Modeling Long-term Delayed Strains of Prestressed Concrete with Real Temperature and Relative Humidity History

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Abstract
The prediction of delayed strains is very important in prestressed concrete structures. The temperature and relative humidity of concrete structures are two important parameters for this prediction, and they vary over time on site. Based on model code 2010 and the superposition principle, the authors propose an analytical method that takes account of such variations in temperature and relative humidity. The coupling between delayed strains in concrete and stress relaxation in prestressing bars is also considered. The proposed method is validated with respect to in-situ measurements on the VeRCoRs mock-up, which is a 1/3 scale mock-up of a biaxially prestressed confining structure. At the end of the paper, the authors discuss the importance of taking into account the coupling between delayed strains and stress relaxation, the influence of variations in temperature on the delayed strains of concrete and the value of Poisson’s ratio for drying creep.

1. Introduction

It is well known that concrete structures continue to undergo deformation long after construction. This kind of long-term delayed behavior is of great importance for the safety evaluation and life span assessment of massive civil engineering structures. For instance, in the French nuclear power plants (NPPs), the containment buildings are biaxially prestressed concrete structures, where the prestressing is necessary for safety in case of an accident (Charpin et al. 2018). How the delayed strains of concrete evolve and how the prestressing relaxes over time is crucial for evaluating the safety and life span of these containment buildings.

Other factors in addition to the applied load influence delayed strains in concrete, for example the temperature and relative humidity. Under isothermal conditions, considering loading conditions and relative humidity, the delayed strains of concrete are conventionally decomposed into four components: basic shrinkage (also known as autogenous shrinkage), which is the delayed strain in a load-free specimen which does not exchange any water with its environment; drying shrinkage, which is the difference between the delayed strains of a load-free specimen that exchanges water with its environment; basic creep, which is the difference between the delayed strains of a loaded specimen that exchanges water with its environment and the sum of its basic shrinkage, basic creep and drying shrinkage. Most design codes (EN 2004; ACI 2008; fib 2012) and the majority of academic models described in the literature (Bazant 2008; Granger 2016; Aili et al. 2016; Sellier et al. 2016; Mathieu et al. 2018), to mention a few, propose time-evolution laws for these four components under isothermal conditions. It should be kept in mind that this classical decomposition of delayed strains cannot take into account any possible couplings between different components. For instance, the basic shrinkage would be different in sealed sample and sample submitted to drying. Drying creep and drying shrinkage are observed to be proportional to each other in the study of Gamble and Parrott (1978) and in the RILEM draft recommendation TC-242-MDC (RILEM 2015), suggesting some correlation between them. Models without the classical decomposition have been proposed (Sellier et al. 2016; Aili et al. 2020). However, with the challenge that consists in an industrial context to work with imperfect data and within the framework of design codes with the objective of simulating industrial context such as lack of data and restriction within models of design codes, the authors follow a classical decomposition of delayed strains in this study.

In order to better study and predict delayed strains and the loss of prestress, the company operating the French nuclear power plants, Electricité de France (EDF) has built a 1/3 scale mock-up of a containment building. This mock-up is named VeRCoRs, which is an abbreviation of “VErification Réaliste du COntainment des RéacteurS” (i.e., realistic verification of reactor containment) (Corbin and Garcia 2016; Mathieu et al. 2018). It is highly instrumented and its behavior is monitored from the beginning of the construction process (Oukhemanou et al. 2016). In-situ measurements showed that the temperature in the concrete structure...
varied over time. The relative humidity was around 80% before the top of the mock-up was covered and then remained at around 50%. These variations in temperature and relative humidity influence the delayed strains of the concrete (Bazant et al. 1976, 1995; Neville et al. 1983; fib 2012). Hence, objective of this study is to evaluate creep and shrinkage of concrete under varying temperature and relative humidity.

The influence of temperature and relative humidity variation on creep and shrinkage has been studied in literature since several decades. Hansen et al. (1966) observed that change of temperature under load causes some extra deformation. Fahmi et al. (1972) studied the influence of sustained and cyclic high temperature on creep strain. These authors have shown that a load-induced thermal strain (LITS) occurs but only during the first heating of a loaded concrete to a given temperature (Torelli et al. 2016). LITS could be explained by a coupling between a thermal damage and creep (Torrenti 2018). Bazant et al. (1976) performed creep experiments on thin hollow cylindrical sample under variable relative humidity and later proposed creep law under variable relative humidity (Bazant et al. 1975; Bazant and Chern 1985). Their model was in line with the micro-prestress solidification theory of creep (Bažant et al. 2004) and was updated recently to take into account new experimental evidence and findings in molecular dynamics (Rahimi-Aghdam et al. 2019). The effect of temperature increase on creep under sealed condition were measured and simulated by Ladaoui et al. (2011), Torrenti (2018) and Manzoni et al. (2020) by considering thermal activation energy. Shrinkage under variable cyclic relative humidity were measured and studied by Torrenti et al. (1997). Vandewalle (2000) and Rao et al. (2015) measured creep and shrinkage under variable temperature and relative humidity cycle. Cagnon et al. (2015) measured creep under variable relative humidity cycle and found that drying creep could be recovered upon wetting. Seasonal variation of relative humidity and temperature was considered by Li and He (2018) on creep of concrete.

More realistic simulation of temperature and relative humidity, including rainfall based on meteorological data acquisition were considered in other studies (Shimomura 2015; Watanabe and Ohno 2015). As for structural level, Ozaki et al. (1995) and Chijiwa et al. (2018) simulated long-term behavior of prestressed concrete containers vessels while considering variable temperature and relative humidity. In the above mentioned literature, the concept of equivalent time was used considering thermal activation of water viscosity. This study is aimed at evaluating how variations in relative humidity and temperature affect the long-term delayed strains of concrete, based on fib model code MC2010 (fib 2012) and the work of Müller et al. (2013). The coupling between the relaxation of the prestressed bars and the delayed strains of concrete was also taken into account. The LITS that occurs only during the first heating phase, however, is neglected.

This paper begins by presenting the MC2010 equations for the delayed strains of concrete and relaxation of steel, after which the influence of temperature on shrinkage based on the activation energy is considered. The superposition principle is applied to take account of the impact of variations in relative humidity and temperature on creep of concrete and the relaxation of steel. Calibration of the shrinkage and creep models based on laboratory tests that were performed to characterize the material used in the mock-up is then carried out before predicting the delayed behavior of the mock-up and comparing this with on-site measurements. The paper ends with a discussion of the significance of the impact on the simulation results of coupling between the delayed strains of concrete and the relaxation of steel, of variations in temperature and Poisson’s ratio for drying creep.

2. MC2010 equations for isothermal conditions

This section begins by presenting the MC2010 equations for concrete shrinkage and creep and then describes the equation for the relaxation of prestressing steel.

2.1 Concrete shrinkage and creep

As seen in the introduction, the delayed strain $\varepsilon'$ of concrete can be decomposed into basic shrinkage $\varepsilon^{bs}$, drying shrinkage $\varepsilon^{dr}$, basic creep $\varepsilon^{bc}$ and drying creep $\varepsilon^{dc}$:

$$\varepsilon' = \varepsilon^{bs} + \varepsilon^{dr} + \varepsilon^{bc} + \varepsilon^{dc}$$

(1)

Basic shrinkage $\varepsilon^{bs}$ and drying shrinkage $\varepsilon^{dr}$ are given as a function of mean compressive strength $f_{cm}$ and drying time $t-t_s$ by the following equations:

$$\varepsilon^{bs} = \varepsilon^{bs}_{fcm}(0.1f_{cm}^{0.016} + 6 + 0.1f_{cm})(1 - e^{-0.02h_0/2})$$

(2)

$$\varepsilon^{dr} = \varepsilon_{h_0}^{dr}(20 + 110\alpha_{a1}d) e^{-\alpha_{a2}d/2} \beta^{h_0/2} \frac{(t-t_s)}{0.035 h_0 h^2 + (t-t_s)_{t1}}$$

(3)

where $\alpha_{a1}$, $\alpha_{a2}$, $h_0$ are parameters that depend on the type of cement, $h$ is the notional size (equal to $A_s / 2p$, where $A_s$ is the cross section and $p$ is the perimeter) in millimeters; $t_s$ is the age of the concrete in days at the start of drying; $\varepsilon^{bs}_{fcm}$, $\varepsilon^{dr}_{h_0}$, $\varepsilon^{'bs}$ and $\varepsilon^{'dr}$ are parameters that were not included in the MC2010 equations and that have been added to adjust the predictions to our experimental results (the default values are equal to 1). $\beta^{h_0/2}$ is a function of the relative humidity $RH$:

$$\beta^{h_0/2} = 1.55 \left[ 1 - \left( \frac{RH}{RH_{eq}} \right)^{h_0/2} \right]$$

(4)
where $RH_{eq} = 72[-0.046(f_{cm} - 8)] + 75$.

For basic creep and drying creep, the time evolution is expressed by creep coefficients, i.e., $\varepsilon^c(t, t_0) = \sigma^c_0, \phi^c(t, t_0)/E$ and $\varepsilon^d(t, t_0) = \sigma^d_0, \phi^d(t, t_0)/E$, where $\sigma^c_0$ is the applied constant stress, $t_0$ is the age of the concrete at loading and $E$ is the modulus of elasticity of concrete. The basic creep function reads:

$$\phi^c(t, t_0) = \frac{0.8}{(f_{cm})^{0.7}} \ln \left( 1 + \frac{30}{(t_0,adj) + 0.035^2} \right)$$

where $t_{0,adj}$ is the adjusted age at loading of the concrete on the basis of the type of cement and the curing temperature; $\xi_{c1}$ and $\xi_{c2}$ are parameters that may be adjusted according to the experimental results.

The drying creep function reads:

$$\phi^d(t, t_0) = \frac{1}{\xi_{d1}} \beta^d_{c, c}(f_{cm}, RH, t_0) \beta^d_{c, c, c, c}$$

where

$$\beta^d_{c, c}(f_{cm}, RH, t_0) = \frac{412}{(f_{cm})^{0.4}} \left[ \frac{1 - \frac{100}{RH}}{0.1 + \left(\frac{t_0,adj}{100}\right)^{0.2}} \right]$$

$$\beta^d_{c, c, c, c} = \left[ \frac{t - t_0}{\xi_{d2}} \beta^d_{c} + t - t_0 \right]^{0.5}$$

$$\gamma(t_0) = \frac{1}{2.3 + 3.5/\sqrt{t_0,adj}}$$

$$\beta^d_{c} = \min\left\{ 1.5h + 250 \left( \frac{35}{f_{cm}} \right)^{0.5}, 1500 \left( \frac{35}{f_{cm}} \right)^{0.5} \right\}$$

$\xi_{d1}$ and $\xi_{d2}$ are parameters that may be adjusted according to the experimental results (the default values are equal to 1).

2.2 Relaxation of steel

The relaxation kinetics of prestressing in reinforcing steel is expressed by the time evolution of relaxation loss $\rho(t) = (\sigma_0 - \sigma(t))/\sigma_0$, where $\sigma_0$ and $\sigma(t)$ are the stress at the time of prestressing and at time $t$, respectively. If the relaxation loss after 100 hours and after 1000 hours is denoted as $\rho_{00}$ and $\rho_{00}$, respectively, the time evolution of relaxation loss is given by:

$$\rho(t) = \rho_{00} \left( \frac{t}{1000} \right)^k$$

where the parameter $k = \log(\rho_{00}/\rho_{00})$. Supposing that elastic modulus of steel is constant, the stress evolution under a constant strain $\varepsilon_0$ reads as $\sigma(t) = E, \varepsilon_0 (1 - \rho(t))$. With Boltzmann superposition principle, this expression is generalized to the case of varying strain $\varepsilon(t)$ (Christensen 1982):

$$\sigma(t) = E \int_{-\infty}^{t} \varepsilon(t) \frac{d(1 - \rho(t - \tau))}{d \tau} d \tau$$

2.3 Calibration of models based on laboratory tests

To characterize the concrete used in the VeRCoRs mock-up, EDF performed laboratory shrinkage and creep tests on cylindrical specimens with a diameter of 16 cm and a height of 100 cm (Charpin and Haelewyn 2019). Delayed strains were measured under four different conditions: a sealed load-free specimen, a drying load-free specimen, a sealed and loaded specimen and a drying loaded specimen. The strains were measured on the center part of the specimen (on a 50 cm basis), as a mean of three contact LVDT, one sample for each test, using the experimental setup described in (Le Roy et al. 2017). Drying was conducted with a relative humidity of 50%. Drying of load-free specimen was started at the age of 1 day. The delayed strains of the sealed load-free specimen and the drying load-free specimen were measured from the age of 1 day. The applied load consisted of a uniaxial load of 12 MPa. Both loaded specimens were kept sealed until the age of 91 days. Loading was then applied, simultaneously drying was started. The tests were conducted at a temperature of 20°C. Based on the cement type (strength class 52.5 R), the adjusted age $t_{0,adj}$ was taken as 94 days.

The four components of delayed strains were computed from the measured strain on these four specimens. Eqs. (2), (3), (5) and (6) were then fitted for basic shrinkage, drying shrinkage, basic creep and drying creep, respectively. The input parameters are given in Table 1 and the fitting parameters are set out in Table 2. Figures 1 and 2 show the experimental measurements and calibration results for shrinkage and creep, respectively.

Relaxation tests were performed at IFSTTAR on the rebars at 20°C and 40°C (Toumi Ajimi et al. 2016). Based on relaxation test results at 20°C, it was found that $\rho_{00} = 0.551$. Fitting Eq. (10), one obtains the best fit for $\rho_{00} = 0.893$, as displayed in Fig. 3. The influence of temperature is discussed in Section 3.1.

3. Influence of ambient temperature and relative humidity

In contrast with the models and the laboratory experi-
ments conducted under isothermal condition, the temperature and relative humidity in the VeRCoRs mock-up were not constant but varied over time. This section presents how variations in temperature and relative humidity were taken into account.

The temperature history displayed in Fig. 5 was measured close to the two embedded strain sensors H5 and H6 at mid-height of the VeRCoRs mock-up. These sensors were located far away from unusual features like the hatch in the containment. H5 was placed 5 cm away from the external surface of the inner containment and H6 was 10 cm away from the external surface. A simplified temperature history was estimated from the average values of the measured temperature near H5 and H6. The temperature difference between H5 and H6 indicated the existence of a spatial variation of temperature on the mock-up. However, the influence of spatial variation of temperature was neglected in this study, considering that the duration during which temperature gradient existed is shorter than the whole simulation time. The relative humidity was measured using a relative humidity sensor inside the containment and in the annular space. Before the cover was placed on the mock-up, the relative humidity was 80%. At the age of almost 1 year, the relative humidity had dropped to 50% and remained constant for the rest of the test.

3.1 Influence of temperature

This section presents the influence of temperature on the creep and relaxation models in the cases with constant temperature. As far as shrinkage is concerned, it is known that the kinetics of (drying) shrinkage is influenced by temperature. MC2010 can take into account these effects but is limited to cases with constant temperature. Considering the difficulty for cases with varying temperature, the influence of temperature is neglected in this study for the sake of simplicity.

At first, Eq. (5) of basic creep kinetics is re-written in the form of Eq. (13), with creep modulus $C$, which is defined as the asymptotic value of $\frac{\sigma(t)}{\varepsilon(t)}$ in a creep test performed under stress $\sigma_0$ (Vandamme and Ulm 2009) as below:

$$\varphi_{bc}(t, t_0) = \frac{\xi}{C} \ln \left(1 + \frac{t - t_0}{\tau(t)} \right)$$

(13)
BaZant and Li (2008), Sellier et al. (2016) and Torrenti (2018) postulated that the creep modulus is affected by thermal activation. In this case, the creep modulus $C$ in Eq. (13) is considered to be dependent on temperature as follows:

$$C(T) = C \exp \left( \frac{Q}{T} - \frac{1}{293} \right)$$  \hspace{1cm}(14)$$

where $Q$ is the activation energy of water viscosity. Implementing this temperature dependency in the framework of superposition principle in Section 3.3, influence of varying temperature can be taken into account.

For cases with constant temperature, the influence of temperature on drying creep was considered based on MC2010. A temperature dependent coefficient $\beta(T) = \exp \left( \frac{1500}{273 + T} - 5.12 \right)$ was added to the term for the kinetics of drying creep. Eq. (8) thus became:

$$\beta_{d,c,t} = \frac{t - t_0}{\xi_{d,c} \beta_{c} + t - t_0}$$ \hspace{1cm}(15)$$

The amplitude is also modified by a temperature change, by multiplying the temperature dependent term $\varphi_{d,c}(T) = (\exp(0.015(T - 20)))^{1/2}$. Hence, the drying creep model of Eq. (6) becomes:

$$\varphi_{d,c}(t, t_0, T) = \xi_{d,c} \beta_{c}/(f_{cm}, RH, t_0) \beta_{d,c,t} \varphi_{d,c}(T)$$ \hspace{1cm}(16)$$

Then, this drying creep coefficient will be inserted into superposition principle in Section 3.3 to extend for the case with varying temperature.

The relaxation of steel is also influenced by temperature. In the fib model code MC2010 (fib 2012), under isothermal condition for temperature other than 20°C, the relaxation loss is obtained by multiplying the stress loss at 20°C by the following temperature-dependent parameter:

$$\alpha_f(T) = \frac{T}{20^\circ C}$$ \hspace{1cm}(17)$$

Equation (17) above was checked by comparing the relaxation measurement of rebars at 40°C in (Toumi et al. 2016) with the relaxation law in Eq. (11) multiplied by $\alpha_f(40^\circ C) = 2$. As can be seen in Fig. 3, there is a misfit between measured value and model prediction of stress loss at 40°C. The model overestimates stress loss at long term. Consequence of this overestimation will be discussed in Section 5.1.

### 3.2 Influence of relative humidity

The influence of relative humidity on drying creep is taken into account by the factor $\beta_{rh}$ in Eq. (7). To consider variations in relative humidity, the superposition principle, which will be described in Section 3.3, is applied.

For drying shrinkage, the influence of relative humidity is taken into account by the factor $\beta_{rh}$ in Eq. (4). To consider variations in relative humidity over time, the concept of equivalent time is applied. Based on the history of relative humidity in Fig. 5, the time when the relative humidity drops from 80% to 50% is denoted by $t_d$. Before this time, based on Eq. (3), the drying shrinkage is expressed as follows:

$$\varepsilon_{dc}^{t} = \varepsilon_{dc}^{0} (t - t_0, RH = 80\%), \quad for \quad t \leq t_d \hspace{1cm}(18)$$

After time $t_d$, the drying shrinkage should follow Eq. (3) with $RH = 50\%$. For the time of drying, an equivalent time $t_{eq}$ is computed, after which the same drying shrinkage at time $t_d$ with a relative humidity of 50% would be obtained, i.e.,

$$\varepsilon_{dc}^{t_{eq}, RH = 50\%} = \varepsilon_{dc}^{t_d - t_0, RH = 80\%}.$$ 

Then, for time $t > t_d$, the drying shrinkage is computed as follows:

$$\varepsilon_{dc}^{t} = \varepsilon_{dc}^{0} (t - t_0 - t_{eq}, RH = 50\%), \quad for \quad t \geq t_d \hspace{1cm}(19)$$

### 3.4 Superposition principle

Creep of concrete, like other properties of concrete, depends on its age (Mehta and Monteiro 2006; Grasley and Lange 2007), which is also indicated in Eq. (13). For loading level less than 40% of the strength, assuming that time-dependent viscoelastic behavior of concrete is linear (Neville et al. 1983), the response to a sum of two stress histories can be then considered to be equal to the sum of the responses to each of them taken separately, from which the Boltzmann superposition principle is derived (Christensen 1982). Hence, the superposition principle is particularly useful for modeling the delayed strains in the VeRCoRs mock-up for the two following reasons: firstly, in the VeRCoRs mock-up, the stresses in the concrete are not constant as the stress in the prestressed cables relaxes over time; secondly, in order to take account of the influence of variations in temperature and relative humidity, the stress history is decomposed into the sum of $n$ stress pulses as in other studies (Bazant and Oh 1983; Walraven and Shen 1991). It is then assumed that the temperature and relative humidity remain constant during each pulse.

In view of the fact that the VeRCoRs mock-up is bi-axially stressed, Poisson’s effects are considered when the superposition principle is applied. This section explains the steps that are required to apply this principle to vertical basic creep $\varepsilon_{v,b}$ and vertical drying creep $\varepsilon_{v,d}$ under vertical stress $\sigma_{v,b}$, and the Poisson’s effect of horizontal stress $\nu_v \sigma_{v,b}$ or $\nu_v \sigma_{v,d}$ ($\nu_v$ and $\nu_v$ are the Poisson’s ratio of the basic creep of and drying creep, respectively). The procedure for applying this principle is exactly the same for the other components, i.e., horizontal basic creep $\varepsilon_{h,b}$ and horizontal drying creep $\varepsilon_{h,d}$.

First, the time is divided into $n$ time steps. At time $t_i$, which is the beginning of time step $i$, the concrete is
Table 3 Cross-section, stress values and prestressing day.

| Steel rebar direction | Cross-section [m²/m²] | Stress [MPa] | Prestressing day |
|-----------------------|-----------------------|--------------|------------------|
| Horizontal            | 0.0104                | 10.6         | 17/06/2015       |
| Vertical              | 0.0047                | 6.3          | 02/07/2015       |

loaded with the vertical stress \( \sigma_{\nu}^{\text{pre}} \) at time \( t_i + dt_i \), the end of time step \( i \), the vertical stress \( \sigma_{\nu}^{\text{pre}} \) is removed.

Applying the principle of superposition, the basic creep strain \( \varepsilon^{\text{cr},\nu}_i \) at time \( t_i \) is obtained thus:

\[
\varepsilon^{\text{cr},\nu}_i = 0
\]

\[
\varepsilon^{\text{cr},\nu}_i = (\sigma_0^{\text{cr},\nu} - v_i \sigma_0^{\text{pre},\nu}) \cdot \varphi_0(t_i, t_0, T(t_0))/E_c
\]

\[
\varepsilon^{\text{cr},\nu}_i = (\sigma_0^{\text{cr},\nu} - v_i \sigma_0^{\text{pre},\nu}) \cdot \varphi_0(t_i, t_0, T(t_0))/E_c
\]

\[
\frac{+ \sum_{j=0}^{i-1} (\sigma^{\text{cr},\nu}_j - v_i \sigma^{\text{pre},\nu}_j) \cdot (\varphi_0(t_{i+1}, T(t_j)) - \varphi_0(t_{i+1}, T(t_0))/E_c}
\]

where \( E_c \) is the Young’s modulus of concrete. For drying creep, \( \varphi_0 \) and \( v_i \) are replaced by \( \varphi_d \) and \( v_d \), respectively:

\[
\varepsilon^{\text{cr},\nu}_i = 0
\]

\[
\varepsilon^{\text{cr},\nu}_i = (\sigma_0^{\text{cr},\nu} - v_i \sigma_0^{\text{pre},\nu}) \cdot \varphi_d(t_i, t_0, RH(t_0))/E_c
\]

\[
\varepsilon^{\text{cr},\nu}_i = (\sigma_0^{\text{cr},\nu} - v_i \sigma_0^{\text{pre},\nu}) \cdot \varphi_d(t_i, t_0, RH(t_0))/E_c
\]

\[
\frac{+ \sum_{j=0}^{i-1} (\sigma^{\text{cr},\nu}_j - v_i \sigma^{\text{pre},\nu}_j) \cdot (\varphi_d(t_{i+1}, T(t_j)) - \varphi_d(t_{i+1}, RH(t_0))/E_c}
\]

The Poisson’s ratio for basic creep is considered to be equal to 0.2 (Aili et al. 2015, 2016), and it is assumed that the Poisson’s ratio for drying creep is 0 (Charpin et al. 2017). Other possible values for the Poisson’s ratio for the drying creep will be discussed in Section 5.3. It should be noted that the temperature in Eqs. (20) and (21) is evaluated at time of loading (for each pulse) according to the proposition of (Walraven and Shen 1991). However this is not proved and may not be applicable to all cases. Another option would be to assume that concrete behaves like a rheologically simple material, as observed by Christensen (1982, Section 3.6).

With regard to the relaxation of the prestressing cable, since the initial applied strain is not kept constant due to coupling with concrete strain, it is necessary to consider relaxation under variable strain. As the horizontal stress is around 50% of the tensile stress, the horizontal relaxation is ignored. Applying a similar procedure to that for concrete creep, the time is first divided into \( n \) time steps. At time \( t_i \), which is the beginning of time step \( i \), a vertical strain \( \varepsilon^{\text{cr},\nu}_i \) is applied to the cable, which is removed at time \( t_i + dt_i \), the end of time step \( i \). By applying the principle of superposition, the stress \( \sigma^{\text{cr},\nu}_i \) at time \( t_i \) is obtained thus:

\[
\sigma^{\text{cr},\nu}_i = \sigma_0^{\text{cr},\nu} E_c
\]

\[
\sigma^{\text{cr},\nu}_i = \sigma_0^{\text{cr},\nu} E_c \cdot (1 - \alpha(T_i) \cdot \rho(t_i, t_0))
\]

\[
\sigma^{\text{cr},\nu}_i = E_c \cdot (1 - \alpha(T_i) \cdot \rho(t_i, t_0))
\]

\[
\left(+ \sum_{j=0}^{i-1} E_c \cdot (1 - \alpha(T_j) \cdot \rho(t_{i+1}, t_j) - (1 - \alpha(T_j)) \cdot \rho(t_{i+1}, t_j))\right)
\]

where \( E_c \) is the Young’s modulus of steel rebars.

4. Application to the benchmark problem

This section deals with simulation of the time evolution of the delayed strains of the concrete and the relaxation of stress in the prestressing steel of the VeRCoRs mock-up. A small unit of material in a part of the structure without unusual features (like a hatch) as shown in Fig. 6 is considered in order to compare with strain measurements of sensors H5 and H6 shown in Fig. 4. These H5 and H6 sensors are vibrating wires measuring both horizontal (tangential) and vertical deformations, and are associated with PT100 thermometers. No structural effect is considered. However, the fact that horizontal prestress was applied two weeks before the vertical prestress is duly accounted for. For the sake of simplicity, it is assumed that prestressing was applied instantaneously. The age of concrete corresponding to the horizontal loading was assumed to be equal to 91 days. The cross-sections of the prestressing steel, the prestress value and the date at which prestress was applied are listed in Table 3.

In what follows, the calculation steps are presented at first. This is followed by the comparison of the simulation results with on-site measurements.
4.1 Calculation steps to take into account of coupling between the delayed strains of the concrete and the prestress relaxation

Prior to considering the coupling between the delayed strains of concrete and the relaxation of steel rebars, the time evolution of basic shrinkage $\varepsilon^{bs} (t)$ [Eq. (2)] and of drying shrinkage $\varepsilon^{ds} (t)$ [Eqs. (18) and (19)], is computed. Based on the simplified temperature history $T(t)$ in Fig. 5, and applying the thermal expansion coefficient $\alpha = 10 \mu^\circ C$, the thermal strain of concrete is expressed thus:

$$\varepsilon^{th} (t) = \alpha (T(t) - 20)$$

(23)

To assess the time evolution of concrete strain and the relaxation of steel rebars, the entire time period was divided into $n$ steps of equal length $\Delta t$. The strain and stress values given for time step $i$ are those that were measured at the start of that time step. Hence, the delayed strains components and relaxation at step $0$ are all equal to 0. Then, at the end of step $i$, based on the vertical stress $\sigma_i^{v,v}$ and strain $\varepsilon_i^{v,v}$ and the horizontal stress $\sigma_i^{h,h}$ and strain $\varepsilon_i^{h,h}$ in the concrete; the stress $\sigma_i^{v,v}$ and strain $\varepsilon_i^{v,v}$ in the vertical steel rebars; the stress $\sigma_i^{h,h}$ and strain $\varepsilon_i^{h,h}$ in the horizontal steel rebars, the following four sub-steps (calculation from step $i$ to $i+1$) were performed:

(a) The vertical strain $\varepsilon_i^{v,v}$ and horizontal strain $\varepsilon_i^{h,h}$ of the concrete was computed thus:

$$\varepsilon_i^{v,v} = \frac{\sigma_i^{v,v} - \nu \sigma_i^{h,h}}{E_c}$$

$$\varepsilon_i^{h,h} = \frac{\sigma_i^{h,h} - \nu \sigma_i^{v,v}}{E_c}$$

(24)

where $\nu$ is the elastic Poisson ratio (deemed to be equal to 0.2). The right-hand side of Eq. (24) represents the elastic strain, basic creep, drying creep, basic shrinkage, drying shrinkage and thermal strain, respectively. The basic creep and drying creep were calculated with Eqs. (20) and (21).

(b) The strain $\varepsilon_i^{v,v}$ in the vertical steel bars and that $\varepsilon_i^{h,h}$ in the horizontal steel bars was computed by considering the continuity of displacement:

$$\varepsilon_i^{v,v} = \varepsilon_i^{v,v} + (\varepsilon_i^{v,v} - \varepsilon_i^{v,v}) + \varepsilon_i^{h,h}$$

$$\varepsilon_i^{h,h} = \varepsilon_i^{h,h} + (\varepsilon_i^{h,h} - \varepsilon_i^{h,h}) + \varepsilon_i^{h,h}$$

(25)

(c) The stress $\sigma_i^{v,v}$ in the vertical steel bars and that $\sigma_i^{h,h}$ in the horizontal steel bars were calculated thus:

$$\sigma_i^{v,v} = E \varepsilon_i^{v,v} (1 - \alpha (T_i) \cdot \rho (t_i, t_i))$$

$$+ \sum_{j=0}^{i} E \varepsilon_j^{v,v} (1 - \alpha (T_j) \cdot \rho (t_j, t_i)) - (1 - \alpha (T_i) \cdot \rho (t_i, t_i) \cdot \rho (t_i, t_i + dt_i)))$$

$$\sigma_i^{h,h} = E \varepsilon_i^{h,h}$$

(26)

(d) The vertical stress $\sigma_i^{v,v}$ and the horizontal stress $\sigma_i^{h,h}$ in the concrete were calculated by considering the equilibrium of forces in the cross section:

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Fig. 4 Position of sensors in the mock-up. Top: Vertical section of the mock-up; Bottom: Horizontal section at height of 8 m, where the H5 and H6 sensors are located.
In this section, the simulation results are compared with the measurements from two embedded strain sensors at mid-height of the VeRCoRs mock-up, H5 and H6, shown in Fig. 4. Figures 7 and 8 display the horizontal and vertical strain measured by these strain gauges.

The simulations are performed from 6th of May 2015 to 31st March 2018 and are compared with the measurements from the two strain gauges in Figs. 7 and 8. In order to observe the delayed part of the vertical strain, 100 μm/m was added to the predicted vertical delayed strains in order to obtain the same elastic strain at loading (i.e., the day of vertical prestressing) as in Fig. 8. It is seen that both the value of strain and its rate of increase over time are well predicted by the model. The duration of simulated time of approximately 3 years seems short comparing to the age of NPPs in usual safety assessment. Since the thickness in this mock-up is at a scale of one third, it is believed that delayed behavior of concrete is 9 times faster than in a real NPP. Longer simulation will be compared when the in-situ measurement will be available in the future.

5. Discussion

The model is simplified in this section, firstly by decoupling delayed strains and relaxation, and secondly by ignoring the influence of temperature on delayed strains. The section ends with a discussion about the Poisson’s ratio value for drying creep strain.

5.1 Coupling between relaxation and delayed strains of concrete

In the previous section, it was considered that, following the stress relaxation that occurred in the steel bars over time, the stress in the concrete also decreased. The delayed strains of the concrete were thus viscoelastic strain under variable stress. In this section, the delayed strains of the concrete are estimated by ignoring the coupling between the relaxation of steel bars and the delayed strains of the concrete. These estimated delayed strains are the compared with the results from the model.

In order to ignore the coupling between relaxation of the steel bars and the delayed strains of the concrete, it is assumed that the stress in concrete remains constant over time after prestressing. The vertical basic and drying creep ε_{v0}^{bc} and ε_{v0}^{dc} and the horizontal basic and drying creep ε_{h0}^{bc} and ε_{h0}^{dc} are computed with Eqs. (20) and (21) by replacing all the values of σ_{v}^{bc} and σ_{h}^{bc} by their initial values σ_{v0}^{bc} and σ_{h0}^{bc}, respectively. The mean values (ε_{bc}^{h} + ε_{dc}^{h})/2 of basic creep and (ε_{bc}^{v} + ε_{dc}^{v})/2 of drying creep are compared with the results from the model in Figs. 9 and 10.

With regard to the relaxation of the steel, it has been assumed that the strain in the steel bar remained constant over time. Therefore, the stress σ_{v}^{v}(t) in the vertical steel bars was computed with Eq. (22) by replacing all the values of ε_{v0}^{v} by the initial value ε_{v0}^{v}. The stress σ_{h}^{v}(t) in the horizontal steel bars remains constant over time. This stress history is compared with the results from the model in Figs. 11 and 12.

We can see that by ignoring the coupling between the relaxation of prestressing and the delayed strains of concrete, three years after prestressing the mean basic creep and mean drying creep were overestimated by 33 μm/m and 30 μm/m respectively, compared to the results from the model. For the stress in the steel bars, the overestimations were approximately 14% (156 MPa) and 17% (145 MPa) respectively. The outcome is a slight overestimation of the delayed strains.

When considering the effect of temperature on relaxation of steel bar at 40°C, Eq. (17) of MC2010 overesti-
mated the stress loss at long term as displayed in Fig. 3. From the discussion in this section, it may be inferred that stress in steel bar is underestimated, meaning assessment of prestress on the safe side.

5.2 Influence of temperature on delayed strain
The influence of temperature on the delayed strains in the concrete and the stress relaxation in the steel bars was determined on the basis of isothermal tests performed at different temperatures. The model takes account of temperature variations over time by applying the superposition principle. This section discusses how significant the impact on delayed strains would be if the influence of temperature variations is ignored.

In order to ignore the influence of temperature variations on basic creep and drying creep, the temperature was assumed to be constant and equal to 20°C in Eqs. (20) and (21). Expressed in other terms, the creep modulus in Eq. (14) remained constant. As far as drying creep is concerned, both parameter $\beta_\eta$ and $\phi_{\beta, T}$, in Eq. (15) were computed with a temperature of 20°C. In the relaxation model for steel, constant temperature meant the parameter $\alpha_\eta$ in Eq. (17) is constant and equal to 1. However, the temperature history displayed in Fig. 5 was taken into account to compute the thermal stress over time.

The mean basic creep, mean drying creep and stress in steel bars are compared with the simulation from the model in Figs. 9 and 10. When the influence of temperature variations on the creep of concrete and the relaxation of steel is ignored, as shown in Figs. 9 and 10, three years after prestressing both the mean basic creep and mean drying creep were underestimated by 36 $\mu$m and 28 $\mu$m respectively compared to the simulation results output by the model in Section 4. For the stress in the steel bars, the overestimation compared to the model was less than 20 MPa (2%). The outcome in this case is that the strains were slightly underestimated. In the case of the VeRCoRs mock-up, the result of the two approximations (neglecting relaxation and the effect of temperature) may be close to the measurements. Nevertheless, this conclusion is not generally applicable because the size of the structure has a major influence on delayed strains.
5.3 Poisson’s ratio for drying creep

The influence of the value of Poisson’s ratio \( \nu \) on drying creep is examined in this section. Since there are almost no experimental measurements of the value of Poisson’s ratio \( \nu \) for drying creep, there is no consensus on its value. In Section 4, Poisson’s ratio \( \nu \) was taken as equal to 0. Charpin et al. (2017) analyzed ten years of concrete shrinkage and creep measurements and proposed taking the Poisson’s ratio of drying creep as the same as the Poisson’s ratio of basic creep. Here a Poisson’s ratio of 0.2 is tested, like that for basic creep, and of -1, as proposed in the French annex of EC2-1 in the case of biaxial prestressing. This choice reflects the fact that the difference between vertical and horizontal delayed strains rapidly becomes constant in the case of biaxial prestressing (Benboudjema and Torrenti 2015).

The full simulation (described in Section 4) is then performed again, taking the Poisson’s ratio of drying creep as equal to 0, -1 and 0.2. The results are compared with the in-situ measurements in Figs. 13 and 14. It is apparent that all three of the simulations with different values of Poisson’s ratio give predictions, which are more or less consistent with the in-situ measurements of vertical strain. However, it is notable that with a value of 0.2 for the Poisson’s ratio of drying creep, three years after prestressing the model gave a strain value that was 150 \( \mu \)m lower than the simulation with a value of -1 for the Poisson’s ratio of drying creep. This should be compared with the difference between the two measurements, namely 175 \( \mu \)m at the same age. For horizontal strain, the difference between the simulations with the three values of Poisson’s ratio was smaller than that for vertical strain. Therefore, the authors consider that the impact of the value of Poisson’s ratio for drying creep is not very large, and smaller than the differences between the measurements in the structure. It is therefore acceptable to apply a similar value for Poisson’s ratio for drying creep and basic creep.

6. Conclusions

In this paper, applying the creep and shrinkage models in MC2010, the authors have proposed an analytical method for taking account of the influence of variations in temperature and relative humidity on the delayed strains of concrete and the stress relaxation of steel. The model also takes account of the coupling between the delayed strains of the concrete and the stress relaxation of the steel bars. The influence of this coupling in the case of varying temperature and different values of Poisson’s ratio of drying creep has been discussed. The model is limited to the material level and does not take account of structural factors. In critical zones such as gusset or hatch, cracking is expected to occur and reduces the tightness of the NPP. Such problems cannot be studied with the present modeling but more complex and numerical simulations are needed. However, the model in this study can be helpful to evaluate prestress loss in steel bars and hence to estimate margin of safety.

The following conclusions are drawn through this study:

1. Despite its simplicity, the model was able to predict delayed strains in a mock-up of the containment building of a nuclear power plant with a fair degree of accuracy.

2. The superposition principle can be applied to take account of variations in temperature and relative humidity over time.

3. Coupling between the delayed strains in the concrete and the stress relaxation in the steel bars is important. If it is ignored, the stress in the prestressing may be overestimate by as much as 17% after three years in the case of VeRCoRs mock-up.

4. With regard to the temperature history of the VeR-CoRs mock-up, if the effect of temperature variation on the delayed strains of the concrete and the stress relaxation of the steel is ignored, the delayed strains are underestimated by 15%.

5. The value of Poisson’s ratio for drying creep does affect the prediction of the delayed strain, but only to a limited extent. It would seem acceptable to adopt the same value for the Poisson’s ratio for drying and basic creep.
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