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Investigation of drying effects on cracking and mechanical performance of concrete

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Abstract

Cement based material structures have to be managed over long periods. However, they are exposed to numerous loadings and present time-dependent deformations. In addition, the material is a porous medium in which pore pressure has an impact on macroscopic behaviour. The goal of this study is to better forecast the long term behaviour of cement based materials taking into account interactions between loads, time-dependent deformations and pore pressure. To do so, a previous model developed by the authors is extended to implement the impact of pore pressure on mechanical behaviour. Two applications are ran on a mature ordinary concrete. The damage state is clearly dependent of the mechanisms involved in the modelling. Considering pore pressure allows to find damage depth more accurate regarding experimental evidences. After a calibration of one test (at $h_r = 45\%$), we were able to predict the behaviour of other tests, with an underestimation of 10% on one sample where h_r changed. Interestingly, it was not manageable to use the same Biot parameter for the drying shrinkage and the mechanical modelling. As a result, by changing drastically the mechanical behaviour, the modelling of pore pressure is important to accurately assess the cracking state.

Keywords: Concrete, Cementitious Material, Drying Transport, Capillary Pressure, Eigen Stresses

1. Introduction

The long term management of key concrete structures (dams, power plant, nuclear waste facility disposal, large bridges) needs modelling to accurately predict cracking. This phenomenon is known to reduce drastically the lifespan of such struc-

5 tures. Today, mechanical loadings are rather well controlled with standards (Eu-
6 rocode, ACI, JCI), whereas environmental actions (temperature, humidity) remain
7 problematic. Strains related to temperature and humidity, both due to seasonal vari-
8 ations or human activities (cooling tower in the process of electricity generation),
9 often present inadequate predictions of behaviour in service regarding cracking, in
10 particular, structures with geometric differences that favour restrictions [1]. To ac-
11 count for all these loads, models have to be multi-physics.

12 An important feature of concrete material is the time-dependent strain which is a
13 well known phenomenon that control the long term behaviour of the material [2, 3].
14 This includes creep, shrinkage and swelling. Moreover, cement based materials are
15 porous medium. Initially saturated, they are the seat of water movements and ex-
16 changes with the ambient atmosphere. These exchanges generate eigen-stresses or
17 cracking resulting from different mechanisms [4, 5]. This is a multi-scale problem.
18 At the macroscopic scale, the impact of drying gradients related to shrinkage within
19 the material will generate tensile stresses on the surface, which are self-equilibrated
20 by compression of the core. In addition, strains incompatibilities occur between com-
21 ponents: reinforcements and concrete [6], aggregates and cement pastes [7, 8, 9, 10]
22 and incompatibilities due to different geometry and stiffness between concrete mem-
23 bers [11, 12]. Finally, at nano-scale, drying modifies the physical characteristics of
24 Calcium Silicate Hydrate (C-S-H) [13]. All of these have an impact on the service-
25 ability, the mechanical performance of both the current loadings [14, 15, 16, 17] and
26 the accidental loadings [18]. For some structures as dams, nuclear containment ves-
27 sels, it may significantly increase concrete permeability and reduce tightness [19, 20].
28 It could also favour the penetration of aggressive species (carbonation, sulphate and
29 chloride ions). Thus, to forecast the long term behaviour it is necessary to model
30 hydric loading explicitly.

31 At a macroscopic level, there are many approaches in the literature which in-
32 corporate hydric loading. Uncoupled approaches are developed in structural design
33 codes [21, 22] supposing homogeneous delayed strains. More complex approaches
34 are requisite to account for heterogeneous strains. In a continuum framework
35 [2, 23, 24, 25] or in discrete models [26, 27] many models have been developed

36 assuming that the total strain is split between the different delayed strains with no
37 regard for capillary pressure. Others use poro-mechanics to distinguish the role of
38 water (capillary, disjoining pressures) and the matrix [28, 29].

39 In this study, we extend a model previously developed at a macroscopic scale
40 and in a continuum framework [23, 30, 31] to add to autogeneous, thermic, drying
41 shrinkage and creep strains, the effects of hydric forces in the mechanical model. The
42 first part of the paper is devoted to the description of the model constitutive equations,
43 with a special focus given on the incorporation of pore pressure's effects. A second
44 part deals with numerical modelling. Those are performed under CAST3M software
45 [32] and compared to a set of experimental data obtained on a mature ordinary
46 concrete [17].

47 **2. Hygro-mechanical modelling**

48 *2.1. General framework of modelling*

49 The proposed model is based on the classical assumption of small strain. Under
50 this hypothesis, total strain is decomposed in an elastic strain (ϵ_{el}) and in a various
51 term of additional strains (Eq. 1).

$$\epsilon_t = \epsilon_{el} + \sum_i^n \epsilon_i \quad (1)$$

52 For the purpose of this work, drying shrinkage, creep and cracking are considered
53 as additional strains. Hydration is not investigated and material properties are
54 assumed at their final stage. Autogeneous shrinkage and thermal strains are not
55 considered. These are motivated by the lack of information regarding hydration
56 process on the isothermal experimental basis used for the model calibration. How-
57 ever, it is easy to add strains in this model as shown by Briffaut et al. [30]. Moreover,
58 based on experimental evidences [33, 34], it is assumed that transport and mechan-
59 ical phenomena are decoupled. As a result, the hygro-mechanical model developed
60 in this paper is computed as a staggered problem weakly coupled as proposed in
61 earlier studies [31, 35, 36].

62 *2.2. Drying modelling*

63 There are different approaches to model drying processes in the literature. These
 64 include models based on a diffusion equation written in either water content [37, 38,
 65 39] or relative humidity [40]. There are more complex approaches based on modelling
 66 the transfer of different components within the porous material [41, 42, 43]. In this
 67 approach, the capillary pressure gradient is used as the main driving potential and
 68 the liquid and vapor water phases are modelled. It has shown to be adapted for
 69 high performances and ordinary concretes [41, 42, 43]. Thus, with the assumption of
 70 isothermal exchanges, in a mature material with a gas phase composed of a mixture
 71 of two perfect gases (dry air and water vapour) and neglecting the Darcean transport
 72 of the gas in front of the diffusion of water vapour, the drying state is governed by
 73 the differential equation (2).

$$\phi \left(1 - \frac{\rho_v}{\rho_w} \right) \frac{\partial S_w}{\partial P_c} \frac{\partial P_c}{\partial t} = \text{div} \left[\left(\frac{k_{rl}(S_w) K_w^{int}}{\mu_w} + \frac{d_{rl}(S_w) D_v^{int} P_{vs}}{\rho_w^2} \left(\frac{M_v}{RT} \right)^2 \exp \frac{-P_c M_v}{\rho_w RT} \right) \nabla \mathbf{P}_c \right] \quad (2)$$

74 where ρ_w (kg m^{-3}), ρ_v (kg m^{-3}), μ_w (Pa s), $\phi(-)$, M_v (g mol^{-1}), R ($\text{J K}^{-1} \text{mol}^{-1}$), T
 75 (K), P_{vs} (Pa), S_w (-), P_c (Pa), K_w^{int} (m^2) and D_v^{int} ($\text{m}^2 \text{s}^{-1}$) stand respectively for
 76 the density of liquid and water vapour, the dynamic viscosity of liquid, the porosity,
 77 the molar mass of water, the perfect gas constant, the temperature, the saturated
 78 vapour pressure, the liquid saturation degree, the capillary pressure, the intrinsic
 79 permeability and diffusivity.

80 To assess the evolution of saturation degree with regard to relative humidity, the
 81 relationship proposed by van Genuchten [44] is used (Eq. 3).

$$S_w = [1 + (-\alpha \ln(h_r))^\gamma]^{-\beta} \quad (3)$$

$$\gamma = \frac{1}{1 - \beta} \quad (4)$$

82 with α (-), γ (-) and β (-) three parameters that can be fitted on experimental
 83 isotherms and h_r (-) the relative humidity. Furthermore, the relation (4) is proposed
 84 by van Genuchten to restrict γ values according to β in the case of Mualem pore
 85 size distribution [45].

86 Concerning transport properties, another important point which has to be con-
 87 sidered is their evolutions regarding the saturation degree [43]. In order to model
 88 these dependencies, the relative permeability is modelled according to van Genuchten
 89 [44] model (Eq. 5) and the relative diffusivity is taken as Millington and Quirk [46]
 90 suggest (Eq. 6).

$$k_{rl}(S_w) = \frac{K_w^{eff}}{K_w^{int}} = S_w^{n_k} [1 - (1 - S_w^{\frac{1}{\beta}})^{\beta}]^2 \quad (5)$$

$$d_{rl}(S_w) = \frac{D_v^{eff}}{D_v^{int}} = \phi^{a_{mq}} (1 - S_w)^{b_{mq}} \quad (6)$$

where n_k (-) is a pore factor, related to tortuosity, equal to 0.5 according to van Genuchten [44], but evolving in the range of -4.5 to 5.5 (see for instance [47, 48]), and β (-) the factor set in the relation (3), a_{mq} (-) and b_{mq} (-) model parameters taken between 1.3 and 2.74 and 3.3 and 4.2 respectively [43]. Finally, drying flux through boundaries conditions is assumed to be convective and takes the form of equation (7).

$$\Phi_{conv} = h_c (P_{ci} - P_{ce}) \mathbf{n} \quad (7)$$

91 where Φ_{conv} is the drying flux (m s^{-1}), h_c ($\text{m s}^{-1} \text{Pa}^{-1}$) is the drying boundary co-
 92 efficient, P_{ci} and P_{ce} in (Pa) are respectively the surface and surrounding capillary
 93 pressure and \mathbf{n} the normal of the surface. These capillary pressures are determined
 94 by the Kelvin-Laplace equation.

95 2.3. Shrinkage modelling

96 Drying shrinkage is a macroscopic consequence of the evolution of water content
 97 in the porous body. It is a combination of a variation of capillary pressure [49],
 98 disjoining pressure [50, 51] and surface free energy upon drying. To model this
 99 phenomenon, it exists on the one hand, simple models based on phenomenological
 100 observations [52] and on the other hand poro-mechanical approaches [53, 54] (review
 101 of poromechanical model in Di Bella et al. [55]). The model used in this application
 102 is the one proposed by Coussy et al. [54] (Eq. 8a). They introduce the equivalent
 103 pore pressure π (Eq. 8b) that takes into account the contribution of the interface
 104 energy $U(S_w)$ (Pa).

$$\epsilon_{ds} = \frac{1 - 2\nu}{E} b_w \pi \mathbf{1} \quad (8a)$$

$$\pi = P_m - U(S_w) \quad \text{with} \quad U(S_w) = \int_{S_w}^1 P_c(S'_w) dS'_w \quad (8b)$$

105 with E (Pa) the young modulus, P_m (Pa) the average pore pressure, which is equal
 106 to $P_m = S_w P_c$ in the case where gas pressure is negligible (close to atmospheric
 107 pressure) and b_w (-) the Biot liquid coefficient. Finally equation (8a) assumes that
 108 the porous body is elastic. This may lead to an underestimation of two or three times
 109 the final shrinkage amplitude [56]. To avoid this pitfall, a viscous-elastic modelling
 110 is used [57] (Eq. (9)).

$$\epsilon_{ds} = (1 - 2\nu) b_w S_w \int_{t'=0}^{t'=t} \left(\frac{1}{E} \frac{dP_c}{dt'} + J(t - t', t') \frac{dP_c}{dt'} \right) dt' \quad (9)$$

111 with J (Pa^{-1}) the specific creep. This model allows for retrieving partially reversible
 112 drying shrinkage in the case of wetting [58], but not completely the hysteresis, also
 113 present in the water retention curve, but not modelled here.

114 2.4. Creep modelling

115 2.4.1. Basic creep

116 The model used in this work is the one presented by Hilaire [31]. It is a modified
 117 Burger model composed in serial of a Kelvin-Voigt solid plus an aging dashpot. The
 118 constitutive equations of the model are given by the equations:

$$\dot{\epsilon}_{bc} = \dot{\epsilon}_{kv} + \dot{\epsilon}_{am} \quad (10a)$$

$$\frac{\dot{\sigma}}{k_{kv}} = \tau \ddot{\epsilon}_{kv} + \left(1 + \frac{\dot{k}_{kv}}{k_{kv}} \tau \right) \dot{\epsilon}_{kv} \quad \text{with} \quad \tau = \frac{\eta_{kv}}{k_{kv}} \quad (10b)$$

$$\dot{\epsilon}_{am} = \alpha_{bc} \frac{\langle \sigma \rangle_+}{\eta_{am}(t)} + \frac{\langle \sigma \rangle_-}{\eta_{am}(t)} \quad \text{with} \quad \eta_{am}(t) = \eta_{am}^\infty \times t \quad (10c)$$

119 with α_{bc} (-) a parameter allowing to model the dissymmetric behaviour between
 120 tension and compression (not investigated in this paper $\alpha_{bc} = 1$, for more informa-
 121 tion regarding the impact of α_{bc} see [31]), k_{kv} (Pa) the stiffness and η_{kv} (Pa s^{-1})

122 the dynamic viscosity of the Kelvin-Voigt solid and η_{am}^∞ (Pa s⁻¹) the final dynamic
 123 viscosity of the aging dashpot.

124 To take into account multi-axial loadings, this approach has been classically
 125 extended to three-dimensional problems using the behaviour law of the material by
 126 introducing the basic creep Poisson's ratio ν_{bc} (Eq. 11) .

$$J_{ijkl}(t, t') = J_{1D}(t, t') \left[-\nu_{bc} \delta_{ij} \delta_{kl} + \frac{1 + \nu_{bc}}{2} (\delta_{il} \delta_{jk} + \delta_{ik} \delta_{jl}) \right] \quad (11)$$

127 with $J_{1D}(t, t')$ the basic specific creep in the uniaxial case and δ the Kronecker
 128 symbol.

129 2.4.2. Drying creep

130 Drying creep is the result of two distinct phenomena. The micro-cracking part
 131 which is characterized when the cracking of the material is considered and the stress-
 132 induced shrinkage part which has to be explicitly taken into account. Several works
 133 [59, 60, 61] highlight the proportionality relationship between drying shrinkage and
 134 drying creep strains. This observation leads to a model linking the drying creep
 135 strain rate to the drying shrinkage one (Eq. 12).

$$\dot{\epsilon}_{dc} = \lambda_{dc} |\dot{\epsilon}_{ds}| \sigma \quad (12)$$

136 with λ_{dc} (Pa⁻¹) a constant and σ (Pa) the uniaxial stress. This model is extended
 137 in a fully three dimensional problem by introducing a drying creep Poisson's ratio,
 138 equal to the basic creep, in a similar way than basic creep (see equation 11).

139 2.5. Mechanical modelling

140 2.5.1. Damage modelling

The mechanical model used in this work is an isotropic damage model; the
 decrease of the material stiffness is described by the evolution of a variable D . In
 an isotropic case, this scalar variable is introduced in the behaviour law (Eq. 13).

$$\sigma = (1 - D) \mathbb{C} : \epsilon \quad (13)$$

141 with \mathbb{C} the four order stiffness tensor non-damaged. The allowable values of $D \in [0;1]$.
 142 The evolution of the damage depends on a load threshold function: $f = \epsilon_{eq} - \kappa(D)$,
 143 where κ the hardening-softening parameter is equal to the maximum of ϵ_{eq} or ϵ_{d0}
 144 and ϵ_{d0} the tensile damage threshold corresponding to $\frac{f_t}{E}$. Classically, the equivalent
 145 strain (ϵ_{eq}) is taken as proposed by Mazars [62]:

$$\epsilon_{eq} = \sqrt{\sum_{i=1}^3 \langle \epsilon_i \rangle_+^2} \quad (14)$$

with, ϵ_i the principal strains extension. Under multi-axial loading, the damage variable is taken as a linear combination of variables D_t and D_c (Eq. (15)).

$$D = \alpha_t^{\beta_D} D_t + \alpha_c^{\beta_D} D_c \quad (15)$$

with β_D (-) a parameter introduced by Pijaudier-Cabot et al. [63] to describe shear dominated problems. In the following applications this parameter will always be equal to 1. Then α_t parameter is calculated based on the principal strains ϵ_i :

$$\alpha_t = \sum_{i=1}^3 \frac{\epsilon_i^t \langle \epsilon_i \rangle_+}{\epsilon_{eq}} \quad (16)$$

146 with ϵ_i^t positive principal strains and $\alpha_c + \alpha_t = 1$. Regarding compression and
 147 tension damages, the original evolution law proposed by Mazars [62] is used (Eq. 17)
 148 in compression. In order to use energetic regularization, damage evolution law in
 149 tension is defined as Feenstra and De Borst [64] proposed (Eq. 18).

$$D_c = 1 - \frac{\epsilon_{d0}(1 - A_c)}{\kappa} - A_c \exp(-B_c(\kappa - \epsilon_{d0})) \quad (17)$$

$$D_t = 1 - \frac{\epsilon_{d0}}{\kappa} \exp(-B_t(\kappa - \epsilon_{d0})) \quad (18)$$

150 ϵ_{d0} (-) the damage threshold in tension, A_c (-) and $B_{t,c}$ (-) the model parameters
 151 controlling the post-pic phase evolution of the material behaviour.

152 To avoid mesh dependency and non uniqueness of the solution due to softening
 153 behaviour of concrete, an energetic regularization is used [65]. This regularization
 154 is based on parameter B_t (-) which is a function of the size of element h (m), the
 155 tensile strength f_t (Pa), the fracture energy G_f (J m^{-2}) and the threshold ϵ_{d0} (-):

$$B_t = \frac{h \times f_t}{G_f - \frac{hE\epsilon_{d0}^2}{2}} \quad (19)$$

156 *2.5.2. Effect of h_r on mechanical strength*

157 Drying effects are numerous and occur at different time and scales in cementi-
 158 tious materials. First of all, rather hydration is not investigated, there is an obvi-
 159 ous competition between hydration (consumption of water by cement reaction) and
 160 drying (exchange between concrete porosity and ambient atmosphere). As a result
 161 moist curing plays an important role in strength development [66]. Second of all,
 162 drying shrinkage appears as a macroscopic consequence of drying. As concrete and
 163 more generally cementitious material present heterogeneity and low permeability,
 164 differential drying shrinkage develops both between the core and the edge of the
 165 material [4, 5] as well as between the different components due to stiffness differ-
 166 ence [7, 8, 10]. Finally, drying in porous network generates pore pressure composed
 167 of capillary pressure, disjoining pressure and surface energy which are involved in
 168 the pre-stressing of the solid part of the microstructure. As mentioned by Bažant
 169 et al. [2] the latter explain in part the improvement of the tensile strength if con-
 170 crete is dried without any cracking. These are confirmed by experimental evidences
 171 [67, 68, 69] and are modelled in others approaches [28, 29].

172 To account for the impact of pore pressure in the modelling of the mechanical
 173 behaviour, the initial behaviour law (Eq. 13) is expressed in the framework of porous
 174 media [70] (Eq. 20).

$$\boldsymbol{\sigma} = \mathbb{C}\boldsymbol{\epsilon} + bp\mathbf{1} \quad (20)$$

175 with p (Pa) the pore pressure and $\boldsymbol{\sigma}$ (Pa) the total stress. Then the aforementioned
 176 f threshold function of the original Mazars's model is conserved but the threshold
 177 (ϵ_{d0}) is rewritten as (Eq. 21).

$$\epsilon_{d0} = \frac{f_t}{E} - \frac{(1 - 2\nu)}{E} b_m S_w P_c \quad \text{with} \quad P_c < 0 \quad (21)$$

178 with b_m (-) a calibration parameter. To illustrate the positive contribution of cap-
 179 illary pressure in the modelling (Eq. 21), the behaviour of an unity cubic volume

180 under respectively tension (Fig. 1a) and compression (Fig. 1b) loadings, is inves-
 181 tigated. In these examples, drying shrinkage is uniform, *i.e.* there is no drying
 182 cracks.

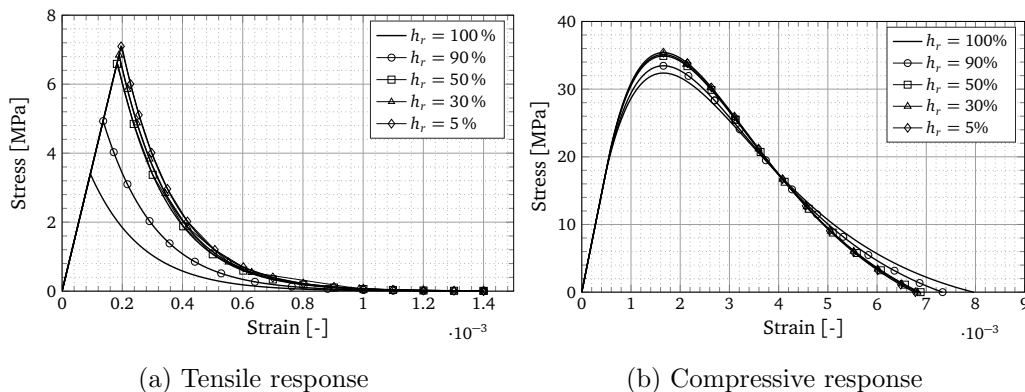


Figure 1: Mechanical responses with $b_m = 0.3$ (Eq. 21) for different uniform values of relative humidity (h_r), in tension and in compression (drying shrinkage is thus uniform, *i.e.* no drying cracks occur).

183 Neither in tension nor in compression the tangent modulus is affected by capillary
 184 pressure (Fig. 1). On the contrary, the peak tensile strength is strongly influenced
 185 by the saturation degree (Fig. 1a). This is not similar under compression (Fig. 1b).
 186 In compression, the extensions are not direct but induced by loading. As a result,
 187 the impact of capillary pressure is reduced. Another important point is the non
 188 linearity of the impact drying. This results from the function $S_w P_c$ which is an
 189 increasing function non linear. Finally, in this first attempt to model the effect of
 190 pore pressure, only capillary pressure is taken into account. The pressure induced
 191 by disjoining pressure and surface energy are not considered. It may result in
 192 a misleading impact of pore pressure in the low relative humidity ranges (below
 193 $h_r = 40\%$).

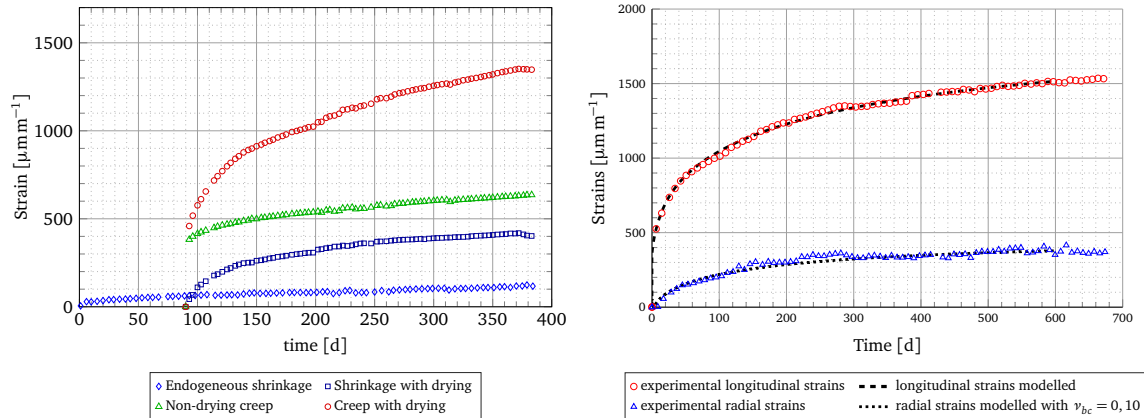
194 3. Cases of application

195 The hygro-mechanical approach presented in section 2 is applied to two different
 196 cases. The first one is interested in a prismatic specimen that undergoes drying
 197 shrinkage until drying equilibrium. The second regards a bending beam subjected
 198 to mechanical loading after a curing period of 70 days. The experimental data used

199 as a basis for this modelling were obtained within the framework of the PhD of
 200 Soleilhet [17]. During the PhD no creep data were obtained. To overcome this
 201 problem, creep models were identified on VeRCoRs project data [71]. The concretes
 202 used in both experimental investigations were almost similar.

203 3.1. Identification of creep model parameters on VeRCoRs data base

204 VeRCoRs is a project led by Électricité de France [72]. It consists in a 1/3 scale
 205 concrete containment building (CCB) built to improve the understanding and the
 206 modelling capabilities of ageing and leakage of double walls CCB. In this project,
 207 creep were monitored on cylindrical specimens of $16 \times 100 \text{ cm}^2$ size over a period of
 208 10 months (Fig. 2a). The storage conditions of the specimens were a temperature of
 209 20°C and a relative humidity of 50%. Mechanical compression loading of 12 MPa
 210 was applied after 90 days. The strains were measured with strain gauges (for more
 211 information regarding experimental protocol, see Charpin et al. [73] on another
 212 concrete.).



(a) Delayed strains of VeRCoRs concrete [71]

(b) Total creep strains identified

Figure 2: Experimental and identified delayed strains of VeRCoRs concrete

213 Characteristic delayed strains were computed from this data base and then iden-
 214 tified in order to simulate the total creep (Fig. 2b). Data regarding delayed strains
 215 (basic and drying specific creep) are presented in Appendix C. Identified parameters
 216 are summarized in Table 1. In order to fit the radial strains, a creep Poisson ratio
 217 of 0.10 is identified.

τ_{am} (d)	η_{∞} (GPa s ⁻¹)	k_{kv} (GPa)	τ_{kv} (d)	ν_{bc} (-)	λ_{dc} (MPa ⁻¹)
90	130	135	15	0.10	7.82×10^{-2}

Table 1: Set of creep parameters identified on VeRCoRs data

218 3.2. Drying shrinkage prism

219 In this application, a $7 \times 7 \times 28$ cm³ concrete prism is subjected to a dry envi-
 220 ronment with 45 % of relative humidity for a period of 400 days. The modelling is
 221 carried out using a three-dimensional simulation of one eighth of the test piece due
 222 to symmetry conditions (Fig. 3).

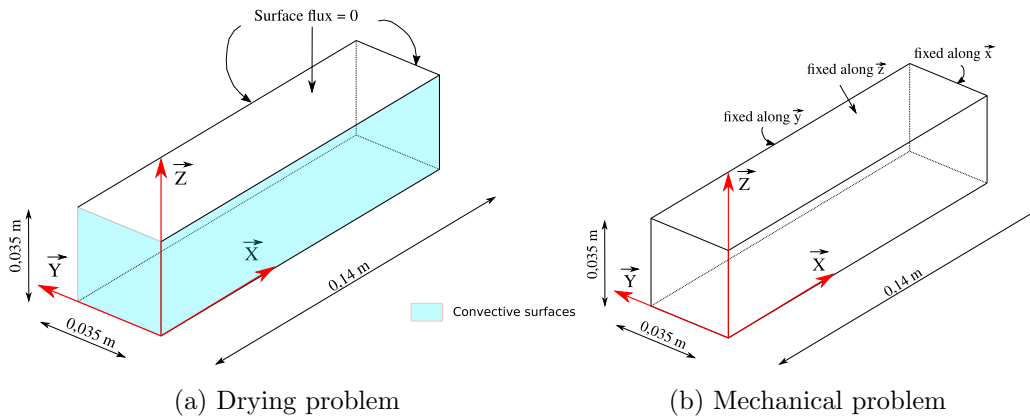


Figure 3: Geometries and boundaries conditions for drying shrinkage prism.

223 For drying submodelling, the ambient relative humidity is imposed on convective
 224 surfaces (Fig. 3a). The parameters of the drying model (Tab.2) are identified based
 225 on the relative mass variation of the experimental specimens (Fig. 4a). To assess
 226 the evolution of relative mass variation, the equation (22) is used.

$$w_n = \frac{\phi \rho_w}{\rho_c} S_w \quad (22)$$

227 with w_n (%) the water mass content, ρ_c (kg m⁻³) the saturated concrete density. This
 228 relationship allows to compare the computed mass variation with the experimen-
 229 tal results and to identified drying parameters (Tab. 2). The drying identification
 230 was processed algorithmically by an automatic update of the finite element model
 231 parameters (see Carette et al. [74]).

β (-)	α (-)	n_k (-)	K_w^{int} (m ²)	ϕ (-)	h_c (m s ⁻¹ Pa ⁻¹)	ρ_c (kg m ⁻³)
0.48	5.6	-0.30	3.88×10^{-21}	0.17	48	2370

Table 2: Identified set of drying parameters. For lack of sensitivity in this hydric condition water vapour diffusion parameters are taken equal to $a_{mq} = 2.74$, $b_{mq} = 4.2$ and $D_v^{int} = 2.55 \times 10^{-5}$ m² s⁻¹ as suggested by Thiery et al. [43].

232 Drying equilibrium (close to 5.2% after 400 days) and drying kinetic are well
233 modelled (Fig. 4a). The accurate prediction of the relative mass variation allows to
234 model the drying shrinkage according to the equation (9). For mechanical modelling,
235 the symmetry plans of the sample are blocked (Fig. 3b). The resulting evolution
236 with an identify value of Biot parameter equal to 0.30, overestimates the ultimate
237 shrinkage at equilibrium of about 18% (Fig. 4b). In addition, it is remarkable that
238 the model does not seem at equilibrium at 400 days even if relative mass variation
239 is constant. This is probably due to the visco-elastic part of the model. In this case,
240 the model is not totally in accordance with experimental data. However, it allows
241 to stay in a poromechanic framework for drying shrinkage and effect of capillary
242 pressure on strength.

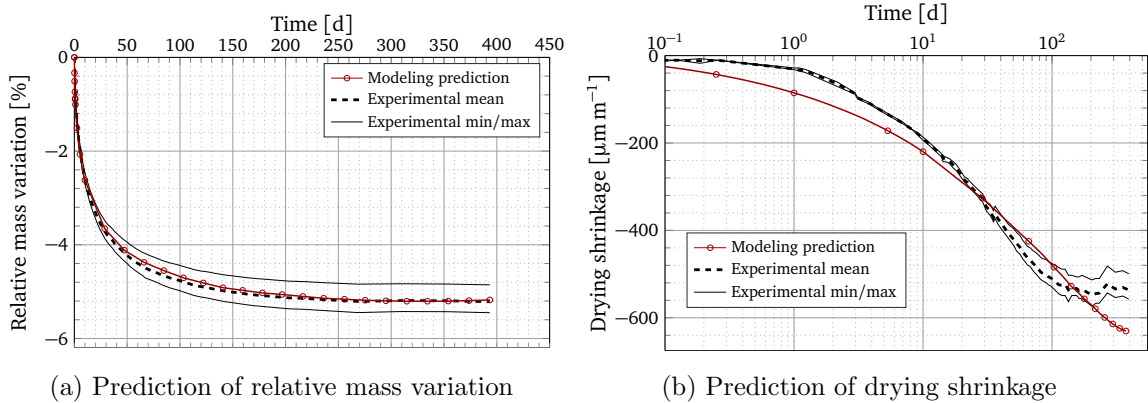


Figure 4: Evolution of drying and shrinkage over 400 days

243 According to the evolution of shrinkage, the normal stress state (along the x
244 axis) is studied (Fig. 5). Three modelling scenarios are investigated:

- 245 1. One without creep;
- 246 2. A second with creep taken into account ;

247 3. A third with creep and capillary pressure.

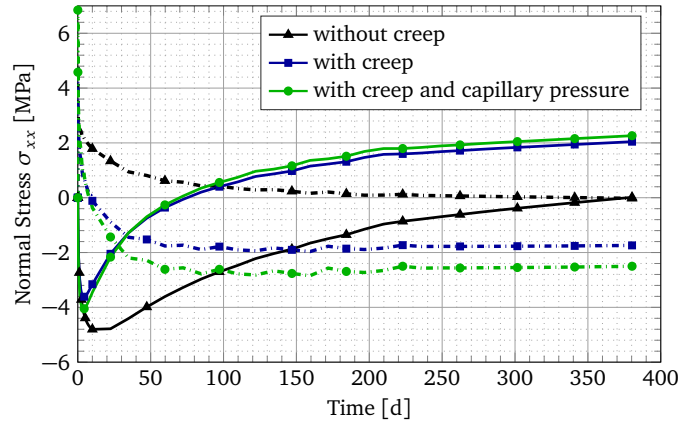


Figure 5: Evolution of σ_{xx} (see Fig. 3) on the surface in dotted lines and in the core of the test piece in solid lines.

248 Influence of creep on the evolution of normal stress (σ_{xx}) is highlighted. For the
 249 three models, the samples are free of stresses at time zero. With drying, the normal
 250 stress reaches the tensile strength at the edge. Different scenarios are simulated.
 251 Regarding the edge stresses, in the case (1), they soften until the damage variable
 252 reached the value of 1. Conversely in the case (2), the stress decreases rapidly due
 253 to relaxation phenomena within the specimen. Similarly, the compressive stress
 254 reached in the core of the specimen is 20% greater in the case (1). In addition,
 255 the amount of time needed to inverse the type of loading (tensile \Leftrightarrow compressive) is
 256 greater without creep (almost 375 days) than with creep (close to 30 days). Finally,
 257 the modelling (2) and (3) are almost similar both on the edge and on the core parts.
 258 The main difference is located in the peak stress reached on the skin of the sample.
 259 With the proposed formulation, the damage threshold is increased and as a result,
 260 the material stiffens and the maximum stress reached in the skin of the specimen
 261 (almost 7 MPa) increases and limits the evolution of damage.

262 3.3. Bending test under drying

263 In the latter application, $10 \times 10 \times 84 \text{ cm}^3$ test specimens (notched and unnotched)
 264 are subjected to a drying environment of respectively 30% and 45% relative humid-
 265 ity for a period of 70 days. They are then tested in three-points bending test.

266 Furthermore, experimental data from companion samples, that were kept under
 267 water over the same period are available to calibrate mechanical models.

268 3.3.1. Drying submodelling

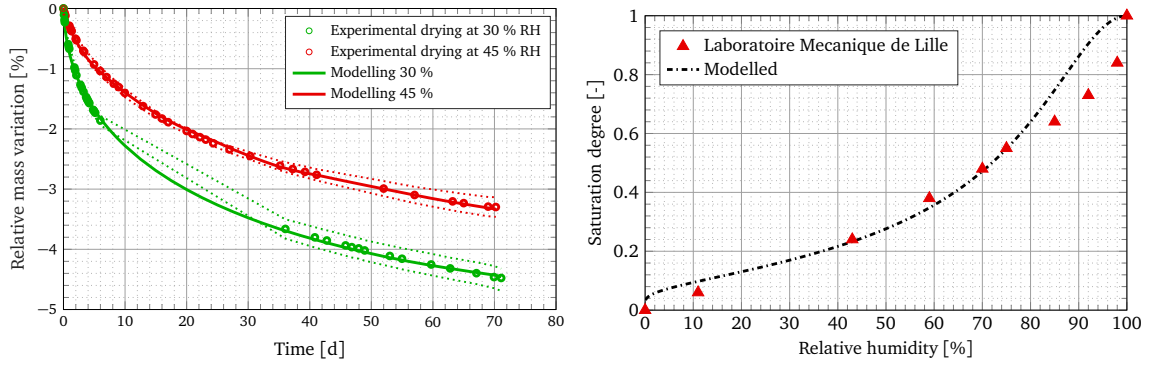
269 The first stage of the numerical modelling is conducted through a three dimen-
 270 sional modelling of the test specimens. Symmetries are not taken into account in
 271 order to let open the possibility to use random fields. The mesh of the sample is
 272 made of 36400 cubic linear elements. The mesh density is higher on the edge of the
 273 sample. These zones are expected to present the strongest drying gradient and a
 274 finer mesh (smallest element 1/10 the size of the biggest 20 mm) is necessary. The 70
 275 days of drying are discretized in time as follows: 40 time steps of 45 s followed by 40
 276 time steps of 2115 s, 20 time steps of 8640 s, 20 time steps of 30 240 s, 10 time steps of
 277 216 380 s and finally 10 time steps of 302 780 s. Ambient conditions are applied over
 278 the entire external surface through a drying flux. As mentioned previously, drying
 279 parameters are identified algorithmically and identified parameters are summarized
 280 in the Table 3.

h_r (%)	β (-)	α (-)	n_k (-)	K_w^{int} (m ²)	ϕ (-)	ρ_c (kg m ⁻³)
30	0.48	5.6	-2.45	1.6×10^{-21}	0.15	2450
45	0.48	5.6	-2.89	0.6×10^{-21}	0.14	2468

Table 3: Sets of drying parameters identified for the beam problem. Density and porosity are characterized experimentally and diffusion parameters remain identical (*i.e* $a_{mq} = 2.74$, $b_{mq} = 4.2$ and $D_v^{int} = 2.55 \times 10^{-5}$ m² s⁻¹).

281 Regarding the identified drying parameters values, slight differences between the
 282 two h_r conditions are noticeable. This can be partially explained by the variability
 283 of concrete itself and by the inherent variability between concrete batches. Never-
 284 theless, the parameters are really close.

285 As previously illustrated, the prediction of the relative mass variation is really
 286 accurate (Fig. 6a). Kinetic is well modelled in both condition and the identified
 287 value of intrinsic permeability is estimated close to 10^{-21} m². This is consistent for
 288 ordinary concrete [75].



(a) Relative mass variation until mechanical test (b) Modelling versus experimental desorption isotherm

Figure 6: Macroscopic drying properties. Green color for h_r equals to 30 % and red color for 45 %

289 Another key point in the drying modelling is the assessment of drying fields. This
 290 is a difficult task as there is no straight forward method to measure these fields and
 291 therefore to validate the modelling. As an attempt to validate drying modelling, the
 292 evolution of relative humidity field is plotted (Fig. 7). Since the drying boundaries
 293 conditions are symmetric, the resulting fields is as well. The slowness of the process is
 294 also illustrated: only few millimeters desaturated after one day (Fig. 7a). Afterwards
 295 drying process goes on and applied drying condition ($h_r = 45\%$) is reached on the
 296 edge of the sample at the end of the modelling period. After 70 days, the center
 297 of the specimen is close to 90 % of relative humidity, almost the same as the initial
 298 value (98 %). Finally, after 70 days, the drying gradients are important within the
 299 specimen. As an illustration, the average humidity gradient between the center and
 300 the edge of the sample ($\nabla h_r|_{x=0.42m}$) is almost equal to $1142\% \text{ m}^{-1}$.

301 3.3.2. Mechanical submodelling

302 In the mechanical part, the internal stresses induced by drying gradients and
 303 the residual mechanical behaviour are determined. The mechanical submodelling is
 304 based on the same mesh. Model's mechanical parameters are identified (algorithmi-
 305 cally) on experimental data (compressive and bending test) obtained on materials
 306 that have been prevented from drying. These tests were performed at the same
 307 time ($t=70$ days) in order to minimize the impact of hydration (Fig. 8). Identified
 308 parameters are resumed in Table 4. The first four parameters are identified on the

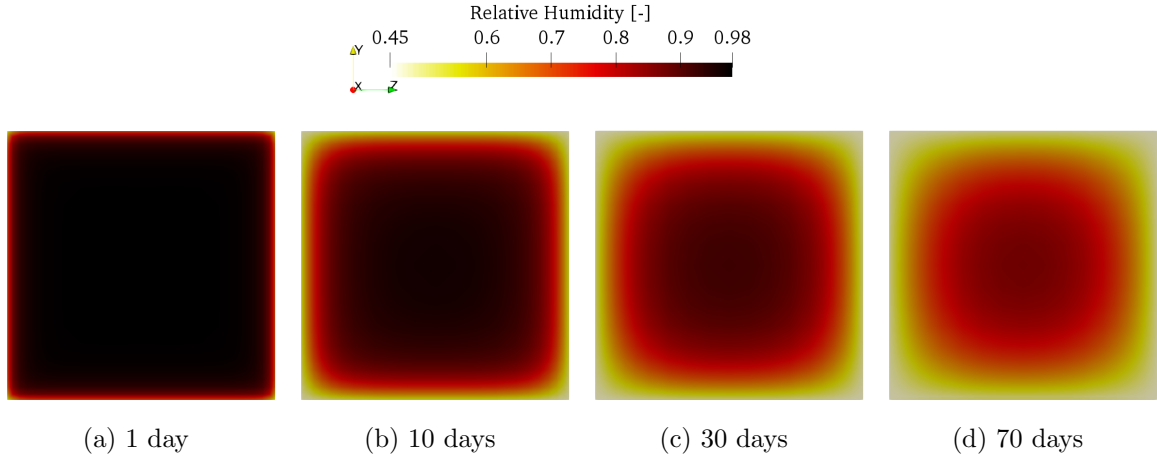


Figure 7: Evolution of h_r fields in the middle cross section. Case of applied $h_r = 45\%$

309 compressive tests. The tensile strength and cracking energy are then identified on
 310 the bending tests. Compressive parameters are adjusted if necessary (variation of
 311 few percent). It can be seen that the Young modulus values are close between the
 312 different modelling. As far as tensile strengths are concerned, they are between 2
 313 and 3 MPa. Finally, the cracking energy is close to 83 J m^{-2} on average. These
 314 identified values seem consistent for this type of concrete.

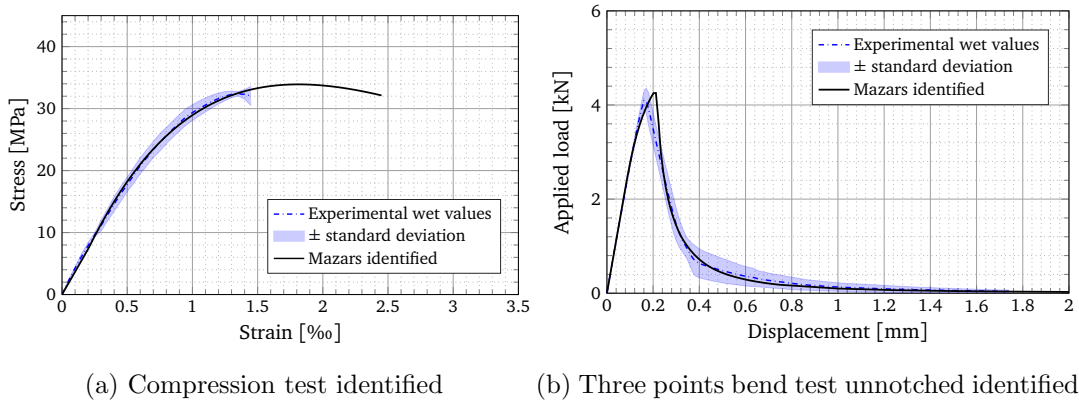


Figure 8: Mazars model identified. Example of companion samples from ambiance $h_r=30\%$.

315 Drying shrinkage is modelled with the same Biot coefficient than the one used in
 316 the first application (*i.e.* $b_w=0.30$). At 70 days the predicted value of drying shrinkage
 317 is consistent with the experimental value (Fig. 4b). Except for rigid bodies which
 318 are locked, the specimen is free to deform. The time stepping of the modelling is
 319 the same as the drying submodelling.

Campaign	Test	E (GPa)	ν (-)	a_{comp} (-)	b_{comp} (-)	f_t (MPa)	G_f (J m ⁻²)	β_D (-)
1	Compressive	36.9	0.24	1.33	1682	3.5	73	1
	Unnotched Bending	37.1	0.24	1.33	1682	2.5	88	1
	Notched Bending	38.0	0.24	1.33	1682	2.0	73	1
2	Compressive	36.7	0.24	1.38	1777	3.4	75	1
	Unnotched Bending	37.1	0.24	1.38	1777	2.9	95	1
	Notched Bending	38.0	0.24	1.38	1777	2.1	75	1

Table 4: Set of mechanical parameters of Mazars model. Poisson ratio set arbitrarily to ordinary value.

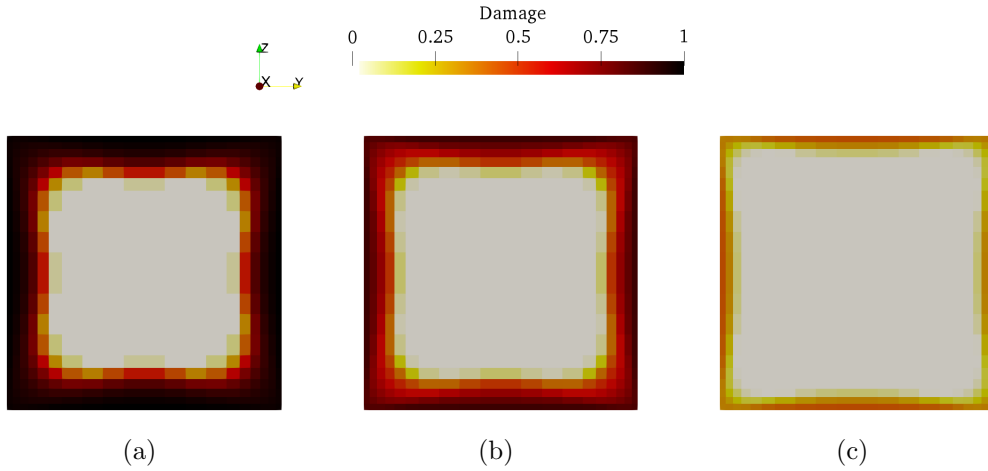


Figure 9: Damage fields in the middle cross section after 70 days as a function of mechanism modelled: a) only shrinkage b) creep taken into account c) Creep and capillary pressure modelled.

320 As illustrated (Fig. 5), the decrease in stress within the material is related to
321 the relaxation mechanism and so related to creep. In the case of damage, creep is
322 important but damage threshold is another key parameter which impacts directly the
323 state of damage (Fig. 9). Indeed, the damaged depth is lower when the relaxation is
324 modelled but it is still important. In the proposed model, the modelling of capillary
325 pressure induces an increase of tensile strength. This leads to a significant reduction
326 of damage (Fig. 9c). Here, damage induced by drying impacts only a few millimeters
327 (the damage variable is greater than 0.25 at a depth value of 3.2 mm). In similar
328 conditions (CEM I with a $w/c = 0.5$ under respectively 33% and 55% relative
329 humidity) Wu et al. [76] measured a damage depth close to 4 mm. In addition, the
330 internal stresses are investigated (Fig. 10). Prior to mechanical loading ($t=70$ days),
331 the stress state is different from zero. The edge are in tensile state and the core is

332 compressed. This mechanical state evolves with drying and so does the residual
 333 mechanical behaviour.

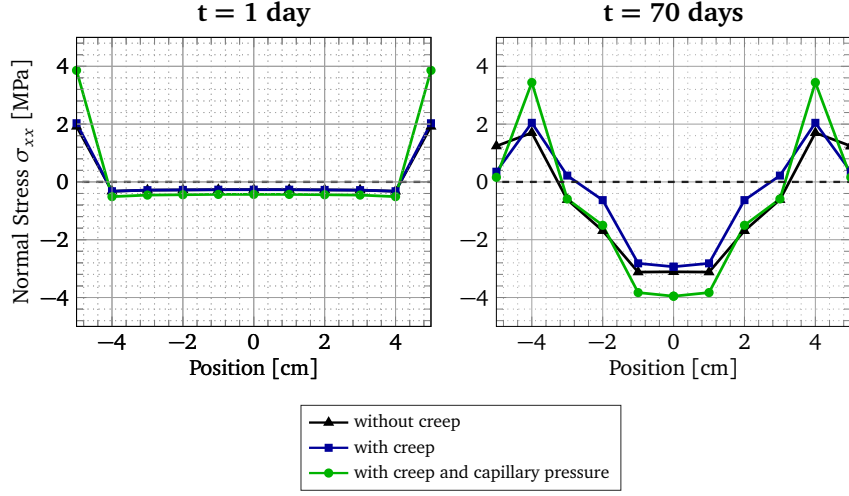


Figure 10: Normal stress along the center line in the middle cross section ($x=0.42$ m).

334 For the modelling of the bending tensile test, the mechanical state of the beam
 335 (damage, stress and displacement) induced by drying prior to loading is taken into
 336 account. At this stage, initial mesh is separated into two meshes. One for the
 337 unnotched beam with the same mesh characteristics and another for the notched
 338 beam. For this test, the only difference is the removal of the mesh elements in the
 339 notch. This result in a mesh composed of 36280 cubic linear elements (Fig. 11).

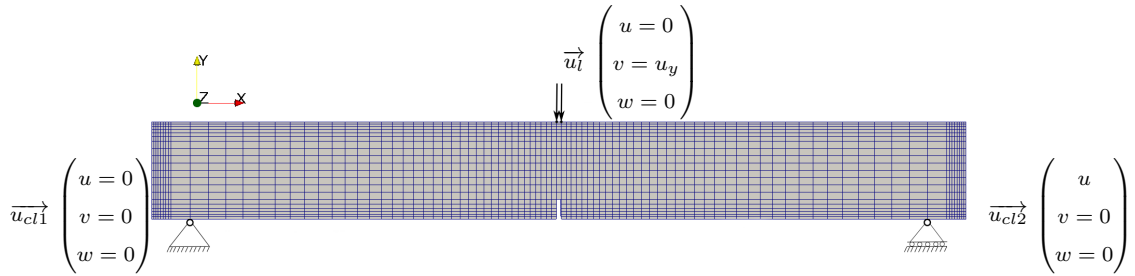


Figure 11: Meshes of bending test: special case of the notched beam. Drying occurs by all the external surfaces for the dried specimens. u_y is the imposed mechanical displacement.

340 Mechanical boundary conditions are similar in both bending tests. The transla-
 341 tion along \vec{y} and \vec{z} are fixed at the supports and the translation along \vec{x} is locked
 342 on one of the two supports and only (Fig. 11). The time steps are composed of 123
 343 time steps. They are divided into 3 time steps of 0.67 s followed by 5 time steps

344 of 2.2 s, 25 time steps of 1.80 s, 25 time steps of 9.28 s, 25 time steps of 17.4 s and
 345 finally 40 time steps of 16.9 s.

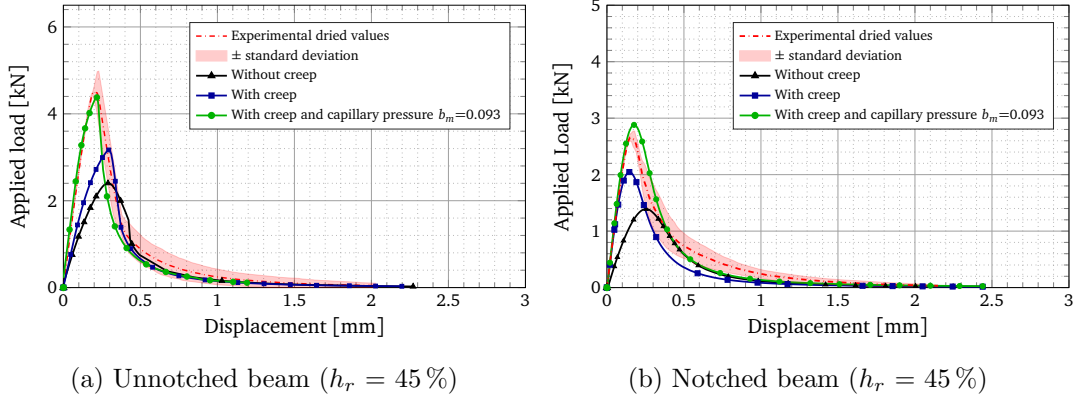


Figure 12: Prediction of the beam behaviour under three points bending load after 70 days of drying. Experimental value obtained on the mean of three samples.

346 To model the impact of capillary pressure on strength, the value of $b_m=0.09$ is
 347 calibrated on one beam (Fig. 12a) and is no longer modified. Similarly of previous
 348 study the predicted mechanical behaviour of the beam is determined regarding the
 349 diverse mechanisms modelled (Fig. 12). As expected, the residual macroscopic me-
 350 chanical behaviour is strongly correlated to the damage state. Taking into account
 351 the only mechanism related to drying shrinkage is not sufficient. The stiffness as
 352 well as the peak strength are reduced (respectively 50% and 47% of peak strength).
 353 However, modelling creep improves the behaviour but it is not enough. The gap
 354 remains significant in strength (underestimation of 28% and 23% of peak strength)
 355 even though the stiffness in the notched case is well predicted as opposite of the
 356 unnotched case. This difference is explained by the notch. In the case of the spec-
 357 imen unnotched, the edges of the sample are damaged. In contrary in the notched
 358 case, as the notch is realised just prior to bending, the partially damaged zone is
 359 removed. When the loading is applied, the material in the notch is undamaged.
 360 Finally, the modelling of the three mechanisms allows to recover the experimental
 361 behaviour both the stiffness and the peak strength.

362 The behaviour prediction of the samples kept at 30% relative humidity is then
 363 realised (Fig. 13). Except for the modelling of notched specimens, which slightly

364 underestimates the value of peak strength of 14 % (Fig. 13b), the chosen value is
 365 able to stand for the physic of all these tests.

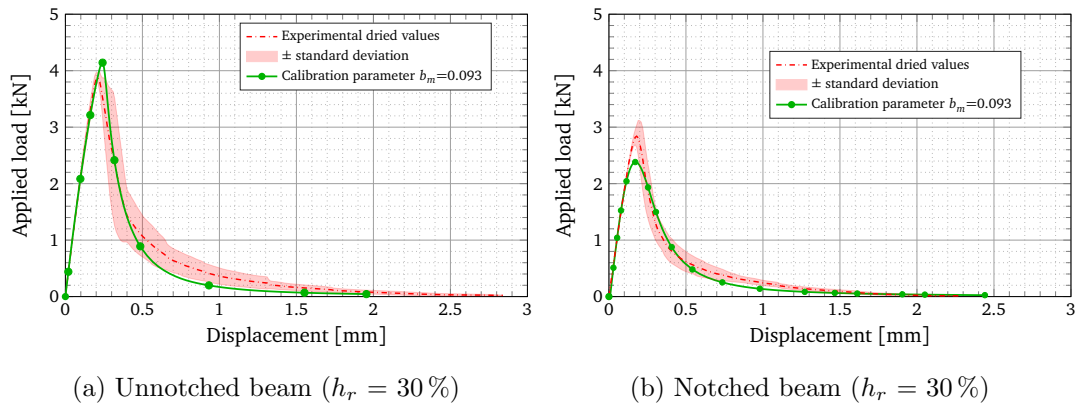


Figure 13: Prediction of the beam behaviour under three points bending load after 70 days of drying. Case of samples at $h_r = 30\%$.

366 Finally, the parameter b_m calibrated on mechanical results is more or less related
 367 to the Biot coefficient (b_w). The coefficient (b_w) is assumed to be equal to 0.24 for
 368 a fully hydrated concrete and may rise to 0.32 for a material hydrated at only 90 %
 369 according to the work of Souyris [77]. Even if the ultimate drying shrinkage value
 370 is a little bit overestimated (*c.f.* Fig. 4b) the identified value of $b_w = 0.30$ seems
 371 consistent. However, if this coefficient is taken into account to model the impact
 372 of capillary pressure on strength with the proposed formulation, the experimental
 373 curves are poorly reproduced (Fig. 14). Biot coefficient overestimates the peak force
 374 of the beams (respectively 39 % for unnotched beam and 52 % for notched beam in
 375 the case of $h_r = 45\%$ and 42 % in the case of $h_r = 30\%$). The Biot parameter, which
 376 induces shrinkage, seems to be more related to macroscopic behaviour whereas the
 377 value identified (b_m), three times smaller, seems to be more related to local behaviour
 378 of the material. Thus this parameter is related to the damage variable [78] as well as
 379 the saturation degree. This statement is also highlighted by the difference between
 380 notched beams respectively at $h_r = 45\%$ and $h_r = 30\%$ (Fig. 14b and 14c). The
 381 experimental behaviour is almost similar but the modelling predictions are rather
 382 different due to different damage state and hydric fields. This observation leads
 383 authors such as Sellier et al. [29] to implement models that rely on the state of

384 stresses to determine this parameter. Furthermore, in the proposed formulation,
 385 the equivalent pore pressure (π in part 2.3) does not consider the effects associated
 386 to disjoining pressure and surface tension which are predominant in fine pore [78, 79].

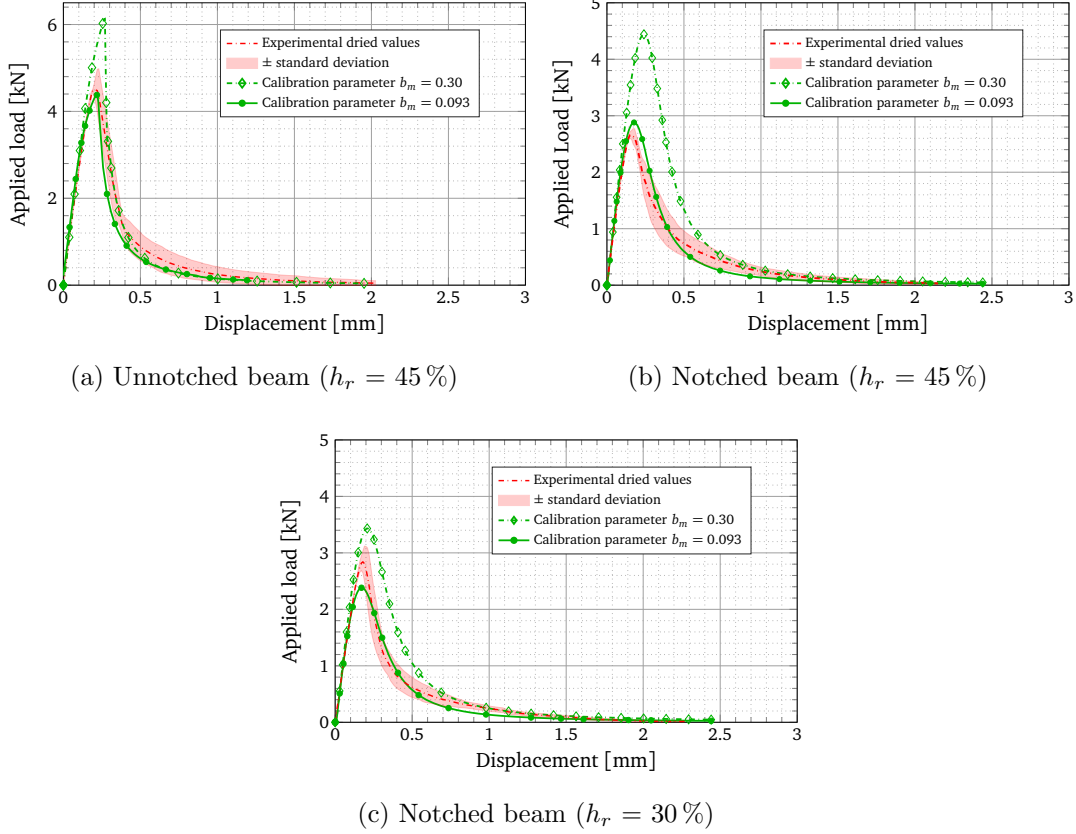


Figure 14: Influence of b_m parameter on mechanical behaviour

387 4. Conclusion

388 The main goal of this study was to extend a Chemo-Thermo-Hygro-Mechanical
 389 model previously initiated by Benboudjema and Torrenti [23] continued by Briffaut
 390 et al. [30], completed by Hilaire et al. [31] and improved with the incorporation of
 391 the effect pore pressure on mechanical strength on a poromechanical framework.
 392 This multi-physics model was reduced at the only hygro-mechanical part in order to
 393 model the impact of drying on cracking and mechanical behaviour of a mature con-
 394 crete in an isothermal conditions. To pursuit this goal, a sequential analysis has been
 395 developed. It was composed of a first stage of modelling drying transfers through

396 a diffusion equation that considered liquid permeation and water vapour diffusion
397 terms. It was followed by a modelling of the drying shrinkage by a poromechanical
398 model with capillary pressure as a driving force. The basic creep strains were mod-
399 elled by a simple four parameters rheological model and the additional strains due
400 to drying creep were modelled. The mechanical model used was a classic damage
401 model regulated in cracking energy. This mechanical model was thus modified to
402 account for the impact of capillary pressure on strength. The model was applied on
403 two different cases. One on a prism subjected to 400 days of drying and a second on
404 beams kept in dry atmosphere and then brought to failure by mechanical loading.
405 The following conclusions can be drawn from the work presented:

- 406 • To model the impact of drying on mechanical behaviour, the modelling of only
407 drying shrinkage was not sufficient. This led to a significant underestimation
408 of the residual mechanical behaviour (around 50 % in the case of 45 % relative
409 humidity). Stress relaxation induced by creep was necessary and it reduced
410 the underestimation to 25 %. Finally, the consideration of capillary pressure
411 in the mechanical behaviour allowed to approach the real behaviour of the
412 material with a weaker cracking induced shrinkage.
- 413 • The prediction of the behaviour of bending specimens under mechanical load-
414 ing after drying was consistent with the experimental behaviour. Thus by
415 calibrating the parameters on one of the tests it was possible to simulate the
416 other experimental tests in a suitable way, keeping the same set of parameters.
- 417 • In the proposed modelling, capillary pressure was used as a driving force to
418 model drying shrinkage. It also allows to model the positive effect of cap-
419 illary suction. Thus, to account for the drying shrinkage and the positive
420 effect induced by capillary suction, the use of the same Biot coefficient, was
421 not possible. In our modelling two different values were used (respectively
422 $b_w = 0.30$ and $b_m = 0.093$) to avoid an overestimation of 45 % in average of
423 the mechanical behaviour.

424 In an attempt to extend this study, some prospects of improvement can be inves-
425 tigated. Among these, the heterogeneity of the material is an important factor. It

426 will be interesting to model heterogeneities to consider the interfacial transition zone
427 (ITZ). This area known to show cracking due to strains incompatibilities between
428 the components, will change the macroscopic mechanical behaviour of the material.
429 So far, this phenomenon is not modelled. In addition, cracking is a discontinuous
430 phenomenon and the modelling presented here is continuous. This approach can
431 be questioned by comparing it to a discrete model. Work in this direction is cur-
432 rently ongoing. They will provide more realistic cracking surfaces. Finally, the last
433 point to investigate concerns the Biot coefficient. It would be interesting to see how
434 the latter evolves according to parameters such as geometry, the severity of drying
435 conditions or mechanical loading and damage.

436 **Appendix A. Mix proportion**

437 To maximise the effect of drying shrinkage, the material used in this study is
438 an ordinary concrete with a high water to cement ratio ($w/c= 0.62$). The mix pro-
439 portions of each component are given in the table A.5. The concrete is made with
440 Ordinary Portland Cement and a plasticizer is added. Aggregates are limestone ag-
441 gregates. Its usual mechanical characteristics are: an average compressive strength
442 at 28 days of 40.6 MPa and a tensile strength obtained by splitting test of 3.5 MPa
443 both on $16 \times 32 \text{ cm}^2$ cylinders.

Coumpound	Nature	Quantity	Unit
Cement	CEM I 52,5 R	320	kg m^{-3}
Sand	Siliceous	830	kg m^{-3}
Aggregate (4-11mm)	Limestone	445	kg m^{-3}
Aggregate (8-16mm)	Limestone	550	kg m^{-3}
Water (total)		197.6	kg m^{-3}
Plasticizer	SIKAPLAST Techno 80	2.75	kg m^{-3}

Table A.5: Concrete mix parameters

444 **Appendix B. Numerical flow chart**

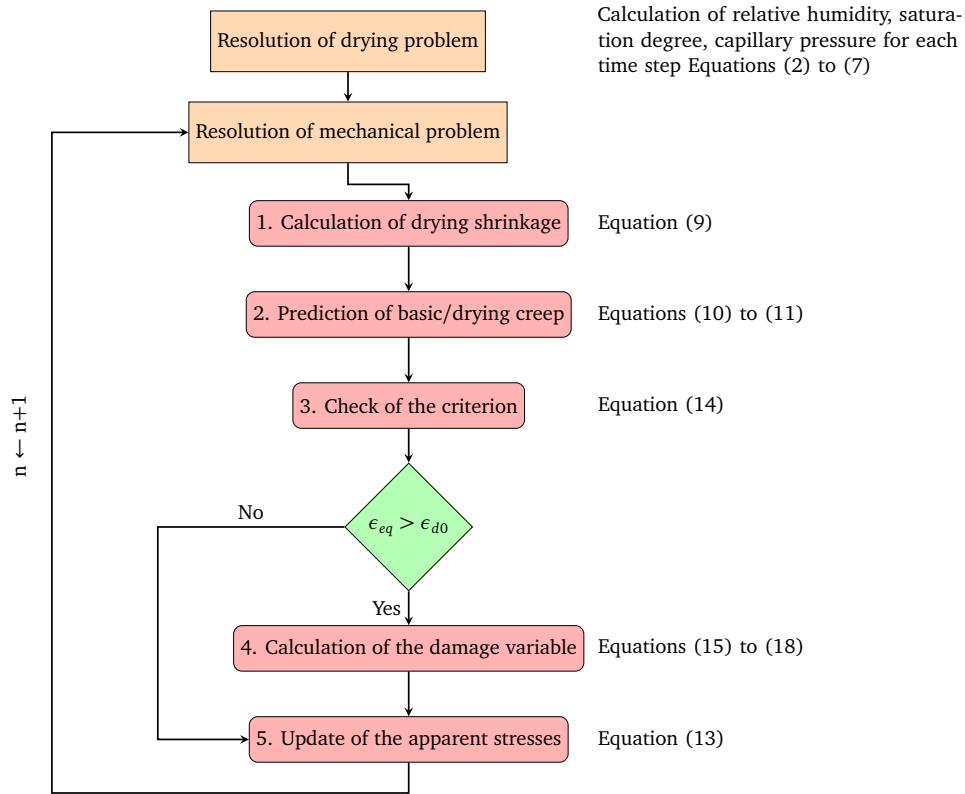


Figure B.15: Flow chart of the hygro-mechanical model

445 Appendix C. Creep data

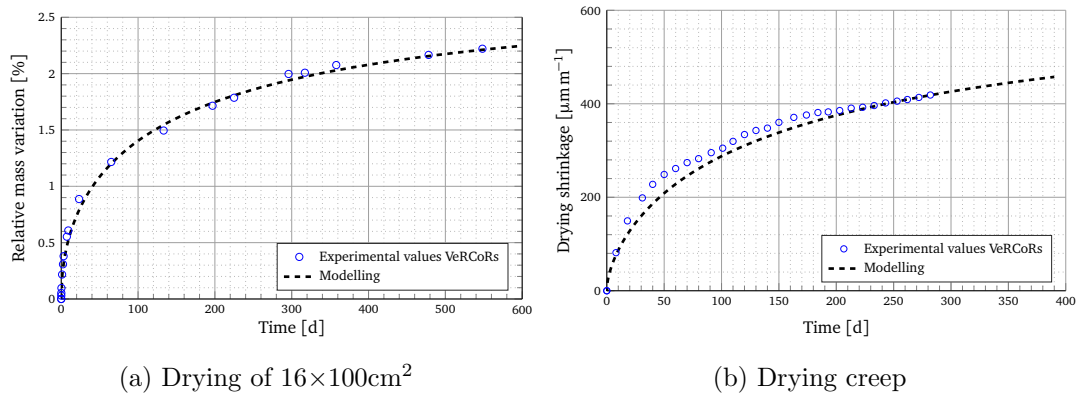


Figure C.16: Drying and drying shrinkage from VeRCoRs project

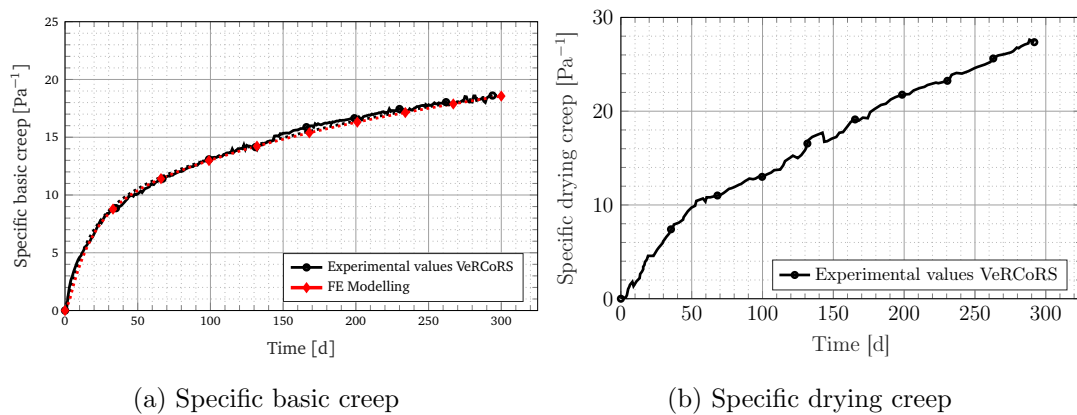


Figure C.17: Creep data from VeRCoRS project

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