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## A continuum Damage Approach of Asphalt Concrete Fatigue Tests

Didier Bodin<sup>1</sup>,  
Gilles Pijaudier-Cabot<sup>2</sup>, Member, ASCE,  
Chantal de La Roche<sup>3</sup>  
and Armelle Chabot<sup>4</sup>

### ABSTRACT

To forecast pavement fatigue cracking, fatigue performances of asphalt mixtures are assessed using laboratory cyclic fatigue tests. A damage model is implemented to predict the behavior of asphalt mixtures during these tests. Under sinusoidal loading, the evolution of the complex modulus of the material is defined as the damage variable associated to a microcracking mechanism. Its evolution during fatigue tests is described by an elasticity based, nonlocal damage model. A cycle based time treatment is implemented into a finite element code, to simulate high cycle fatigue tests. The damage model parameters identification is performed using uniaxial fatigue tests. With these parameters, bending fatigue tests simulation results are discussed. The comparison between simulated results and experimental data points out a good approximation of the fracture process before damage localization in the specimen.

**Keywords:** fatigue, damage, nonlocal, finite element, asphalt concrete

### INTRODUCTION

Cyclic mechanical loadings, in pavement layers, lead to material fatigue damage. Cumulation of these micro degradations can initiate very injurious cracks through the pavement structure. Laboratory cyclic tests on asphalt mixes allow to predict and to take into account material fatigue performances in mixture and pavement design. Classical laboratory approach is based on sinusoidal constant amplitude loadings. The specimen stiffness is given as a function of the number of cycles as shown in (Fig. 1) which presents the displacement controlled uniaxial case. Various laboratory fatigue tests have been proposed (SHRP 1994) as well as different interpretation methods (Di Benedetto et al. 1997). Differences between results of one type of test to another, show the difficulties to point out the intrinsic characterization of the asphalt mixes fatigue damage process.

During laboratory fatigue tests, asphalt concrete fracture process can be divided into three phases (Piau 1989), (Lefeuvre et al. 2000). These three different stiffness evolution stages

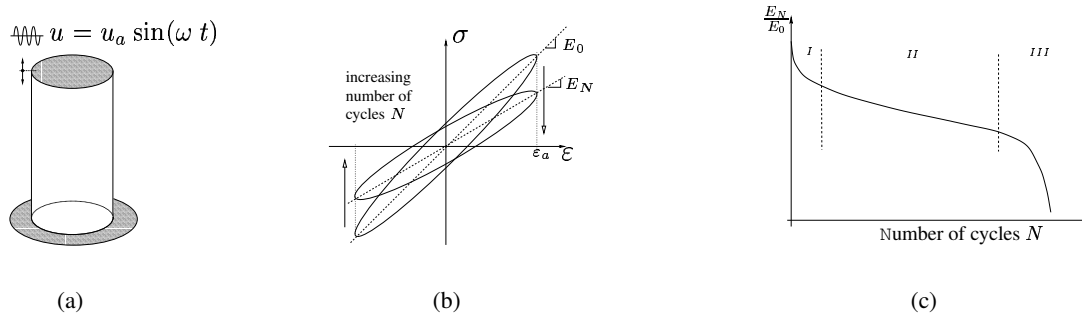
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<sup>1</sup>Pavement Material and Structure Dept., Laboratoire Central des Ponts et Chaussées, BP 4129, F-44341 Bouguenais cedex, France. E-mail: didier.bodin@lcpc.fr

<sup>2</sup>R&DO, Laboratoire de Genie Civil de Nantes-Saint Nazaire Ecole Centrale de Nantes, BP 92101, F-44321 Nantes cedex 03, France. E-mail: gilles.pijaudier-cabot@ec-nantes.fr

<sup>3</sup>Pavement Material and Structure Dept., Laboratoire Central des Ponts et Chaussées, BP 4129, F-44341 Bouguenais cedex, France. E-mail: chantal.de-la-roche@lcpc.fr

<sup>4</sup>Pavement Material and Structure Dept., Laboratoire Central des Ponts et Chaussées, BP 4129, F-44341 Bouguenais cedex, France. E-mail: armelle.chabot@lcpc.fr



**FIG. 1. Uniaxial displacement controlled fatigue tests conditions (a) test principle, (b) stress - strain evolution and the stiffness decrease, (c) specimen stiffness function of number of cycles**

(Fig. 1(c)) are connected with different damage mechanisms. In phase one (*I*), microcracks are created in the binder films between the aggregates. Therefore, the initial curve drop illustrates this process, superposed to a thermomechanical transient regime due to viscoelastic dissipation. In phase two (*II*), the thermal regime is permanent and the initiated cracks grow, leading to a progressive macrocrack pattern. At last, in phase three (*III*), a single macrocrack propagates until fracture of the specimen, via an accelerated process.

For these tests without rest period, the dissipation induced thermal effects (de La Roche et al. 1998) are experimental artifacts that are not field representative. In this paper, the viscoelastic problem is considered in terms of complex modulus (Huet 1963). We assume the isothermal damage evolution process under sinusoidal loadings, which is relevant under given laboratory tests conditions. This allows to use the elastic formalism. An elasticity based damage approach is chosen (Bodin et al. 2001) to describe the local complex modulus decrease (Fig. 1(b)) induced by microcrack development (Lemaitre 1996). The strain localization phenomenon, in the description of the whole fracture process, is treated here by a nonlocal integral damage model (Pijaudier-Cabot and Bažant 1987). A finite element implementation has been carried out for structural fatigue calculations using a cycle based time integration scheme (Peerling et al. 2000). Identification of the model parameters is performed using uniaxial tests and then applied to bending tests.

## THE CONTINUUM DAMAGE APPROACH

The damage parameter  $D$ , related to the material degradation, is assumed to be a scalar. Hence, the elasticity tensor reduction is isotropic and the stress-strain relation is given by (Eq. 1)

$$\sigma_{ij} = (1 - D)a_{ijkl}\varepsilon_{kl} \quad (1)$$

where  $\sigma_{ij}$ ,  $\varepsilon_{kl}$  are the components of the stress and strain tensors respectively ;  $a_{ijkl}$  are the components of the elastic moduli tensor ( $E$  : Young modulus and  $\nu$  Poisson ratio) of the virgin material. The second thermodynamical principle constrains the damage rate to be positive or equal to zero. The damage grows from 0 for the virgin material to 1 when the material does not transfer stress anymore.

Damage evolution is determined using a scalar equivalent strain expression (Eq. 2), which is a function of the positive principal stresses  $\langle \sigma_i \rangle$ . The tension driven damage mechanism is expressed by a modified Rankine criterion (Patzák and Jirásek 2001),

$$\tilde{\varepsilon} = \sqrt{\sum_{i=1,2,3} \left[ \frac{\langle \sigma_i \rangle}{E(1-D)} \right]^2} \quad (2)$$

and the damage rate expression.

$$\dot{D} = g(D, \tilde{\varepsilon}) \langle \dot{\tilde{\varepsilon}} \rangle \quad (3)$$

The evolution function  $g(D, \tilde{\varepsilon})$  is determined for the studied material. For fatigue application, the expression given by (Eq. 4), already used for fatigue damage by Paas et al. (1993), is chosen:

$$g(D, \tilde{\varepsilon}) = C D^\alpha \tilde{\varepsilon}^\beta \quad (4)$$

where  $C$ ,  $\alpha$  and  $\beta$  are three material parameters. Their value is calibrated according to the material experimental behavior.

The nonlocal integral treatment of the damage provides a consistent description of strain localization phase of damage evolution. It consists in changing  $\tilde{\varepsilon}$  by its weighted average  $\bar{\varepsilon}$  (Eq. 5) in the damage evolution law (Eq. 3) (Pijaudier-Cabot and Bažant 1987).

$$\bar{\varepsilon}(x) = \frac{1}{V_r(x)} \int_{\Omega} \psi(|x-y|) \tilde{\varepsilon}(y) d\Omega \quad \text{with} \quad V_r(x) = \int_{\Omega} \psi(|x-y|) d\Omega \quad (5)$$

where  $\Omega$  is the volume of the structure and  $V_r(x)$  is the representative volume at the point  $x$ . The weight function  $\psi$  which depends on the distance  $|x-y|$  of the considered point  $y$ , is Gaussian typically :

$$\psi(|x-y|) = \exp\left(-\frac{4|x-y|^2}{l_c^2}\right) \quad (6)$$

The characteristic length  $l_c$  is a material parameter of the nonlocal damage model, which is related to the microstructure of the material. When the strain state is homogeneous over the specimen, the integral expression  $\bar{\varepsilon}$  (Eq. 5) is equal to  $\tilde{\varepsilon}$ . The damage evolution is then not a function of the characteristic length  $l_c$ . In particular, in uniaxial homogeneous conditions, the damage evolution does not depend on  $l_c$ , before localization occurs.

For uniaxial displacement controlled fatigue tests (Fig. 1(a)- 1(b)), the damage  $D$  (Eq. 7) reached after  $N$  loading cycles can be directly calculated from equations (Eq. 3, Eq. 4),

$$D = \left[ \frac{C(1-\alpha)}{1+\beta} \varepsilon_a^{\beta+1} N \right]^{\frac{1}{1-\alpha}} \quad (7)$$

where  $\varepsilon_a$  is the strain amplitude applied to the specimen.

A fatigue life criterion can be defined as the number of cycles until damage reaches the threshold value  $\kappa$ . Replacing  $D$  by  $\kappa$ , in (Eq. 7) yields the fatigue life  $N_F$  as a function of the loading amplitude  $\varepsilon_a$  (Eq. 8).

$$N_F = K \varepsilon_a^{-(1+\beta)} \quad \text{with} \quad K = \frac{(1+\beta) \kappa^{1-\alpha}}{C(1-\alpha)} \quad (8)$$

This result is in accordance with the Manson and Coffin fatigue strain approach, mentioned in (Suresh 1998). The fatigue life  $N_F$ , function of loading amplitude  $\varepsilon_a$  curve, which corresponds to the Whöler curve of the material in a log log diagram, shows a negative slope equal to  $\beta + 1$ .

## FATIGUE CUMULATIVE DAMAGE

Fatigue damage process is computed with a finite element program. For high cycle fatigue, the time scale can be higher than  $10^6$  cycles. With a minimum of 10 increments per cycle, the direct incremental approach of the nonlinear problem is not reasonable. A so-called "jump-in-cycle" procedure (Lemaitre 1996) enhanced by (Peerling et al. 2000) to the cumulative damage calculation, is then adopted. Starting from the known value of damage after the cycle  $N$ , this method calculates the damage reached after the  $N + \Delta N$  cycle. Each step uses one nonlinear calculation over a single loading cycle.

The number of cycle  $N$ , extended to a continuous time variable, is taken as an alternative time scale, taking into account the slow damage evolution. The damage growth per cycle becomes the damage rate with the new time variable  $N$  (Eq. 4).

$$\frac{\partial D}{\partial N} \approx \int_{\text{cycle } N} \dot{D} dt = G(D(N)) \quad (9)$$

As for the damage rate expression (Eq. 3), the cycle-based damage rate  $G$  at cycle  $N$  is a function of the expression of the damage reached  $D(N)$ . The integration of the damage growth, between cycles  $N$  and  $N + \Delta N$ , is approximated according to a trapezoidal scheme (Eq. 10).

$$D(N + \Delta N) \approx D(N) + \frac{1}{2} [G(D(N)) + G(D(N + \Delta N))] \Delta N \quad (10)$$

An explicit algorithm is implemented here. The Euler forward predicted value of the damage value at the cycle  $N + \Delta N$  is inserted into the term  $G(D(N + \Delta N))$  of (Eq. 10).

$$D(N + \Delta N) \approx D(N) + \frac{1}{2} \{G[D(N)] + G[D(N) + G(D(N)) \Delta N]\} \Delta N \quad (11)$$

In order, to avoid error propagation in the extrapolation scheme, a new parameter  $\eta$  is introduced to build a self driven algorithm (Peerling et al. 2000). The  $\Delta N$  value is calculated according to the relation (Eq. 12).

$$\Delta N = \eta \frac{\partial D}{\partial G} \quad (12)$$

The self driven algorithm program has been validated with a 2D simulation of the uniaxial constant strain amplitude test condition. To do so, the damage calculated with the program implemented in the finite element code, is compared to the analytical result (Eq. 7).

## APPLICATION TO ASPHALT CONCRETE FATIGUE

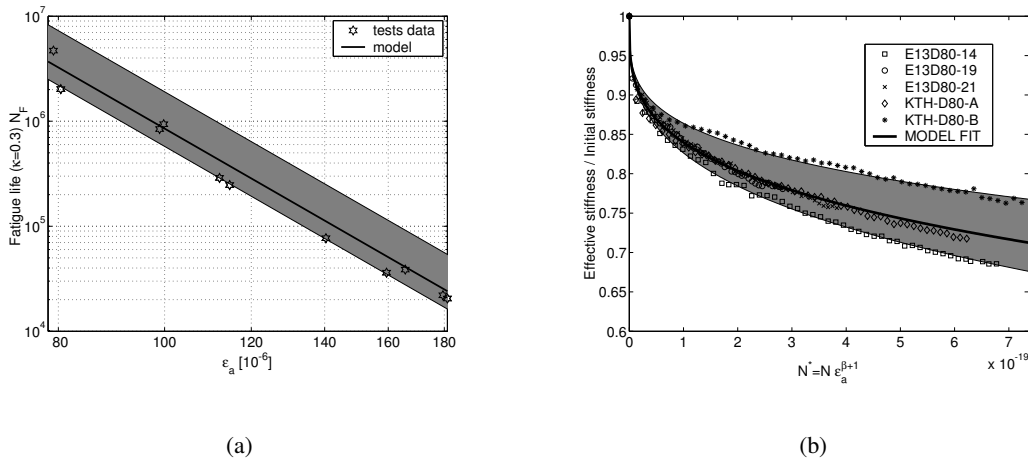
The fatigue damage model is used to describe the stiffness evolution of asphalt concrete specimens during fatigue tests. The experimental results presented in this section are obtained in the fatigue part of the current asphalt concrete testing and evaluation RILEM program (Partl et al. 2000). Testing conditions are set to a 10 Hz loading frequency and a  $10^\circ\text{C}$  controlled temperature. The tested material is a 0/6 mm dense-graded asphalt mix containing 6.4 % of 50/70 penetration grade pure bitumen.

### Model parameter identification

The principle of the  $\alpha$ ,  $\beta$  and  $C$  parameter identification is to adjust the model to the uniaxial displacement controlled fatigue test results.

The  $\beta$  parameter is directly calculated (Eq. 8) from the slope of the Whöler curve (Fig. 2(a)). The slope value determined by a least square method is rounded to 6 ( $\pm 0.6$  for the eleven tested specimen). This fixes the  $\beta$  value to 5.

Then, the expression of the damage  $D$  function of the number of cycle  $N$  (Eq. 7) is adjusted on the test results, setting the  $\alpha$  and  $C$  value. To be as close as possible to the isothermal process assumption, the model is calibrated using strain amplitude tests fixed at  $80 \cdot 10^{-6}$ . In these test conditions, the constant strain amplitude  $\varepsilon_a$  can be slightly different from a test to another and a transformation along the x axis is performed to take exactly into account each test amplitude. The damage expression (Eq. 7) is transformed into an  $\varepsilon_a$  expression using the transformation  $N^* = N\varepsilon_a^{\beta+1}$ . In the  $D - N^*$  diagram (Fig. 2(b)) the  $\alpha$  and  $C$  values are fitted by a least square method.



**FIG. 2. (a) Whöler curve for uniaxial tension-compression test (b) Damage model fit superposed to  $80 \cdot 10^{-6}$  constant strain amplitude experimental damage measurements (0-6 mm asphalt concrete containing 6.4% of 50/70 pen bitumen 10 Hz -  $10^\circ\text{C}$ )**

In order to take into account the fatigue data scattering, three parameter sets are estimated (Table 1). The first one called "average", corresponds to a fit using all the available data. The two others, called "up" and "down", represent the fitting obtained for the both extreme damage evolutions observed in the set of tests (Fig. 2(b)). In both figures (Fig. 2) the bold line is obtained with the "average" set and the extreme curves, which define the grey filled zone, are obtained with the parameters fitted using the extreme experimental values.

In this study, unlike the classical choice adopted for fatigue processes by (Alimami 1988), (Paas et al. 1993) and (Peerling et al. 2000), the coefficient  $\alpha$  is negative, which controls the correct evolution in the beginning of the damage evolution. Then parameter  $C$  does not directly affect the shape of the evolution, but leads to a correct fit on experimental results.

The tension-compression fatigue test device does not allow data acquisition during phase *III*. The damage model identification is then performed according to phases *I* and *II*.

Parameter	"up" fitting	"average" fitting	"down" fitting
$\alpha$	-2.73	-2.41	-2.20
$C [10^{16}]$	0.95	3.38	6.88

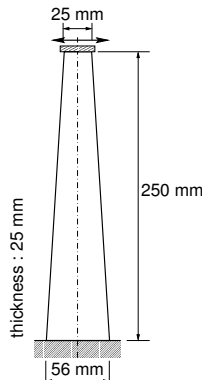
**TABLE 1. Damage evolution parameters  $\alpha$  and  $C$  ( $\beta = 5$ ) fitted on constant strain amplitude  $80 \cdot 10^{-6}$  fatigue tests (0-6 mm asphalt concrete containing 6.4% of 50/70 pen bitumen 10 Hz -  $10^\circ\text{C}$ )**

### Bending fatigue simulations

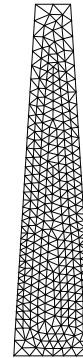
The numerical implementation of the damage model has been applied to the 2D problem of the French standardized fatigue test (Fig. 5). The large base of the specimen is clamped. A sinusoidal horizontal displacement with a constant amplitude is applied to the top small edge.

For the asphalt concrete presented, the Young modulus is set to the corresponding value of the complex modulus at  $10^\circ\text{C}$  and 10 Hz i.e.  $E=12000$  MPa and Poisson ratio  $\nu = 0.35$ . The parameters  $\alpha$ ,  $\beta$  and  $C$  of the damage evolution law, are taken from the identification performed on uniaxial tests results, for the same material and for the same tests conditions (Table 1).

The model parameter that remain unknown, is the material characteristic length  $l_c$  of the nonlocal model. At present, its value is not well assessed for asphalt concrete materials. So it has been set according to the estimated value for cement concrete, at three time the maximum aggregate size which yields  $l_c=18$  mm (Bazant and Pijaudier-Cabot 1989). The finite element discretization is given in (Fig. 5). Linear interpolation functions are used for the displacements. The calculation is done in plane stress conditions. The time integration is carried out with the explicit cycle based scheme (Eq. 10 and 11) and the adaptative step size selection algorithm with  $\eta=5 \cdot 10^{-2}$ .



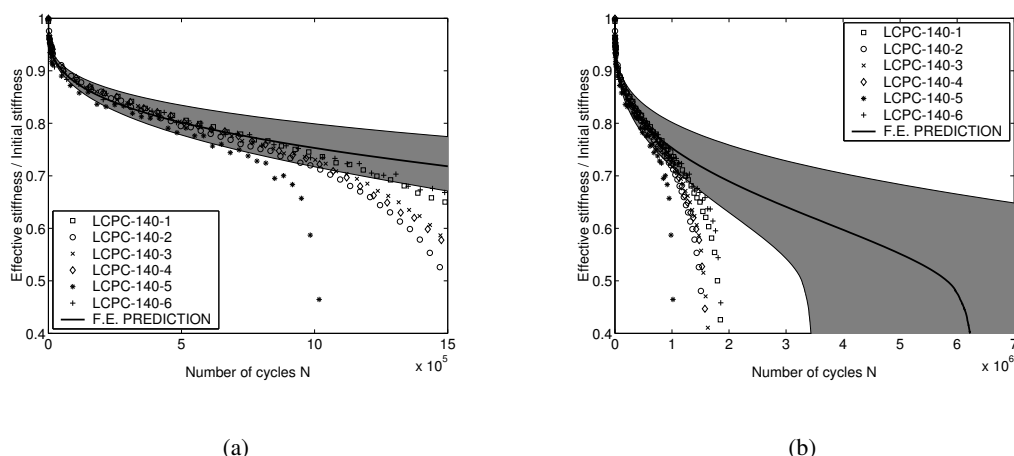
**FIG. 3. Geometry and loading of the cantilever bending fatigue tests**



**FIG. 4. Mesh of the trapezoidal bending beam geometry**

In order to simulate bending fatigue tests involving as little thermal effect as possible we consider, as for the parameters identification, loading at small levels. The following results are obtained for a strain amplitude of  $140 \cdot 10^{-6}$ . From the both points of view of fatigue life and thermal regime, this loading level leads to a range equivalent to the uniaxial  $80 \cdot 10^{-6}$  strain amplitude used for the model calibration.

The specimen stiffness (divided by the initial one) is plotted as a function of the number of loading cycles in (Fig. 5).



**FIG. 5. Simulation on bending tests conditions (a) To first phases I and II. (b) The complete damage process (0-6 mm asphalt concrete containing 6.4% of 50/70 pen bitumen 10 Hz - 10°C)**

Three simulation results are superposed to the experimental data. The simulation with extreme parameters (Table 1) defines the grey zone which includes the continuous line associated to the simulation with the average parameters set. The stiffness decrease, calculated during both beginning phases I and II shows good prediction.

The modelling exhibits a phase III regime (Fig. 5(b)) with an acceleration of the failure process. It is directly related to the growth of the zone where the elements are completely damaged. The transition between phase II and phase III is representative of a transition between a diffuse and localized damage. The diagram (Fig. 5(b)) shows that average parameters do not yield a good estimation of this transition in terms of number of cycles. The grey zone evolution shows as well, a great dependance of the damage parameter on the end of the fatigue process.

## CONCLUSION

An elasticity based nonlocal continuum damage model is presented for fatigue failure of asphalt concrete. The associated 3D finite element implementation has been described. An application to displacement controlled fatigue tests on asphalt concrete specimens presents the modelling capabilities.

Uniaxial tension-compression conditions allow to identify damage model parameters. In these conditions, the resulting model predicts the fatigue process until macrocrack propagation.

Fatigue damage prediction ability has been tested on the trapezoidal cantilever fatigue bending tests. The simulations show a good description of the continuous damage process which covers about 25% drop of specimen stiffness.

These first results point out the relevance of the presented damage model for asphalt mixes fatigue. Simulation of other test geometries can be carried out to widen its relevance field. The prospect of this work is to improve the modelling in order to better describe transition relative to the macrocrack failure mechanism.

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