 slab could withstand the applied load under fire exposure, during which the composite mesh 9 reached a temperature of about 440 °C.

10

11 This study aims at understanding FRCM behavior under high-temperature conditions as provided 12 by an environmental chamber. Specimen reinforcement included fabrics made of carbon fibers

13 ranging from dry to highly impregnated with an organic resin. A series of uniaxial tensile tests

14 and double shear bond tests were performed under temperatures up to 120 °C to understand the

15 influence of temperature over the mechanical properties of the FRCM and, in particular, the

variation of bond between fabric and matrix when organic resin pre-impregnation was applied tothe fabric.

18 To better understand the experimental results, a variational damage model has been numerically

19 implemented in a finite element code, and simulations have been performed, reproducing the

20 FRCM different behaviors observed in double shear bond tests at different temperatures and

21 different fibers impregnation. Modeling investigations have been conducted also in [25-27] to

have an insight into experimental results. However, FRCM behavior based on variation damage

23 models has never been reproduced.

24 The model proposed in this paper falls into the category of phase-field models, which, in the last 25 decade, have been object of many researches, and have been applied to several problems of material science. The phase-field approach was first proposed in [28] for regularizing the 26 27 Griffith's theory of brittle fracture, formulated as a minimum problem [29]. The sharp fracture of 28 Griffith's theory is approximated by a smeared fracture, described by layers of finite thickness 29 where the material damages and strains localize. An auxiliary scalar field α is introduced into the 30 material internal energy functional, which plays the rule of a damage field assuming values $\alpha=0$ for sound material and $\alpha=1$ for totally damaged material, and an intrinsic length scale parameter 31 32 controls the thickness of the damaged zone. When the length scale parameter goes to zero, the 33 thickness of the damaged layer shrinks to zero, and sharp Griffith's fracture is recovered. 34 Including both displacement and damage smooth fields, the model accounts for a straightforward 35 implementation by standard finite elements. Since the formulation [28], many improved and 36 enriched models have been proposed. Among the many, we mention [30], where the variational problem [28] has been reformulated in the more general framework of finite elasticity, [31], 37 38 where the bulk energy has been decomposed into non damageable spheric and damageable 39 deviatoric parts to reproduce shear fractures, and [32,33], where fracture under tensile and 40 compression stress states has been differentiated.

41 Here, a phase-field model is developed for the double shear test, which is schematized by a two-

42 dimensional problem. Different energies are assigned to the cementitious matrix, to the fibers

43 and to the matrix-fibers interface. While the fibers are supposed to be unbreakable, and thus they

44 are purely elastic, the matrix is breakable. Its internal energy accounts for an elastic and a

- 45 damage term. This latter is sum of a dissipative local contribution, a linear function of α , and a
- 46 non-local contribution depending on the gradient of α , as in [34]. Materials with this energy

exhibit a brittle-elastic mechanics. They deform elastically as long as a maximum stress value is
 attained, and, afterwards, they brutally damage and break.

3 A damageable elastic springs distribution is applied at the matrix-fibers interface. Its damage

4 energy is sum of a local and a non-local gradient term, and the local contribution is a non-linear 5 power function of α . To our knowledge, the expression of the local damage energy is a novelty,

5 power function of α . To our knowledge, the expression of the local damage energy is a novelty, 6 never proposed in literature, and it allows of describing initial purely elastic regimes followed by

- 7 stress-softening phases. Moreover, it allows of accounting for residual strength when large
- 8 relative sliding displacements are attained, thus interpreting the frictional behavior observed in
- 9 pullout tests. While the matrix and fibers material parameters are kept fixed, the parameters
- 10 characterizing the interface vary with respect to temperature and type of fibers (dry or pre-
- 11 impregnated with organic resin). Thus, in the model, thermal and fibers impregnation affect only 12 the interface parameters
- 12 the interface parameters.
- For a system characterized by these energies, the displacement and damage evolution, when certain loads are applied and increased, is determined by solving a constrained incremental
- 15 minimization problem. Incremental minimization is a powerful mathematical tool to capture
- 16 local minima in evolution problems, and it has been applied to problems of fracture [35],
- 17 plasticity [36,37], and crystal plasticity [38,39]. Here it is solved numerically by implementing a
- 18 Sequential Quadratic Programming algorithm.
- 19

The paper is organized as follows. In Section 2 the experimental investigation is explained. Section 3 describes the variational damage model used to reproduce the experimental results of double shear bond tests. In Section 4 the experimental results are reported and compared with numerical simulations in Section 5. Conclusions are drawn in Section 6.

24

25 **2. Experimental investigation**

- Tensile tests were performed using a MTS 651 environmental chamber at temperatures of 20, 80 and 120 °C. A clevis grip system was used to anchor the specimens by means of metal plates epoxied to the ends of the FRCM coupons, in order to allow the slippage of the fabric within the matrix and to better investigate the influence of a coating when the system is exposed to high temperatures [AC434 - Acceptance criteria for masonry and concrete strengthening using fabricreinforced cementitious matrix (FRCM) composite systems]. In this case, the load is transferred
- 32 only through the matrix to the fabric.
 - 33 Double shear bond tests were also performed to evaluate the bond capacity of FRCM applied on 34 clay brick substrate and the behavior at the fabric-mortar interface when exposed to the 35 aforementioned temperatures.
 - 36
 - 37 2.1 Materials

In order to study the effects of enhancing the bond at the fabric-matrix interface with an organic coating on the FRCM fabric and the mechanical behavior of the system when exposed to different temperatures two kinds of carbon fabric are used: dry and highly impregnated with epoxy resin plus sand (HS), (Figure 1).



Figure 1. Dry and coated (HS) carbon fabrics

The enhancement of the bond is made by coating the fabric with a flexible epoxy and by applying a layer of quartz sand on the surface. Table 1 summarizes some of the geometrical and mechanical properties of the carbon fabric as obtained from the manufacturer. Values regarding mechanical properties of the carbon yarns (used in numerical analysis) were evaluated experimentally by means of tensile tests, according to EN ISO 10618/2005. The characteristics of the epoxy used as coating are reported in Table 2.

Table 1. Carbon	Fabric	characteristics
-----------------	--------	-----------------

Dry carbon fabric, bidirection	Dry carbon fabric, bidirectional, balanced					
Resistant area (for each	$0.052 \text{ mm}^2/\text{mm}$					
direction) ¹						
Fabric weight ¹	180 g/m^2					
Fiber tensile strength ¹	4900 MPa					
Fiber elastic modulus ¹	240 GPa					
Fiber break elongation ¹	2 %					
Yarn tensile strength ²	1850 MPa					
Yarn elastic modulus, E_f^2	150 GPa					
Yarn Poisson ratio, v_f^1	0.3					
Table 2. Coating characteristics of Two component epoxy syst	otain from manufact em					
Tensile strength	0.8 MPa					
Density	1.12 g/m					
Max recommended operatir temperature	ισ					
	50 °C					
Strain at break	^{rg} 50 °C 70 %					
Strain at break Exothermic peak	⁴⁵ 50 °C 70 % 58-68 °C					
Strain at break Exothermic peak Resin viscosity at 25°C	⁴⁵ 50 °C 70 % 58-68 °C 6000 mPas					

The inorganic matrix used is a commercially available cementitious mortar, herein referred to as
 Mortar-45. The compressive and splitting tensile strengths were evaluated according to ASTM
 C109 and ASTM C496 [40,41] at 28 curing days, on 50 mm cubes and 50x100 mm cylinders,

4 respectively. The Elastic modulus was evaluated according to ASTM C580 [42] and the average

- 5 results of 5 repetitions are reported in Table 3.
- 6 7

Table 3. Mechanical properties of the mortar used as FRCM matrix

Mortar 45 - Fiber-reinforced, structural						
repair mortar						
Compressive strength	50 MPa					
Elastic modulus, E_m 34.5 GPa						
Splitting tensile strength, σ_e 6.2 MPa						
Unit weight 2275 kg/m ³						
Yarn Poisson ratio, v_m 0.2						

8

9 2.2 Temperature profile

A temperature profile along the length of the FRCM specimen was obtained, using type J thermocouples embedded in the mortar at four different locations (Figure 2). This allowed the possibility of measuring the temperature at the fabric-mortar interface when exposed to 80 and 120 °C, while also understanding the effect of the metal tabs used to grip on the specimen.

14 Results depicted in Figure 3 shows a smooth increase of temperature of the FRCM specimen

- 15 when no load is being applied, where after two hours the center of the specimen reached 88 and
- 16 90% of the peak applied temperature corresponding to the 80 and 120 °C profile, respectively.
- 17



Figure 2. Application of thermocouples at the fabric-matrix interface



Figure 3. Heat development at the fabric-mortar interface of a FRCM coupon when exposed to 80 and 120 °C

5 Observation: the curve ai2 fits almost perfectly with the curve ai3, for this reason is not easily observable in the graph.

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8 2.3 Tensile and double shear bond test at high-temperature exposure

9 FRCM behavior under high temperature exposure have been investigated by means of the two

10 main tests to characterize this material: tensile test as described by AC434 Annex A and double shear bond test (RILEM TC250-CSM).

11

Tensile coupons 410x60x10mm were prepared and left to cure for 28 days at 20 °C and 70% 12

relative humidity. Metal tabs were then adhered with epoxy to the ends of the specimens with an 13 14 anchor length of 150 mm, by using a special high-temperature epoxy able to withstand up to 150 15 °C.

16 A total of 18 uniaxial tensile tests were conducted using a screw-driven universal test frame with 17 a maximum capacity of 130 kN and a MTS 651 Environmental Chamber. A clevis-type grip system was used and axial deformation was measured using a clip-on extensometer with a 100 18 19 mm gauge length, attached to the metal tabs surface. The load was applied under displacement control at 0.3 mm/min. Tensile tests were initiated after conditioning the specimens in the 20 21 chamber for 100 minutes, so as to ensure the internal temperature at the center of the specimen

22 failure zone was approximately 90% of the oven temperature, either 80 or 120 °C.

23

24 A total of 12 double shear bond tests were performed on clay bricks substrates. Specimens were 25 fabricated with dry and coated carbon bidirectional fabrics, using the same type of cementitious 26 mortar (Mortar-45). The bond length was kept constant and equal to 100 mm. The fabric was 27 wrapped around a rigid steel cylinder and the specimens were tested at 20 °C and after being 28 exposed to 120 °C for 100 minutes by means of an environmental chamber. The load was 29 applied under displacement control at 0.5 mm/min. The double shear test set-up and the principal 30 failure mode by slippage of the fabric within the mortar are shown in Figure 4.

1 Furthermore, 3 double shear bond tests have been performed to evaluate the residual capacity of

2 FRCM after being exposed to high temperature. Specimens were placed in oven at 120 °C for 60

- minutes, cooled down and tested at 20 °C. A test matrix that summarizes all experimental tests is
 illustrated in Table 4.
- 5
- 6

Table 4. Test matrix								
Test	TestCarbon fabricTestsTemperature of testing (°C)		Temperature of testing (°C)	Description				
Tensile	DRY	3, 3, 3	20, 80, 120					
Test	HS	3, 3, 3	20, 80, 120	Specimens have been tested after being exposed				
Double	DRY	3, 3	20, 120	to the reference temperature for 100 minutes				
shear bond	HS	3, 3	20, 120					
test	HS	3	20	Specimens have been tested at room temperature after being exposed at 120 °C for 60 minutes				

7



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- 10

Figure 4. Double shear bond test set up and slippage failure mode

11 **3. Variational damage model for FRCM**

12 In this section, the variational damage model used to reproduce the experimental results of the 13 double shear bond test is presented. In Sect. 3.1, the geometry of the experiment is simplified 14 into a two-dimensional scheme, where the hypothesis of plane strain state is assumed. The 15 energy assigned to the sample is discussed in Sect. 3.2, and, in Sect. 3.3, the damage evolution 16 problem is formulated as an incremental energy minimization problem, and the numerical 17 solving strategy is briefly recall.

2 3.1 Geometrical scheme for double shear bond test

3 We consider a single yarn embedded in a cementitious matrix, as represented in Fig. 5a. The 4 system is fixed to a rigid support (clay brick substrate) on one lateral side, and the yarn is subject 5 to a constant displacement δ . It generates a tensile normal stress σ in the varn and a shear stress τ 6 at the interface with the matrix, which flows toward the clamped sample side. The isostatic lines 7 of shear stress in a transverse cross-section are schematically drawn in Fig. 5b. The amounts of 8 shear stress flowing from the internal and external varn faces (the half-surfaces opposite to the 9 clamped and traction-free matrix sides, respectively) are determined as follows. Let us consider the green and blue isostatic lines of Fig. 5b, which flow from points placed on the internal and 10 11 external yarn face, respectively, and whose lengths l_1 and l_2 are approximately mean lengths among all the lines flowing from the internal and external faces. For a sample portion of 12 13 infinitesimal thickness dy, we estimate the vertical shear stresses flowing through the surfaces projection of the green and blue isostatic lines (green and blue surfaces in Fig. 5c). A schematic 14 15 plane representation of these surfaces is drawn in Fig. 5d. Given a longitudinal force f (per unit thickness) in the yarn, the shear stresses τ_1 and τ_2 flowing through the green and blue regions of 16 17 length l_1 and l_2 , are

18
$$\tau_1 = \frac{\beta}{1+\beta} \frac{df}{dy}, \quad \tau_2 = \frac{1}{1+\beta} \frac{df}{dy}, \quad \text{with } \beta = \frac{l_2}{l_1}.$$
 (1)

19 They are determined by solving the equilibrium equation $\tau_1 + \tau_2 = \frac{df}{dy}$, and the kinematical

20 compatibility condition $\frac{\tau_1}{G} l_1 = \frac{\tau_2}{G} l_2$, with $G = \frac{E}{2(1+\nu)}$ the shear modulus. Since l_1 and l_2 are

mean lengths, the ratios $\beta/(1+\beta)$ and $1/(1+\beta)$ in (1) give estimates of the tensile force rates 21 22 transmitted to the clamped surface through the internal and external faces of the yarn, respectively. Thus, supposing a constant distribution of σ in the varn cross-section, stresses in 23 24 the red yarn portion of area $\beta A/(1+\beta)$ are transmitted through the internal part of the matrix, 25 while the stresses in the rose portion of area $A/(1+\beta)$ are transmitted through the external and lateral parts of the matrix. In D'Antino and Carloni [43,44], the authors tested some single-lap 26 27 shear FRCM specimens with and without the external matrix layer in order to study the role of 28 the different matrix layers in the stress-transfer mechanism. It was found that the external layer 29 increases the bond strength of about 10%. Accordingly, in the model we set $\beta=9$, so that 9/10 of 30 the tensile force is transmitted to the support through the internal cementitious layer, and 1/10 31 through the external layer.

32 We restrict the study to the sample portion between the yarn and the clamped side of the sample, 33 where the most severe stress state is attained. Accordingly, we limit to the cross-section part 34 drawn in Fig. 5e, where only the yarn portion of area $\beta A/(1+\beta)$ is considered (red area). It is supposed to be rectangular. Sliders at the boundaries reproduce the confining effect of the 35 36 excluded part of the sample, and the isostatic lines simplify in equispaced straight lines, parallel 37 each other. For this simplified geometry, the strain state is plane, belonging to planes parallel to 38 the x-y plane. The geometry of the plane problem is plotted in Fig. 5e, with h=100 mm, $b_1=5$ mm, $b_2=0.23$ mm (determined for A=1.04 mm² and $b_3=4$ mm). 39

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Figure 5. Problem geometry. (a) Yarn-matrix geometry of the shear bond test. (b) Shear stress isostatic lines in a cross section. (c) Infinitesimal portion of the sample and representative isostatic surfaces (d) Shear stresses in the green (internal) and blue (external) isostatic lines. (e) Cross-section portion. (f) Transversal section subject to plane strain state.

6 7 *3.2 Energies*

8 We refer to the geometrical scheme drawn in Fig. 5f, with Ω_m the matrix domain, Ω_f the yarn 9 domain, and Λ the matrix-yarn interface. The tensile displacement δ applied at the upper side of 10 Ω_f monotonically increases from $\delta_0=0$ to a final positive value. Body loads are neglected, and the 11 hypothesis of plane strain states is assumed. For a given Cartesian coordinate system (O;*x*,*y*), the 12 displacement field is $\mathbf{u}(\mathbf{x}) = [u(x, y), v(x, y)]^T$. We assign the following energies to Ω_m , Ω_f , and 13 Λ .

14 *Yarn energy.* Ω_f is supposed to be made of an isotropic linearly elastic material. Its strain energy 15 is

16
$$\mathsf{E}_{f}(\mathbf{u}) = \int_{\Omega_{f}} (\mu_{f}(\nabla \mathbf{u})^{2} + \frac{\lambda_{f}}{2} (div\mathbf{u})^{2}) d\mathbf{x}, \qquad (2)$$

- 17 where μ_f and λ_f are the Lamé's coefficients.
- 18 *Matrix energy.* The cementitious matrix Ω_m is supposed to be brittle, and its energy is

19
$$\mathsf{E}_{m}(\mathbf{u},\alpha) = \int_{\Omega_{f}} \left(W_{S}^{-}(\mathbf{u}) + (1-\alpha)^{2} (W_{S}^{+}(\mathbf{u}) + W_{D}(\mathbf{u})) \right) d\mathbf{x} + \int_{\Omega_{f}} d\left(\alpha + \frac{l_{m}^{2}}{2} (\nabla \alpha)^{2} \right) d\mathbf{x} \,. \tag{3}$$

1 It depends on the displacement **u** and on the additional scalar field $\alpha(x): \Omega_f \to [0,1]$, which 2 plays the role of a damage parameter. When $\alpha=0$, the material is intact, and, when $\alpha=1$ the 3 material is completely fractured. The first integral in (3) represents the elastic strain energy. W_s^+

4 and W_s^- are the spherical parts of the energy, evaluated in points where volume expansion (

5 $div\mathbf{u} > 0$) and contraction ($div\mathbf{u} < 0$), respectively, are attained, and W_D is the deviatoric part of

6 the energy, whose expressions are

7
$$W_{S}^{\pm}(\mathbf{u}) = \left(\frac{\mu_{m}}{3} + \frac{\lambda_{m}}{2}\right) (div^{\pm}\mathbf{u})^{2}, \quad W_{D}(\mathbf{u}) = \mu_{m} (\nabla \mathbf{u})^{2} - \frac{\mu_{m}}{3} (div\mathbf{u})^{2}, \quad (4)$$

8 with $div^+\mathbf{u} = \max(div^+\mathbf{u}, 0), \quad div^-\mathbf{u} = \max(-div^-\mathbf{u}, 0).$

9 The second integral in (3) represents the damage energy, and it is sum of a local linear term and a quadratic non-local gradient term. Energies of the form (3) have been proposed in [32,33] for 10 11 materials exhibiting different damage evolutions in the case of expansion and compression. When (3) is minimized, a competition is engaged between the first integral term, the elastic bulk 12 13 strain energy, and the second integral term, the damage energy. The strain energy is minimized for fixed **u** by α =1, while the damage energy is minimized by α =0. However, the transition from 14 15 $\alpha=0$ to $\alpha=1$ is associated with a non-null value of $\nabla \alpha$, indeed penalized by the second integrand of the damage energy, which represents an interface energetic term. Since $(1-\alpha)^2$ multiplies 16 $(W_{S}^{+}+W_{D})$ in the first integral, only the strain energy associated to expansion and shear enters 17 18 into the competition with the damage energy. Thus, in the body regions where the volume 19 change is negative, only the deviatoric part of the strain energy can be released by the creation of 20 damage, and thus shear mode failures are energetically convenient (mode II failure). On the 21 contrary, in the regions with positive volume change, the whole elastic energy may redeem the increments of the damage energy, and, as a result, mixed cleavage and shear failures evolve. 22

In [34,45], the minimum problem associated to the functional (3) has been solved in the onedimensional setting of a tensile test, and the following simple formulas have been deduced

25
$$d = \frac{\sigma_e^2}{E}, \quad l_m = \frac{B}{2\sqrt{2}}, \quad G = \frac{4\sqrt{2}}{3}dl_m,$$
 (5)

which relate the constitutive parameters d and l_m , characterizing the damage energy in (3), to easily measurable experimental data, obtained from tensile tests, i.e., the normal stress σ_e attained at the end of the elastic phase, the fracture toughness G, the Young's modulus E, and the size B of the so called process zone, i.e., that zone where damage develops and coalesces in a fracture surface. Formula (5)₂ clearly show that l_m is an internal length of the material related to the width B of the damage bands. In the simulations we determine d and l_m from (5)_{1,2}, and (5)₃ gives an estimate of the fracture toughness.

33 In (2) and (4), Lamé's coefficients μ and λ are related to the Young's modulus E and Poisson's

34 ratio v by the formulas $\mu = \frac{E}{2(1+v)}$, $\lambda = \frac{Ev}{(1+v)(1-2v)}$.

35 *Interface energy.* A distribution of damageable elastic springs are assumed at the matrix-yarn 36 interface Λ , whose energy is

37
$$\mathsf{E}_{s}(\boldsymbol{\delta},\alpha) = \int_{0}^{h} (1-\alpha)^{2} \frac{1}{2} \mathbf{K} \boldsymbol{\delta} \cdot \boldsymbol{\delta} dy + \int_{0}^{h} a \left(\frac{1}{q} ((1-\alpha)^{-q} - 1) + \frac{1}{2} l_{s}^{2} (\nabla \alpha)^{2} \right) dy.$$
(6)

1 depends the displacement jump interface It on at the $\boldsymbol{\delta}(y) = [\delta_x(y), \delta_y(y)]^T = [u_f(b, y) - u_m(b, y), v_f(b, y) - v_m(b, y)]^T$, and on the interface damage 2 3 $\alpha(y)$, with $y \in (0,h)$. As in (3), the first integral represents the elastic energy and the second integral accounts for the damage energy. The elastic coefficients k_x and k_y of the springs in the 4 normal and tangential directions to the interface are collected in the elastic tensor $\mathbf{K} = \begin{vmatrix} k_x & 0 \\ 0 & k_y \end{vmatrix}$. 5

6 The damage energy is sum of two contributions, a power function of α , and a non-local quadratic 7 function depending on $\nabla \alpha$. To characterize the coefficients *a* and *q* in the damage energy, we 8 solve the problem of an infinitesimal interface of length *dy*, subject to a shear stress τ . If we 9 suppose that $\delta_x = 0$, the elementary interface energy is

10
$$d\mathsf{E}_{s}(\delta_{y},\alpha) = ((1-\alpha)^{2} \frac{1}{2}k_{y}\delta_{y}^{2} + \frac{a}{q}((1-\alpha)^{-q} - 1) - \tau\delta_{y})dy, \qquad (7)$$

11 where the last term is the external energy due to the imposed shear load τ . To determine an 12 equilibrium configuration we require the energy first variation to be non-negative, 13 $\delta d\mathbf{E}_{s}(\delta_{y}, \alpha; \lambda, \beta) \ge 0$, for any admissible perturbations (λ, β) such that $\beta \ge 0$. Following a 14 standard variational procedure, we obtain the equilibrium relations

15
$$\tau = (1-\alpha)^2 k_y \delta_y, \qquad -(1-\alpha)k_y \delta_y^2 + a(1-\alpha)^{-(q+1)} \ge 0.$$
 (8)

16 The first equation is the linear load-displacement relation due to the springs. The second relation is a damage criterion analogous to the criterion discussed in [34,45] for the uniaxial traction 17 problem. The damage can evolve, when $(8)_2$ is satisfied as an equality, and it cannot, if $(8)_2$ is a 18 strict inequality. In a process of increasing τ , starting from an unstressed undamaged initial 19 configuration $\tau = \delta_y = \alpha = 0$, (8)₂ is strictly satisfied for $\alpha = 0$ and $\delta_y < \sqrt{a/k_y}$, or, 20 equivalently, $\tau < \sqrt{ak_y}$. Thus, for $0 < \tau \le \sqrt{ak_y}$, the evolution is purely elastic. The elastic 21 undamaged phase ends when $\tau = \tau_e = \sqrt{ak_y}$ (and $\delta_y = \delta_e = \sqrt{a/k_y}$), from which we obtain 22 $a = \tau_e^2 / k_v$. 23 (9)

This relation is used to calibrate the constitutive parameter *a*, since the right-hand side quantities are easily measurable from experiments. In the subsequent damage phase $\tau > \tau_e$, α grows, and (8)₂ is satisfied as an equality. We say that the evolution is stress-hardening (-softening) if $d\tau/d\alpha > 0$ (<0), that is, if the stress increases (decreases) for α growing. From (8), the evolution is stress-hardening if q>2 and stress softening if -2 < q < 2. In Fig. 6, dimensionless $\delta \tau$ curves are plotted for different values of q. If we solve (8) with respect to α and q, for given values of $\delta_y = \delta_r$ and $\tau = \tau_r$, we obtain the expression

31
$$q = -2\left(\frac{\log(k_y \delta_r^2) - \log a}{\log \tau_r - \log(k_y \delta_r)} + 1\right),$$
(10)

where log stands for the natural logarithm. Suppose that a pair (δ_r, τ_r) is know from experiments. For instance, τ_r is the residual shear stress mainly due to friction attained at a large relative 1 shear displacement δ_r (see the pair (δ_r, τ_r) in the curve q=-1 of Fig. 6). Then (10) is used to 2 calibrate the parameter q.

3



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Figure 6. Response curves for different values of q.

7 The internal length l_s in (6) is related to the length of the detachment front *D*, where α grows 8 from 0 to 1, by the relation [34,45]

9
$$l_s = \frac{D}{const}$$
, with $const = \int_0^1 \sqrt{\frac{q}{(1-\alpha)^{-q}-1}} d\alpha$. (11)

10 If we assume q=-2 of a brittle interface, we get $const = \pi\sqrt{2}/2$, and thus $l_s \approx 0.45D$.

11 *Total Energy.* The internal energy of the system is sum of the energies (2), (3) and (6) 12 $E(\mathbf{u}, \alpha) = E_{\mathcal{T}}(\mathbf{u}) + E_n(\mathbf{u}, \alpha) + E_s(\delta(\mathbf{u}), \alpha).$ (12)

13 Since volume and surface forces are null, it coincides with the total energy of the system.

15 *3.3 Incremental energy minimization*

16 The evolution of **u** and α , for increasing δ , is determined by means of incremental energy 17 minimization. We introduce an evolution "time"-like parameter *t*, and suppose that δ , **u** and α 18 depend on *t*. In particular, δ is a monotonic increasing function of *t*. A subscript is used to 19 indicate dependence on *t*, and a dot indicates derivative with respect to *t*, i.e, given a function 20 $v=v_t(\mathbf{x}), \ \dot{v}=dv/dt$. Time is discretized into intervals of length τ , and within each time step 21 $t \rightarrow t + \tau$, we suppose that **u** and α are linear functions of τ

$$\mathbf{u}_{t+\tau} = \mathbf{u}_t + \tau \dot{\mathbf{u}}_t, \quad \alpha_{t+\tau} = \alpha_t + \tau \dot{\alpha}_t, \tag{13}$$

and that the energy (12) is approximated by the second-order development

24
$$\mathsf{E}(\mathbf{u}_{t+\tau},\alpha_{t+\tau}) = \mathsf{E}(\mathbf{u}_{t},\alpha_{t}) + \tau \dot{\mathsf{E}}(\mathbf{u}_{t},\alpha_{t};\dot{\mathbf{u}}_{t},\dot{\alpha}_{t}) + \frac{1}{2}\tau^{2} \ddot{\mathsf{E}}(\mathbf{u}_{t},\alpha_{t};\dot{\mathbf{u}}_{t},\dot{\alpha}_{t}).$$
(14)

25 This latter rewrites in the form

$$\mathsf{E}(\mathbf{u}_{t+\tau},\alpha_{t+\tau}) = \mathsf{E}(\mathbf{u}_{t},\alpha_{t}) + \tau \mathsf{F}(\dot{\mathbf{u}}_{t},\dot{\alpha}_{t}), \tag{15}$$

with $F(\dot{\mathbf{u}}_t, \dot{\alpha}_t)$ a quadratic functional of $(\dot{\mathbf{u}}_t, \dot{\alpha}_t)$. If (\mathbf{u}_t, α_t) at the instant *t* is known, the solution (13) at $t+\tau$ is obtained by finding the pair $(\dot{\mathbf{u}}_t, \dot{\alpha}_t)$, which solves the constrained quadratic programming problem

30
$$(\dot{\mathbf{u}}_t, \dot{\alpha}_t) = \arg\min\{\mathsf{F}(\dot{\mathbf{u}}_t, \dot{\alpha}_t), \dot{\alpha}_t \ge 0, + \text{boundary conditions}\}.$$
 (16)

A numerical code has been developed to solve the above problem. Discretized by linear triangular finite elements, the solution of (16) has been determined at each time step by means of an alternate iterative minimization procedure that consists in iterating the double minimization of F, first with respect to $\dot{\mathbf{u}}_t$, keeping $\dot{\alpha}_t$ fixed, and then with respect to $\dot{\alpha}_t$, for fixed $\dot{\mathbf{u}}_t$. Iterations are stopped when the L_{∞} -norm difference of two consequent solutions is smaller than a certain tolerance. Convergence and computational performances of this numerical scheme are thoroughly discussed in [46].

8 9

4. Experimental results

1011 4.1 Tensile test results

12 FRCM specimens with coated carbon fabrics were significantly affected by external thermal exposure. Table 5 shows the average results based on three test repetitions. Even if the number 13 14 of specimens tested was limited from a statistical point of view, and results should be confirmed 15 by a larger experimental programme, the emerging line seems quite clear and it allows the considerations reported below. The decrease in mechanical properties of the FRCM system with 16 17 coated fabrics when exposed to high temperatures is significant compared to those at room 18 temperature when the ultimate tensile strength (σ_u) at 80 and 120 °C was found to be 70% lower compared to that at 20 °C for the coated fabric, while the reduction of the ultimate strain (ε_2) was 19 20 substantial, from 26000 to 4000 µE when the specimen was tested at 120 °C. The failure by slippage was the same of the dry fabric FRCM. On the other hand, the FRCM systems with 21 22 uncoated (dry) fabric only experienced a drop of 11% in tensile strength when subjected to 120 23 °C.

- 24
- 25

	Ta	able 5. To	ensile test re	sults [acc	cording to	AC434, A	Annex A]			
					%		%		Fail	
Mortar	Fabric	Temp.		E_2	change	$\sigma_{\rm u}$	change	ε2	Fall.	
		(°C)		(GPa)	E_2^*	(MPa)	σ_u^*	(%)	mode	
		20	Average	-	-	986	-	0.010	S	
		20	COV (%)	-		9		30		
	Dry	80	Average	-	-	955	-3	0.012	S	
		80	COV (%)	-		11		18		
		120	Average	-		874	11	0.008	S	
Mortar-	ır-		COV (%)	-	-	12	-11	12		
45		20	Average	49		1366		0.026	E/C	
			20	COV (%)	25	-	18	-	22	F/S
	цс	80	Average	36	27	411	-70	0.012	S	
	пз		COV (%)	14	-27	6		10		
		120	Average	35	20	407	70	0.004	0	
				120	COV (%)	17	-29	10	-70	22

- 26 27
- S) Slippage of the fabric within the matrix

7 F) Fabric failure

*Percentage change relative to room temperature conditions (20 °C)

1 4.2 SEM analysis

2 The interface between coated carbon yarn and surrounding mortar was observed by SEM (at a 3 magnification ranging from 30x to 50x), on samples cured at room temperature (Figure 7a) and

4 after being tested at 120 °C (Figure 7b).

5 The post-tensile test detachment of the yarn from the matrix resulting in the consequent slippage

6 of the fabric within the mortar is visible. The coated yarn is free to slip within the mortar after

7 the resin has exceeded the T_g , while the sand grains used to improve the interface bond remain

- 8 attached to the cementitious matrix.
- 9



- 10 3kV WD11mmSS40 x30 500 m set 3kV WD14mmSS45 x50 x50 for 500 m set 3kV WD14mmSS45 x50 x50 for 500 m set 3kV wD14mmSS45 x50 for (a) and after (b) tensile test
- 12

12 13 4.3 Double shear bond test results

14 Double shear tests confirmed the undesirable effect of the enhancement of coating the FRCM 15 fabric on the mechanical properties of the FRCM system when subjected to high temperature 16 conditions. Results of the maximum peak load and relative displacements are reported in Table 17 6. At room temperature, the FRCM enhanced with coated fabric had higher shear bond strength 18 than the dry counterpart; however, when exposed to high temperatures the bond at the fabricmortar interface for fibers with epoxy coating is reduced as seen in the displacement-load graph 19 20 shown in Figure 8. The reduction in both load carrying capacity and displacement of the fibercoated FRCM system is due to the exposure to 120 °C temperature. 21

Table 6. Double shear test results [according to RILEM TC250-CSM]

				Peak Load	%	Slip at	Peak stress in
Mortor	Fabria	Temp (°C)		(per side)	change	peak	the textile
WIOItal	Faunc			f_{max}	f _{max}	load	$\sigma_f = f_{max}/A_f$
				(N)		(mm)	(MPa)
		20	Average	1976		2.89	633
	Der		COV (%)	10	-	5	10
	Dry	120	Average	2094	1.6	3.37	671
Mortar-			COV (%)	9	+0	8	9
45		20	Average	2503		4.18	802
	UC		COV (%)	2	-	2	2
	п3	120	Average	961	62	3.12	308
			COV (%)	8	- 02	6	8

A comparison between FRCM specimens exposed and not to temperature, both tested at 20°C, is shown in Figure 9. Results reported in Table 7 show that the maximum peak load is almost the same, even after the specimens have been exposed to 120 °C for 60 minutes. The decrease of the peak load is of 7%, while the ultimate slip remained unchanged. This study showed that the use of FRCM reinforced with coated fabric is able to maintain a good shear-bond resistance even after being exposed to high temperature conditions. Temperatures up to 120 °C are not able to decompose the selected coating and after cooling the epoxy regains its integrity.



1



Figure 8. Double shear bond test: comparison between FRCM reinforced with dry and coated
 (HS) fabrics at different temperatures

13

10



Figure 9. Double-shear bond test: FRCM residual performance after exposure to high temperature (120 °C) for 60 minutes

- 16 17
- 18
- 19
- 20 21

Tuble 7. Double shear test results after exposure to 120 C										
[according to RILEM TC250-CSM]										
				Peak Load	%	Slip at	Peak stress			
Mortar	Fabric	Temperature		(per side)	change	peak	in the textile			
Willian	Fabric	of testing		f_{max}	f _{max}	load	$\sigma_f = f_{max}/A_f$			
		(°C)		(N)		(mm)	(MPa)			
		20	Average	2503		4.18	802			
Mortar-	HS	20	COV (%)	2	-	2				
45		20	Average	2325	7	4.19	745			
		Aft.exposure	COV (%)	4	- /	4				

Table 7 Double shear test results after exposure to 120°C

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4 5. Numerical simulations and comparisons with experimental results

5

6 5.1 Parameters setting

7 We consider the simplified two-dimensional geometry of Fig. 5f, discussed in Sect. 3.1. Matrix 8 and yarn domains are discretized by three-nodes triangular finite elements as shown in Fig. 10a. 9 Young's modulus, Poisson's ratio and tensile strength of the two materials have been assigned as 10 reported in Table 1 and Table 3. Since the aggregates in the cementitious mortar have a size of 11 about 1-2 mm, we assign B=5 mm to the thickness of the process zone. Indeed, according to [47, 12 31], the characteristic thickness of the damage localization zone is typically 2-3 times the size of 13 the constituent grains. Given *E*, σ_e and *B*, parameters *d* and l_m are determined from (5)_{1,2}.

Different constitutive parameters are assigned to the matrix-fiber interface to distinguish dry 14 (DRY) and coated with sand (HS) carbon fabrics at different temperature exposure. These 15 16 parameters have been assigned following an inverse approach, starting from global double shear 17 bond curves (Fig. 8) and deriving the interface constitutive parameters. For sake of simplicity, 18 we assume $k=k_x=k_y$ in the elastic tensor k in (6). Since experiments exhibit temperature-19 dependent mechanical responses, the interface constitutive parameters vary with temperature T, that is, k=k(T), $\tau_e=\tau_e(T)$ and q=q(T). Parameters k and τ_e define slope and extension of the $d-\tau_e$ 20 elastic branch, while q describes the shape of the post-elastic branch. We recall that a in (6) is 21 22 determine from k and τ_e by formula (9). A schematic description of the procedure followed to fix 23 the interface material parameters is proposed in the Appendix.

Interface parameters - DRY carbon fabrics. To reproduce the brittle behavior observed in experiments (see Fig. 8a), we assign q=-2, whatever the temperature *T*. In this way, the interface brutally breaks when the limit elastic shear stress τ_e is reached (see Fig. 6). For *k* and τ_e , we fix $k(20)=1.6 \text{ N/mm}^3$, $k(120)=1.2 \text{ N/mm}^3$, and $\tau_e(20)=\tau_e(120)=1.7 \text{ MPa}$ (see Fig. 10b). For increasing *T*, *k* reduces, while τ_e maintains fixed.

Interface parameters - HS carbon fabrics. Temperature considerably affects the parameter values in the case of coated fabric, since the mechanical characteristics of the epoxy coating

highly depends on thermal conditions. We assume k(20)=1.0 N/mm³, $\tau_e(20)=1.9$ MPa, and

32 k(120)=0.4 N/mm³, $\tau_e(20)=0.75$ MPa (see Fig. 10b). Thus, temperature considerably reduces

33 both the elastic coefficient and the shear strength. Since the experimental curves of Fig. 8b

34 exhibit a pronounced post-elastic softening phase, curves of Fig. 6 suggest of choosing values of

35 q within the range (-2, 2). At $T=20^{\circ}$ C, we set q=0.3, which accounts for a residual shear strength

- 1 $\tau_r = 0.68$ MPa at the shear displacement $\delta_r = 4\delta_e = 7.6$ mm (black dot in Fig. 10b). At $T = 120^{\circ}$ C, we
- 2 set q=0.7. In such a way, at the shear displacement $\delta_r=4\delta_e=7.5$ mm, the residual shear strength is 3 $\tau_r=0.39$ MPa (red dot in Fig. 10b).
- 4 For both the HS and DRY samples, we assume $l_s=1$ mm.
- 6 5.2 Results and comparisons

5

- 7 Simulations are conducted by increasing the vertical displacement δ , and computing the
- 8 corresponding vertical normal stress σ_y on the yarn upper side. These quantities are related to the
- 9 overall displacement s and force f (see Fig.8), measured in the experiments and reported in the
- 10 curves of Fig. 8, by means of the relations

11
$$s = \delta + \frac{h_y}{E_f} \sigma_y, \quad f = 3\sigma_y A, \tag{17}$$

where $h_y=310$ mm is the length of the fabric part which connects the sample to the experimental pulling device, E_f is the yarn Young's modulus, and A is the yarn cross-section area. In (17)₁, the second term in the right-hand side is the displacement due to the elastic deformation of the fabric, and, in (17)₂, the factor 3 accounts for the number of yarns in a fabric.

16 The force-displacement curves given by the numerical simulations are drawn in Fig. 8 (solid 17 line) and compared with the experimental curves (dotted line).

18 Results - DRY carbon fabrics. As expected, DRY 20C and DRY 120C samples exhibit a 19 brittle behavior. The matrix-yarn interface experiences debonding when the limit shear stress τ_e 20 is attained, while the cementitious matrix never damages. The evolution of the damage α and 21 shear stress τ_v for increasing δ is described in Fig. 11. The δ -f curves plotted in Fig. 11a exhibit steep and short softening branches, concluding with drops. Profiles of α and τ_e on the interface 22 23 of the DRY 20C sample are plotted in Figs. 11b and 11c for different values of δ within the 24 softening phase (dotted points in the enlargement of Fig. 11a). Damage forms in the upper part 25 of the interface, when the limit shear stress τ_e is reached. Then it evolves downward, for almost a 26 half of the interface. Finally, a brutal failure occurs, producing the total detachment of the yarn 27 from the matrix. Fig. 11c shows that τ_v is equal to τ_e at the tip of the damaged zone, and it 28 decreases within the damaged zone as α increases, being null there where $\alpha=0$.

Results - HS carbon fabrics. Even in HS samples (with coated carbon fabrics), damage 29 concentrates at the matrix-fiber interface, and the cementitious matrix maintains undamaged. 30 31 Both at $T=20^{\circ}$ C and 120° C, the *s*-*f* curves of Fig. 8 present softening branches which accurately 32 fit the experimental curves. Damage evolution at the interface is described in Fig. 12 where 33 snapshots of α and τ_{v} are represented at different values of s corresponding to the softening regime. In the two simulations, damage forms in the upper side of the interface when τ_{v} reaches 34 35 the maximum value τ_e , and rapidly spreads all over the interface. Increasing s, damage almost homogeneously increases all over the interface, and, at s=0.7 mm, the final damage values 36 37 $\alpha = 0.64$ and 0.60 are reached at the interface of the HS 20C and HS 120C samples, 38 respectively. The interface at the end of the experiments is not completely damaged, being α_f 39 quite smaller than 1, and this provides residual strength sources to the system. A residual 40 strength is also observed in the experiments. Indeed the curves of Fig. 8b present non-null values 41 of f at their end. This strengthening at large displacement may be due to the augmented frictional

- 42 effect produced by the coating.
- 43





Figure 10. (a) Mesh. (b) δ_{v} - τ curves for the calibration of the interface damage parameters.



4 (a) (b) (c) 5 Figure 11. (a) Displacement δ – force f curves for the DRY_20C and DRY_120C samples. 6 Profiles of damage α (b) and shear stress τ (c) at the interface of the DRY_20C sample for 7 different values of δ .



Figure 12. Profiles of damage α and shear stress τ at the interface of the HS_20C (a,b) and HS_120C (c,d) samples for different values of s [mm].

5 6. Conclusions

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6 Based on the experimental results for specific materials presented herein, the following is 7 concluded:

- FRCM systems can provide high temperature compatibility retaining mechanical performance when exposed to 120 °C depending on the type of fabric reinforcement used.
- FRCM reinforced with dry (uncoated) fabric seemed not to be influenced by the temperature conditions, maintaining an adequate resistance and bond to the substrate even when exposed to 120 °C for 100 minutes, when the temperature at the fabric to mortar interface was between 90 and 110 °C.
- FRCM reinforced with coated fabric showed drastic differences depending on the temperature of testing. The reduction in the FRCM ultimate tensile capacity was of 70% when tested at 80 °C. Same reduction was observed at 120 °C. Double shear bond tests showed a reduction in the ultimate peak load of 61% at 120 °C.
- FRCM with coated fabrics preheated to 120 °C for 60 minutes and tested at room temperature showed smaller differences in terms of mechanical performances. The decrease of the peak load was of 7% and no differences were detected in the ultimate slip. These results showed that the coating is able to maintain a good adhesion with the mortar when cooled down after being exposed to 120 °C for 60 minutes. Therefore, FRCM reinforced with coated carbon fabric can still maintain its structural function after the exposure to high temperatures, upon to 120 °C.
 - Numerical simulations confirmed that for any temperature and fabric type, the main failure mechanism is by fabric slippage within the cementitious matrix.
- Double shear simulations on FRCM reinforced with DRY fabrics exhibit a top-down detachment at the matrix-fibers interface after reaching the maximum peak load. The

damage development occurs in the first 50mm of the bonded area and quickly evolves along the entire bond length up to failure.

- In presence of coated fabrics, the detaching mechanism is different from that observed in DRY fabrics. Indeed, damage almost homogeneously starts and slowly develops at the whole fabric-matrix interface. At the end of the softening phase, the interface is only partially damaged, and it is still able of bearing some shear stresses. This final residual strength accounts for material frictional sources.
- 8 Although there are few results in literature regarding the FRCM behavior when exposed • 9 to high temperature conditions, some qualitative comparisons can be made. As 10 experienced by Trapko, FRCMs have proven to behave better than FRP when exposed to 11 high temperatures, maintaining an adequate load bearing capacity up to 80 °C [17]. The 12 use of coated fabrics improved the FRCM mechanical performances but can be a 13 limitation if the temperature within the FRCM exceeds the coating T_g. However, coated 14 fabrics are able to recover the load bearing capacity when cooled down after being 15 exposed to high temperatures (at least up to 120 °C for 60 minutes of exposure) and this 16 result is in line with the experiments conducted by Silva and co-workers [22].
- 17 18

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We conclude that the phase-field variational approach has revealed a promising approach for the study of the failure mechanisms in FRCM systems. Indeed, the proposed model, despite its geometrical simplicity, has shown great predictive capabilities, accurately reproducing the experimental results. A further advantage of the model is that it only depends on few parameters directly related to specific materials properties, which are easily measurable from experiments. Here they are determined following an inverse procedure, i.e., assigning those values that allow for a better fitting of the experimental response curves.

The use of FRCM as externally bonded reinforcement could be a valid solution to be adopted in case of applications were high temperature service conditions is a possible scenario. However, further tests on the mechanical behavior and durability of FRCM systems when exposed to these particular environmental conditions are necessary to better understand the phenomenon and provide guidelines for a proper design approach.

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1 APPENDIX

2 The procedure used to fix the model parameters is schematically drawn, and some examples are3 presented.

4

5 *Parameters setting procedure.* Assume that:

- 6 (i) the experimental force-displacement curve is given. Here, as an example, the curve of 7 Fig. 13 is considered, which is an HS_20 curve among the three HS_20 curves of Fig. 8 8b;
- 9 (ii) the 2D geometry is defined according to the criteria given in Sect. 3.1;
- 10 (iii) Young's modulus and Poisson's ratio of matrix and yarn E_m , v_m , E_f , v_f are given;
- 11 (iv) splitting tensile strength σ_e of the matrix is known;
- (v) size *B* of the process zone in the mortar is assigned (typically 2-3 times the size of the cementitious aggregates);
- 14 (vi) the internal length l_s in (6) is assumed equal to 1 mm; being related to the sliding 15 process zone, its value is quite uncertain and thus it is fixed arbitrary. However, not-16 reported simulations have shown that different values do not modify the results.

17 The remaining constitutive parameters are deduced as follows:

- 18 1. the coefficients d and l_m in the matrix damage energy are determined from formulas 19 $(5)_{1,2}$;
- 20 2. the maximum elastic shear stress τ_e of the interface (see Fig. 6) is estimated from the 21 maximum elastic force f_e of Fig. 13 by the formula
- 22

23

26

$$\tau_{\rm e} = \frac{1}{3hb_3} \frac{\beta}{(\beta+1)} f_e , \qquad (1A)$$

obtained from (1) and (17)₂, where h and b_3 are the geometrical lengths of Fig. 5;

24 3. the elastic coefficient of the interface springs is $k = \tau_e/\delta_e$ (see Fig. 6), which, use (17)₁, 25 rewrites

$$k = \tau_{e} / \left(s_{e} - \frac{h_{y}}{3A} \frac{f_{e}}{E_{f}} \right), \tag{2A}$$

- 27 where quantities on the right-hand side are known (τ_e is determined at point 1, f_e and s_e 28 are deduced from the experimental response curve (point i), E_f is known (point iii), and h_y 29 and A are assigned geometrical quantities (point ii)). Formula (2A) is used to set k;
- 30 4. once τ_e and k are estimated, coefficient a in (6) is determined from (9);

5. finally, the exponent q in (6), which controls the slope of the softening branch, is chosen
via a fitting procedure, aimed at reproducing the experimental softening branch (Fig. 13).

Examples. Some examples are proposed, where the above procedure of parameters setting is followed.

35 We assume the experimental curve of Fig. 13 (point i), the geometry defined in Sect. 3.1 (point

ii), the constitutive parameters of Tables 1 and 3 (points iii and iv), and *B*=5 mm (point v).

Following prescriptions given at points 1-4, we obtain: d=0.0011 MPa, $l_m=1.77$ mm (point 1); $\tau_e=1.9$ MPa (point 2); k=1 MPa/mm (point 3); a=3.61 MPa mm (point 4). For the softening exponent q of point 5, different values are considered in order to show the parameter influence on the post-elastic stress-softening behaviour. Numerical response curves corresponding to different q are drawn in Fig. 13. The slope of the softening branch reduces as q increases. In this case, the curve for q=0.3 is allows for the best matching the experimental softening branch.

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