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Creep Response of Asphalt Concretes: Visco-Elasto-Plastic Modeling

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Abstract: The paper presents and discusses the calibration and validation of a three-dimensional constitutive visco-elasto-plastic model developed for the analysis of the mechanical response of asphalt concretes. The methodology, an inverse problem technique, uses a one-dimensional analytic formulation of the constitutive model and a non-linear constrained optimization based on the Conjugate Gradient algorithm. On the basis of the creep recovery data obtained from an experimental investigation in support of the model calibration, it has been verified that the values of the constitutive parameters can be reliably identified. A subsequent comparison between the experimental creep and numerical curves of the 3-D model demonstrated reasonable shifts, confirming the reliability of the identification procedure for the parameters.

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Key words*: Asphalt concrete: Creep test; Constitutive modeling; Visco-elasto-plasticity.*

Introduction

In recent years, the scientific community in the road sector has become increasingly interested in the development and numerical implementation of constitutive mechanistic models for asphalt concretes. The reason for this is the by now widespread awareness of the many advantages deriving from a reliable analysis of the stress-strain response of the materials forming the bituminous layers of the pavement, such as a more rational sizing of the superstructure [1], which takes into consideration the main causes of deterioration (rutting, fatigue cracking, thermal cracking).

In the computer modeling of bituminous mixtures, the estimate of the constitutive parameters is a fundamental prior step for a reliable simulation of the mechanical behaviour of the materials and hence the pavements. The calibration process obviously becomes increasingly complex as the sophistication of the studied model increases.

This paper presents a calibration and validation procedure for a visco-elasto-plastic model developed by the Group of Mechanics of Infrastructure Materials at Delft University of Technology [1]. Since the determination of the parameters should always be based on a comparison between experimental and model (analytical and/or numerical) data, an appropriate laboratory investigation, focused on the creep characterization of three different asphalt concretes, has been developed by the Authors, in support of both the calibration and validation.

Materials and Methods

Materials

The creep response of three types of bituminous mixtures for road and airport construction has been subject to study: a Stone Mastic Asphalt (SMA), a Wearing Course Asphalt concrete (WCA) and a Base Binder Modified mixture (BBM) were analyzed.

The mixes were produced with natural crushed limestone aggregate, limestone filler and two types of Electric Arc Furnace steel slags, from different steel mills in northern Italy; the aggregates were made available in three different fractions: 0/5, 5/10 and 10/15 mm. For all the mixes the binder used was a “hard” SBS (Styrene-Butadiene-Styrene) modified bitumen, 44 dmm pen at 25°C (EN 1426).

The SMA mix has been designed with reference to the SITEB (Italian Association of Asphalt Pavement Technologists) Specifications, with an overall slag content equal to 25% and a nominal maximum aggregate size of 12 mm. For the WCA mix the grading distribution was optimized with reference to the design grading envelopes of SITEB and several Italian motorways companies. The total amount of steel slag resulted equal to 30%; the maximum aggregate size was 12 mm. The particle size distribution of the BBM mixes was optimized with reference to the design grading envelopes of Italian motorways companies (for binder course asphalt mixes) and the grading envelope “B” of SITEB. The BBM mixtures are characterized by the 44.7% of EAF slag and a nominal maximum aggregate size of 16 mm. Fig. 1 presents the design grading curves of the mixes. Table 1 reports the design bitumen content as well as the main physical and mechanical properties of the mixes, in terms of bulk density, air voids content after compaction, Marshall Stability and Quotient (EN 12697-34 Standard) as well as Indirect Tensile Strength (EN 12697-23 Standard). Full details on the chemical, physical-geotechnical and leaching properties of the aggregates, as well as on the mix design and mechanical characterization of the three asphalt concretes, have been already discussed by the authors [2, 3].

Methods

Creep Analysis

Tests of creep recovery (constant load axial tests) with free lateral

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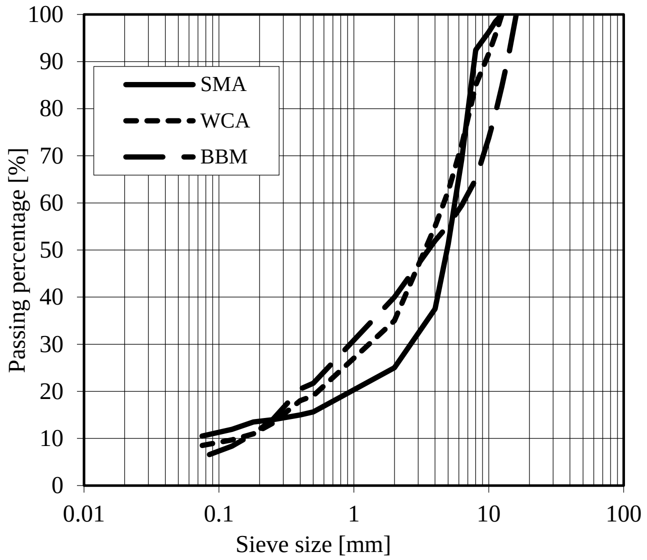

Fig. 1. Design Grading Curves of the Mixes.

Table 1. Physical and Mechanical Properties of the Mixes.

Physical and Mechanical Properties	Mixture Type		
	SMA	WCA	BBM
Bulk Density (kg/m^3)	2,532	2,672	2,796
Voids Content after Compaction (%)	3.2	3.9	5.0
Marshall Stability (daN)	1,530	1,407	1,231
Marshall Quotient (daN/mm)	645	546	391
ITS @ 25°C (MPa)	1.33	1.10	0.97
Optimum Bitumen Content (%)	5.5	5.0	4.5

expansion, at a temperature of 40°C on Marshall cylindrical specimens, were conducted in order to analyse the creep properties of the mixtures. Three load application times of 10, 20 and 30 s were scheduled, and a rest period for visco-elastic recovery set at 110, 100 and 90 s respectively, for an overall test duration of 120 s. In order to obtain a sufficiently representative study of the deformational response of the mix, a set of three stress levels was chosen (100, 300 and 500 kPa) per each loading time.

From the theoretical point of view, different approaches can be found in the literature in order to identify the constitutive relationships which describe the mechanical behaviour and the creep response of an asphaltic material, as for instance fractional models [4-7] or conventional rheological models, characterized by visco-elasticity, elasto-plasticity, visco-plasticity [8-16]. Different approaches are based on the continuum damage mechanics [17, 18] as well as on the distinct element method [19-22].

In the present research, the creep behaviour of the considered asphalt concretes was analyzed by means of a visco-elasto-plastic constitutive model implemented in a three dimensional computational platform, namely CAPA 3D, developed by the Group of Mechanics of Infrastructure Materials at Delft University of Technology [1]; its main characteristics are the energy based formulations and parallel workings of the elasto-plastic and visco-elastic components. In the following, some more details of the energy based formulation of the computational model are given.

Hyper-Elastic Energy Based Three Dimensional Formulation

A material filament defined by vector **dx** in the deformed current configuration is related by means of the deformation gradient tensor **F** to its undeformed (reference) configuration via the relation

$$\mathrm{d}\mathbf{x} = \mathbf{F}\,\mathrm{d}\mathbf{X} \tag{1}$$

If it is now assumed that the forces acting on the material element containing the filament are removed, the initial reference configuration will only be obtained if the material is elastic. In all other cases, another configuration will be obtained in which the original vector **dX** is mapped onto vector with the subscript r indicating the residual nature of deformation. The Helmholtz free energy can be defined as a function of the deformation gradient **F** so that

$$\dot{\Psi} = \frac{\partial \Psi(\mathbf{F})}{\partial \mathbf{F}} : \dot{\mathbf{F}} \tag{2}$$

Then, from the second law of thermodynamics, often referred to as the Clausius-Planck relation, for a non-dissipative material, the second Piola Kirchhoff stress tensor and the Cauchy stress tensor can be found as

$$\mathbf{S} = 2\frac{\partial \Psi}{\partial \mathbf{C}} \quad ; \quad \boldsymbol{\sigma} = 2\,\mathrm{J}^{-1}\,\mathbf{F}\,\frac{\partial \Psi}{\partial \mathbf{C}}\,\mathbf{F}^{\mathrm{T}} \tag{3}$$

Depending on the choice of the Free Energy function, the formulation of the stress tensor will change. It can be shown [1] that a general formulation of the Second Piola Kirchhoff stress tensor can be written as

$$\begin{aligned}\mathbf{S} &= 2\left[\left(\partial_{\mathrm{I}_1}\Psi + \mathrm{I}_1\,\partial_{\mathrm{I}_2}\Psi\right)\mathbf{I} - \partial_{\mathrm{I}_2}\Psi\,\mathbf{C} + \mathrm{I}_3\,\partial_{\mathrm{I}_3}\Psi\,\mathbf{C}^{-1}\right] \\ &= \mathrm{s}_1\,\mathbf{I} + \mathrm{s}_2\,\mathbf{C} + \mathrm{s}_3\,\mathbf{C}^{-1}\end{aligned} \tag{4}$$

Elasto-Visco-Plastic Energy Based Three Dimensional Formulation

Consider now a loading/unloading cycle with residual configuration. Let $\mathbf{F_e}$ denote the deformation gradient relating the residual deformation configuration to the current configuration. Then, according to Eq. (1):

$$\mathrm{d}\mathbf{x} = \mathbf{F}_{\mathrm{e}}\,\mathrm{d}\mathbf{x}_{\mathrm{r}} \qquad \mathbf{F} = \mathbf{F}_{\mathrm{e}}\,\mathbf{F}_{\mathrm{r}} \tag{5}$$

where $\mathbf{F_r}$ denotes the deformation gradient relating the residual deformation configuration to the reference configuration. This process of decomposing the deformation gradient is known as the “multiplicative decomposition” of the deformation gradient to a residual deformation component and a component signifying the elastic unloading that the material must undergo from the configuration at time **t** to the residual configuration.

The deformation gradient of a material, when the elasto-plastic and the visco-elastic components act in parallel, can be decomposed as

$$\mathbf{F} = \mathbf{F}_\infty \mathbf{F}_p \quad ; \quad \mathbf{F} = \mathbf{F}_e \mathbf{F}_v \tag{6}$$

where $\mathbf{F}_\infty$ is the elastic component of the deformation gradient of the elasto-plastic element, $\mathbf{F_p}$ is the plastic component of the deformation gradient of the elasto-plastic element, $\mathbf{F_e}$ is the elastic component of the deformation gradient of the visco-elastic element and $\mathbf{F_v}$ is the viscous component of the deformation gradient of the visco-elastic element.

From this, the right Cauchy-Green strain tensor **C** can be described by:

$$\mathbf{C} = \mathbf{F}^T \mathbf{F} = \mathbf{F}_v^T \mathbf{C}_e \mathbf{F}_v = \mathbf{F}_p^T \mathbf{C}_\infty \mathbf{F}_p \tag{7}$$

A definition has been subsequently derived to compute the total strain tensor of the material **C**, based on either of the elastic strain tensors and the plastic or the viscous deformation gradient, respectively. The Helmholtz free energy function, for a three dimensional model equivalent to the generalized model proposed, can be expressed as

$$\Psi = \Psi_v(\mathbf{C}_e) + \Psi_p(\mathbf{C}_\infty, \xi_p) \tag{8}$$

where ξ_p is a measure of the plastic deformation.

The Clausius-Planck local dissipation inequality can be formulated as

$$\mathbf{S} : \tfrac{1}{2}\dot{\mathbf{C}} - \left[\frac{\partial \Psi_v}{\partial \mathbf{C}_e} : \dot{\mathbf{C}}_e \right] - \left[\frac{\partial \Psi_p}{\partial \mathbf{C}_\infty} : \dot{\mathbf{C}}_\infty + \frac{\partial \Psi_p}{\partial \xi_p} \dot{\xi}_p \right] \geq 0 \tag{9}$$

The previous formulation gives the general frame-work of the large-strain, energy based, CAPA-3D model which is utilized to simulate the behaviour of asphalt materials [1, 23].

One-Dimensional Constitutive Formulation

Fig. 1 represents the one-dimensional scheme of the simplified analytical model, derived from the one (3-D) delineated into the previous Section. It is composed of a Maxwell visco-elastic element, in parallel with an elasto-plastic element; the latter being characterised by an elastic spring, connected in series to a plastic slider, which presents non-linear hardening, with the yielding limit Y linked to the plastic strain ε_p by the equation:

$$Y = k_\infty + (k_0 - k_\infty) e^{-\alpha \varepsilon_p} \tag{10}$$

In Eq. (10), k_0 and k_∞ are the initial and asymptotic yielding limits respectively, while α is the exponent of the hardening law. The constitutive parameters of the analytical model are therefore given by the viscosity η of the viscous dashpot, by Young's moduli E_1 and E_2 of the visco-elastic and elasto-plastic elements respectively, as well as the previously cited k_0, k_∞, and α, linked to the plastic slider.

For this type of model, the response to a static creep load results as being visco-elastic, as long as the tension in the elasto-plastic element is lower than the initial yielding limit. When, during the loading phase, the tension in the elasto-plastic element is above the initial yielding limit, the slider becomes active, leading to the development of irreversible plastic strains. The critical instant (t_{cr}) that delimits the two different mechanical behaviours can be determined with the following equation [24]:

$$t_{cr} = \frac{\eta(E_1 + E_2)}{E_1 E_2} \ln\left[\frac{\sigma_0 E_1}{(\sigma_0 - k_0)(E_1 + E_2)} \right] \tag{11}$$

So, for $t \leq t_{cr}$, the response of the model is governed by the equation:

$$\frac{\eta}{E_1}\dot{\sigma} + \sigma = \frac{\eta(E_1 + E_2)}{E_1}\dot{\varepsilon} + E_2 \varepsilon \tag{12}$$

which, in terms of total strains, leads to the equation:

$$\varepsilon(t) = \frac{\sigma_0}{E_2}\left[1 - \frac{E_1}{(E_1 + E_2)} e^{-\frac{E_1 E_2}{\eta(E_1 + E_2)} t} \right] \tag{13}$$

Vice versa, when $t_{cr} < t \leq t_1$, the behaviour of the model is defined by:

$$\frac{\eta}{E_1}\dot{\sigma} + \sigma = \eta\dot{\varepsilon} + \left[1 - \frac{\alpha \dot{\varepsilon}\, \eta}{E_1} \right] (k_0 - k_\infty) e^{-\alpha\left(\varepsilon - \frac{k_0}{E_2}\right)} + k_\infty \tag{14}$$

that allows to write, in terms of strain rate:

$$\dot{\varepsilon} = \frac{\sigma_0 - k_\infty - C}{\eta\left(1 - \frac{\alpha}{E_1} C\right)} \tag{15}$$

where $C = (k_0 - k_\infty) \exp\left(\frac{\alpha k_0}{E_2}\right) \exp(-\alpha \varepsilon)$ (16)

When a backward Euler scheme is used, Eq. (15) can be written incrementally as:

$$\Delta \varepsilon_T = \frac{\sigma_0 - k_\infty - C}{\eta\left(1 - \frac{\alpha}{E_1} C\right)} \Delta t \tag{17}$$

Eq. (17), being an analytical approximation based on the result of the numerical integration, allows the following equation to be assumed for the total strain:

$$\varepsilon(t) = \varepsilon_0 + (a - \varepsilon_0)(1 - e^{-bt}) \tag{18}$$

where ε_0 is the total instantaneous strain developed by the material at the initial moment of stress, while a and b can be determined by the following equations:

$$a = -\frac{1}{\alpha} \ln\left(\frac{k_\infty - \sigma_0}{k_\infty - k_0} \right) + \frac{k_0}{E_2} \tag{19}$$

$$b = \frac{\sigma_0 - k_\infty - C_0}{\left(a - \varepsilon_0\right)\eta\left(1 - \frac{\alpha}{E_1} C_0\right)} \tag{20}$$

with $C_0 = \left(k_0 - k_\infty\right)\exp\left(\frac{\alpha k_0}{E_2}\right)$ (21)

At the release, where $t > t_1$, the temporal evolution of the total strain is given by:

$$\begin{aligned}\varepsilon(t) &= \varepsilon_0 + \left(a - \varepsilon_0\right)\left(1 - e^{-bt_1}\right) - \frac{k_\infty}{E_2} - \frac{\left(k_0 - k_\infty\right)}{E_2} e^{-\alpha\left(\varepsilon_0 + (a-\varepsilon_0)\left(1-e^{-bt_1}\right) - \frac{k_0}{E_2}\right)} \\ &- \frac{1}{E_2}\left\{\sigma_0 - k_\infty - \left(k_0 - k_\infty\right)e^{-\alpha\left(\varepsilon_0 + (a-\varepsilon_0)\left(1-e^{-bt_1}\right) - \frac{k_0}{E_2}\right)} - \frac{\sigma_0 E_1}{\left(E_1 + E_2\right)}\right\} e^{-\frac{E_1 E_2}{\eta(E_1+E_2)}(t - t_1)}\end{aligned} \tag{22}$$

Calibration and Validation of the Visco-elasto-plastic Model

The constitutive model described in the previous Section, was calibrated and then validated using experimental data obtained from creep recovery tests performed on the asphalt concretes investigated.

The experimental programme, for each loading level, can be summarised as follows:

$$\begin{aligned} &t \le 0, && \sigma(t) = 0 \\ &0 < t < t_1, && \sigma(t) = \sigma_0 = \text{const} \\ &t > t_1, && \sigma(t) = 0 \end{aligned} \tag{23}$$

where t_1 indicates the moment of unloading and σ_0 the value of the creep load applied.

The calibration procedure requires preliminary determination, for each stress level, of the Plastic Strain Curve (PSC), by interpolation of the experimental time-strain pairs of data, formed, for each creep test, by the time of application peak of the stress and the corresponding value of permanent strain (Fig. 2). The PSC was thus determined for each stress level and, from this, the value of the critical time (t_{cr}) identified as the temporal instant when the PSC intercepts the temporal axis of the abscissa. The ordinate value corresponding to the critical time represents the total instantaneous strain (ε_0) necessary for the calculation of the model's total strains according to Eq. (18).

The calibration method is based on the identification of the set of values of the constitutive parameters that minimises the difference, in terms of least squares, between the experimental total strains and those of the model. This consists of an iterative optimisation procedure of the objective function *f(x)* defined by:

$$f(\boldsymbol{x}) = \sum_{i=1}^{N}\left(\varepsilon_i^{Exp} - \varepsilon_i^{Mod}\right)^2 \tag{24}$$

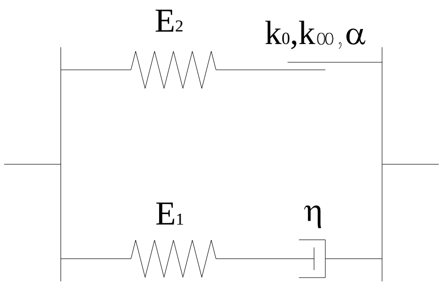

Fig. 2. One-dimensional Visco-elasto-plastic Model.

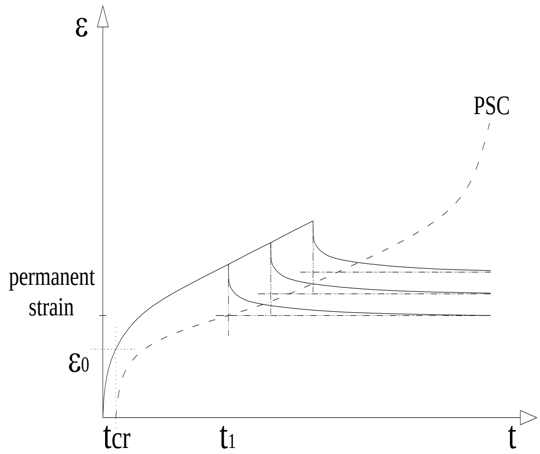

Fig. 3. Creep Curves and Plastic Strain Curve.

where ε_i^{Exp} and ε_i^{Mod} represent a pair of total strains, experimental and model's respectively, corresponding to the same temporal instant ***i***; ***x*** is the vector of the constitutive parameters; *N* is the number of experimental observations. Eqs. (18) and (22) were used for the calculation of ε_i^{Mod} in Eq. (24). For the minimising of the objective function *f(x)*, the Conjugate Gradient algorithm was used [25]. To avoid the algorithm identifying physically unacceptable values, the minimisation was developed within the ambits of constrained optimisation, defining specific acceptability ranges for each of the six variables involved. Starting from the nine available creep curves (three per each stress level), only the creep curve 100 kPa - 20 s was used for the calibration, while the remaining eight made up the dataset for the validation.

Since neither the plastic slider nor the viscous dashpot are active at the moment of release ($t = t_1$), it is possible to estimate the total stiffness of the two elastic springs of the analytical model (E_{TOT}) from the experimental value of the total strain retrieved instantaneously ($\Delta\varepsilon$), according to the equation:

$$E_{TOT} = \left(E_1 + E_2\right) = \frac{\Delta\sigma}{\Delta\varepsilon} \tag{25}$$

where $\Delta\sigma$ represents the variation in tension recorded at the release.

In order to eliminate one variable from the set of parameters in the optimisation, $E_1 = E_{TOT} - E_2$ was assumed; so, E_{TOT} being known from Eq. (25), of the two Young's moduli, only E_2 was identified through the optimisation algorithm.

The calibration procedure is now split into two phases: in the first one E_2 and η are identified, by means of the objective function minimization for the unloading time period, during which the material behaviour is visco-elastic and can be described by Eq. (22); the plastic constitutive parameters are "frozen" and therefore they are not optimized in this step. Then, in the second part of the calibration, assuming as constant the values just obtained for E_1, E_2, and η, the objective function is minimized during the loading phase, in which the material response is visco-elasto-plastic and can be described by Eq. (18), giving in this way the possibility to identify the values of k_0, k_∞, and α.

Numerical Validation with CAPA-3D

The cylindrical Marshall specimens, used in the experimental investigation, have been modeled with a Finite Element Method (FEM) platform, namely CAPA 3D, in order to simulate the creep recovery tests. Given the symmetries of a cylindrical specimen, the FEM analysis was performed using a mesh reproducing a quarter of a cylinder, in order to reduce the computational time. The quarter of the cylindrical Marshall sample has been discretized with a mesh, made of 429 isoparametric cubic finite elements (Fig. 4), each of them characterized by 20 nodes.

The numerical curves, obtained from the 3-D visco-elasto-plastic model implemented in CAPA-3D [26], using the parameters estimated in the calibration phase, were subsequently compared with the experimental and analytical ones, in order to complete the validation phase.

In the numerical model, the viscosity has a deviatoric component η_D, and a volumetric one η_V, linked by the equation:

$$\eta = \left(\frac{1}{3\eta_D} + \frac{1}{9\eta_V} \right)^{-1} \tag{26}$$

Given that the studied asphalt concretes are characterised by residual voids within the range 3.2% to 5% (Table 1), the volumetric behaviour of the material was assumed to be uncompressible, setting an η_V of 10,000 MPa·s; the value of η being known from the calibration, from Eq. (26), that of η_D was computed for each asphalt concrete.

Even if only the response in the loading direction of the creep recovery test is analysed in the present study, the CAPA-3D model is a three-dimensional model and therefore the Poisson's coefficient has to be determined. The Poisson's coefficient is one of the most relevant parameters that affect the deformation properties of bituminous flexible pavements [27]. Therefore, in order to improve the understanding of the mechanical response of asphalt concretes and the accuracy of the finite element simulation results, the Poisson's ratio should be identified accurately [27]. Even though the value of the Poisson's Ratio is usually assumed by literature data [28], it could be calculated using the measured recoverable vertical and horizontal deformation, for instance in a Resilient Modulus test (ASTM D 4123). However, in the present study, an experimental determination of the Poisson's coefficient was not performed and therefore a value from the Standard EN 12697-26 (Annex C) of 0.35 was assumed in the CAPA 3-D model, for all the mixes. The assumption appears reasonable, in considerations of the stress levels used in the creep recovery tests and focusing the attention only on the response along the loading direction. It is worth mentioning that the Standard EN 12697-26 (Annex C), suggests a Poisson's coefficient value irrespective of the testing temperature.

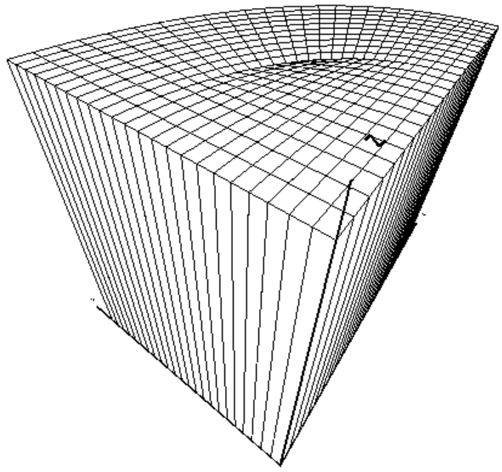

Fig. 4. Numerical Model Mesh.

Table 2. Creep Recovery Test Results.

Stress Level [kPa]	Permanent Axial Strain [-] SMA	WCA	BBM
100	0.000863907	0.001160147	0.001426507
300	0.003184578	0.00437258	0.00559962
500	0.006347112	0.007652613	0.010156

Results and Discussion

Calibration and Analytical Validation

Table 2 reports the permanent axial strain of the mixes, resulted from the constant load axial tests, for loading time of 30 s and for each stress level, at the end of the recovery time. Table 3 reports the values of t_{cr} and the corresponding ε_0 of the mixes, for each of the three stress levels investigated. As it was expected, the higher the

Table 3. Critical Times and Initial Total Strains at Different Stress Levels.

Stress level	SMA		WCA		BBM	
[kPa]	t_{cr} [s]	ε_0 [-]	t_{cr} [s]	ε_0 [-]	t_{cr} [s]	ε_0 [-]
100	0.636115	0.000739	0.089937	0.000712	0.756218	0.000111
300	0.079554	0.000953	0.058021	0.001236	0.252640	0.000723
500	0.001863	0.000973	0.003673	0.001493	0.016358	0.002275

Table 4. Parameters Identification Results.

Mixtures	E_1 [MPa]	E_2 [MPa]	η [MPa·s]	k_0 [MPa]	k_∞ [MPa]	α [−]
SMA	176.0000	95.0000	289.9999	0.0300	1.7930	27.9258
WCA	277.5438	102.4562	93.7390	0.0100	2.2821	20.7037
BBM	298.1718	91.8282	175.8320	0.0204	1.8143	20.3529

stress level, the lower the t_{cr} and the greater the ε_0, for all the mixtures.

Table 4 presents the results obtained from the calibration of the one dimensional visco-elasto-plastic model. The comparison between the total strains from experimental data and those computed with the analytical model, using the set of constitutive parameters identified in the optimisation, for both the calibration and validation curve, is proposed in Fig. 5, 6, 7 (SMA mixes), Fig. 8, 9, 10 (WCA mixes) and Fig. 11, 12, 13 (BBM mixes), referred to the stress levels of 100, 300 and 500 kPa respectively.

The curve of 100 kPa and 20 s used for the calibration shows the minimum shift of model's and experimental data; the analytical curve, relative to the set of parameters identified with the optimisation algorithm, is more or less overlapping, for each mix studied. The shifts between the model's and corresponding experimental curves increase at increasing stress levels. The deviations between the experimental values and those of the analytical model, related to both the peak and permanent strains, are obviously minimal for the calibration curve, but also remain reasonably limited for all the other curves used in the validation, never being above the threshold of 20%. The validation of the analytical model can therefore be considered satisfactory, with respect to the asphalt concretes considered.

Numerical Validation with CAPA-3D

Figs. 14, 15 and 16 report the results of the numerical validation for the mixes analysed, in terms of creep curves relative to the different stress levels and loading time of 30 s; similar results have been obtained for the other two loading times. The percentage deviations between the numerical and experimental data were a maximum of 15.38% for the permanent strains and 17.09% for the peak strains, depending on the mixtures analysed. The numerical validation with the CAPA-3D model can thus also be considered positively.

Conclusions

The work presented in this paper deals with the determination of the material parameters that characterize an energy based, visco-elasto-plastic constitutive model, for bituminous materials.

The identification method is based on a specific experimental-analytical procedure developed ad hoc; it is splitted in two main phases, related with the unloading and loading parts of experimental creep recovery curves. The core of the calibration procedure is given by the one dimensional analytical formulation of the constitutive model, characterized by six material parameters, related to plasticity with nonlinear hardening and visco-elasticity.

The inverse problem is defined with the minimization of the function that characterizes the error between the experimental values and those obtained analytically with the constitutive model.

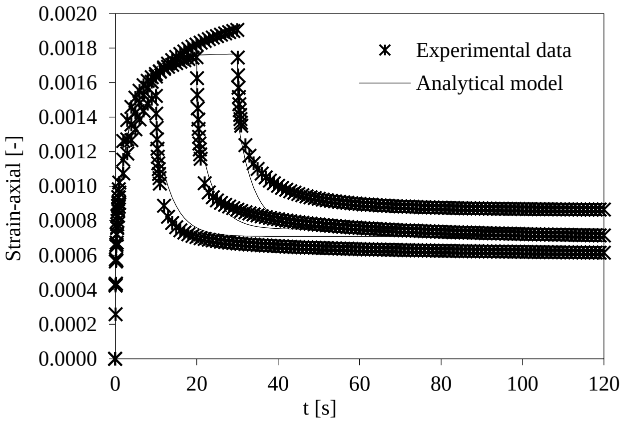

Fig. 5. Experimental and Analytical Creep Recovery Curves @ 100 kPa for SMA Mix.

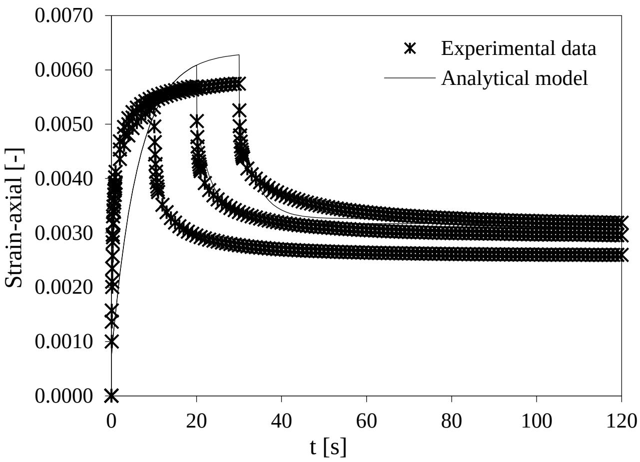

Fig. 6. Experimental and Analytical Creep Recovery Curves @ 300 kPa for SMA Mix.

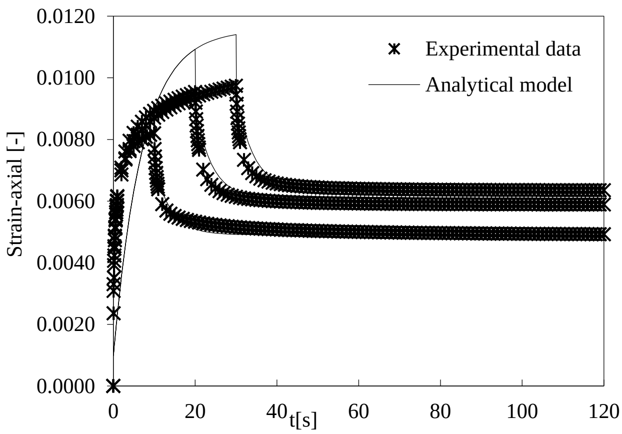

Fig. 7. Experimental and Analytical Creep Recovery Curves @ 500 kPa for SMA Mix.

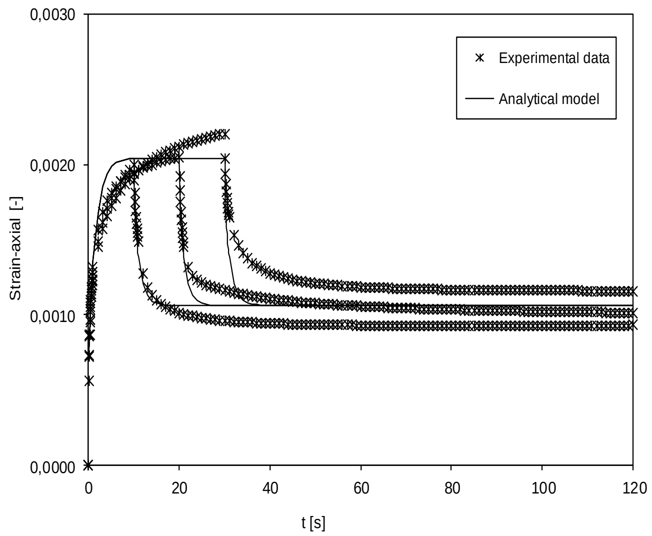

Fig. 8. Experimental and Analytical Creep Recovery Curves @ 100 kPa for WCA Mix.

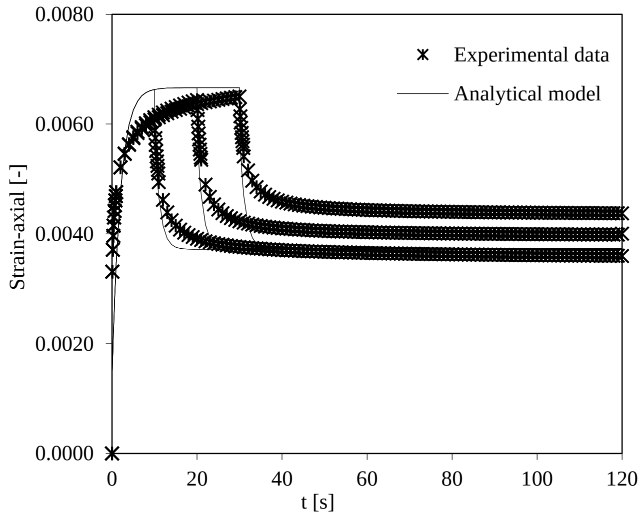

Fig. 9. Experimental and Analytical Creep Recovery Curves @ 300 kPa for WCA Mix.

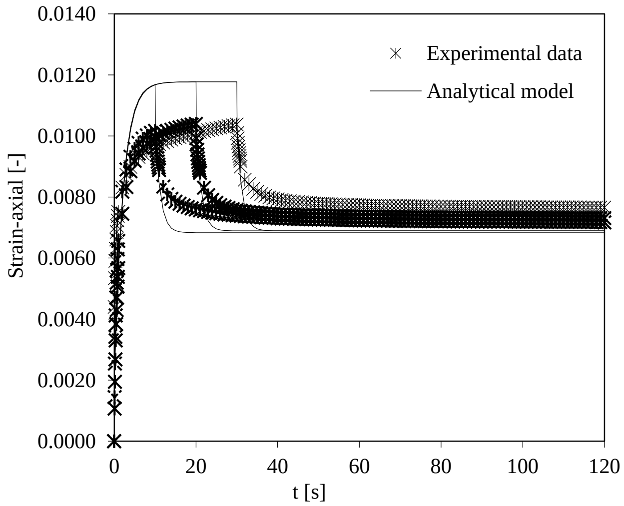

Fig. 10. Experimental and Analytical Creep Recovery Curves @ 500 kPa for WCA Mix.

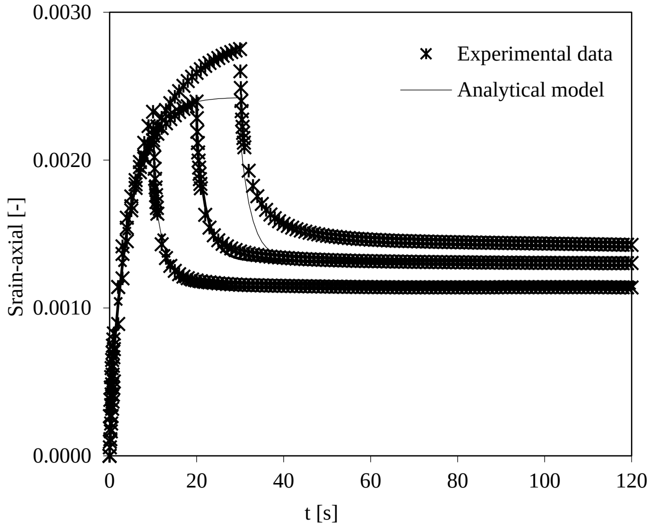

Fig. 11. Experimental and Analytical Creep Recovery Curves @ 100 kPa for BBM Mix.

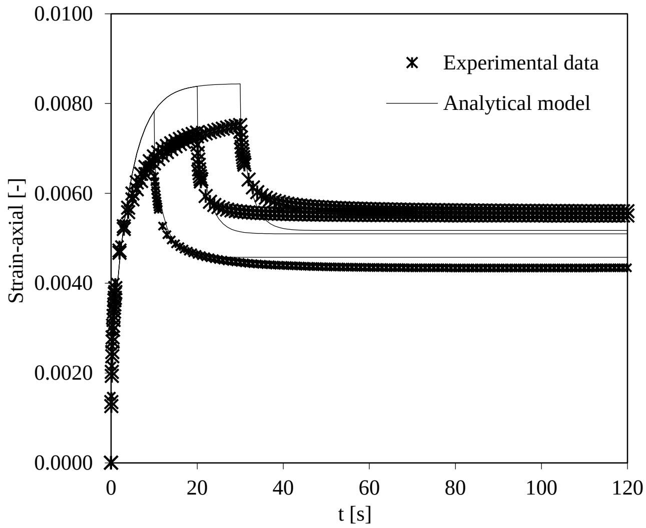

Fig. 12. Experimental and Analytical Creep Recovery Curves @ 300 kPa for BBM Mix.

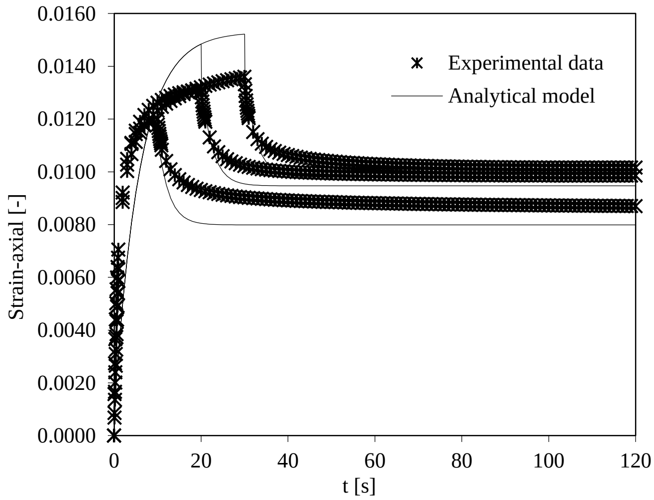

Fig. 13. Experimental and Analytical Creep Recovery Curves @ 500 kPa for BBM Mix.

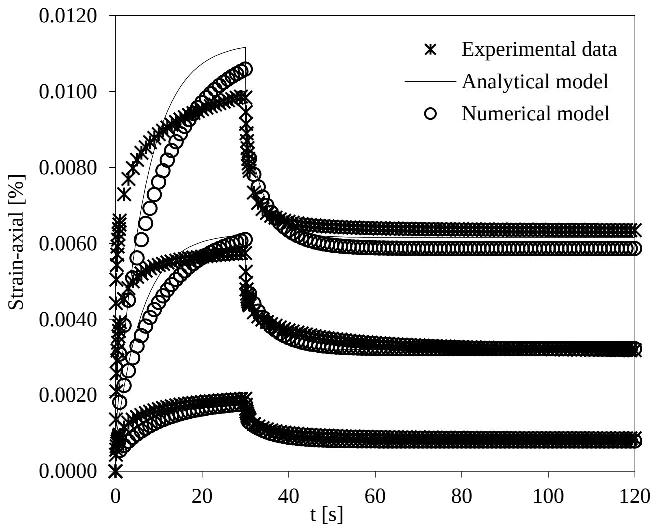

Fig. 14. Experimental, Analytical and Numerical Creep Recovery Curves for SMA Mix.

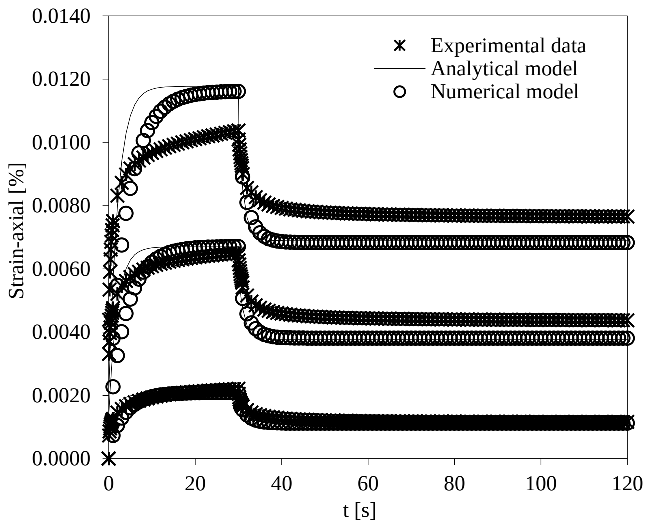

Fig. 15. Experimental, Analytical and Numerical Creep Recovery Curves for WCA mix.

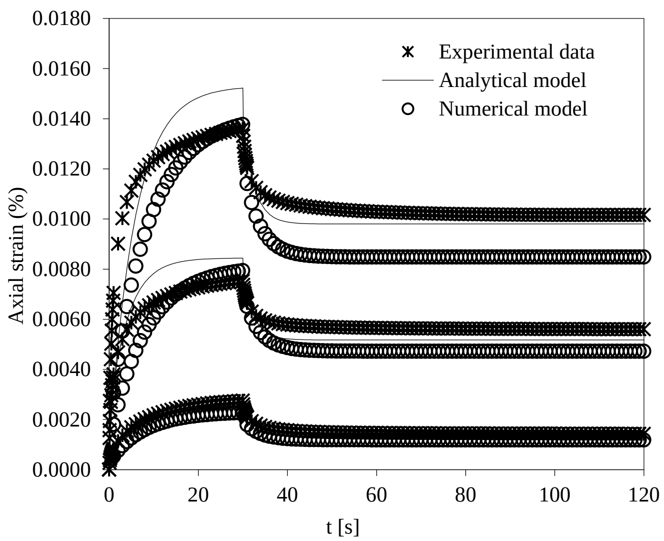

Fig. 16 Experimental, Analytical and Numerical Creep Recovery Curves for BBM Mix.

In order to solve such constrained optimization problem, the Conjugate Gradient algorithm was used; it has been capable of determining material parameter sets, for each asphalt concrete investigated.

The experimental-numerical validation has demonstrated that the 3-D constitutive model can interpret the fundamental aspects of the response of the asphalt concrete, in terms of both maximum and permanent strains, at different loading times and stress levels.

The model has been calibrated and validated on the basis of the creep response of three different asphalt concretes: a Stone Mastic Asphalt, a Wearing Course Asphalt and a Base Binder Modified mixture. However the outlined identification procedure could be used to calibrate and validate the considered visco-elasto-plastic model also for other asphalt concretes.

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