

INFLUENCE OF YIELD SURFACE DISTORTION ON PREDICTION OF MULTIAXIAL LOADING CONDITIONS/

Dr. Muthanna H.S*.

Abstract

Although distortion of yield surface is highly recommended for enhancement of simulations of multiaxial loading conditions, this effect has not been investigated up to now. In this work, therefore, yield surface distortion is incorporated in the constitutive equations scheme and its effect is scrutinized based on simulations for two special categories of loading histories. So as to create assessment discussion, three dissimilar kinematic hardening rules are utilized. It is shown that, irrespective of different hardening rules, the effect of yield surface distortion is noteworthy under multiaxial loading conditions in which steady primary axial stress is combined with cyclic shear strain. On the other hand, for multiaxial loading conditions in which primary axial stress is cyclic simultaneously with secondary shear strain, it is shown that the yield surface distortion has a negligible effect as long as mean stress value is equal to zero whereas this effect becomes significant with the increase of that value. Other imperative results are presented and discussed critically.

Keywords: Cyclic plasticity, multiaxial loading, hardening rules, constitutive equations, yield surface distortion.

1- Introduction

In many engineering applications, structural components can be subjected to cyclic loads within the

inelastic domain, which can produce a cumulative inelastic deformation. In such case, it can lead to collapse due to accumulation

*Lecturer, Dept. of Mechanical Engineering, University of Technology, Iraq

of deformation due to fatigue. A good prediction of accumulation of deformation and of fatigue damage depends on the capacity of the constitutive model used in the analysis to be able to reproduce the material performance.

Consequently, to allow precise modeling of material behavior, numerous constitutive models have been developed and modifications or new formulation are presently being proposed. Up till now, such models have exhibited quite good agreement with uniaxial loading conditions (for instance, see review papers [1-2]). These models, however, may have critical problems in simulating multiaxial loading conditions particularly accumulated inelastic strain even under classical multiaxial loading conditions [1-5].

In order to overcome such critical problems, Abdel-Karim [3] recommended that, the capabilities of a certain class of models can be highly improved by including distortion in yield surface into their constitutive equations. Also, Corona et al. [4] indicated that, inaccurate representation for a series of multiaxial loading histories is attributed mainly to distortion of yield surface. In addition, Portier et al. [5] pointed out that; introduction of distortion of yield surface in the formulation of constitutive equations may improve the capacity of a number of models to simulate multiaxial ratchetting. Furthermore, Bari and Hassan [6] emphasized that, towards achieving a robust model for ratchetting simulation, the incorporation of yield surface shape change into the modeling scheme seems to be a more rewarding approach. It is evident that, distortion of yield surface is extremely recommended to improve results. However, this effect has not been investigated so far.

In this work, accordingly, distortion of yield surface is introduced in the formulation of constitutive equations and its effect on simulations is examined under different multiaxial loading conditions with emphasis on ratchetting experiments. For this purpose, more general demanding tests performed by Portier et al. [5] are utilized. Effect of yield surface distortion is specified on the basis of simulations of these tests using three different kinematic hardening rules. It is shown that, the yield surface distortion has a great influence on prediction of specific multiaxial loading conditions particularly, the most contrasting problem of the Armstrong-Frederick hardening rule, which is

overestimation of ratchetting strain, is conquered as taken into consideration effect of yield surface distortion. On the other hand, in multiaxial loading histories in which primary axial stress is cyclic with secondary shear strain, it is shown that, the yield surface distortion has negligible effect when mean stress value is equal to zero but this effect becomes notable with the increase of that value.

2- Constitutive Equations

As it is well known, any plasticity or viscoplasticity model depends mainly on three concepts: a yield criterion, a flow rule, and a hardening rule. The yield criterion specifies the condition for the onset of plastic flow. In general, the yield surface could change in shape, size, and center location as a result of plastic deformation. The change in the shape of yield surface is called distortion of yield surface. :

With the intention of introducing the distortion of yield surface in formulation of constitutive equations and as an extension to Baltov and Sawczuk model [7-8], it is assumed that elastic domain could be defined as:

$$\sigma_{,ll} = \sqrt{\frac{3}{2} \frac{3C_{pll}}{(1+(3/2)\beta \dot{P})}} (s - a) : (s - a) \dots\dots\dots(1)$$

where, s is the deviatoric stress tensor, a is the back stress in the deviatoric space which represents the center of the yield surface and

P denotes the equivalent inelastic strain increment and is defined as:

$$\dot{P} = \sqrt{\frac{3}{2} \frac{\ddot{P} : \dot{P}}{\dot{\varepsilon} : \varepsilon}} \dots\dots\dots(2)$$

The components of the fourth order tensor C_{ijkl} are plastic strain dependent:

$$C_{ijkl} = I_{ijkl} + \beta \varepsilon_{ij}^n \varepsilon_{kl}^n \dots\dots\dots(3)$$

$$I_{ijkl} = \frac{1}{2} \left(\delta_{ik}\delta_{jl} + \delta_{il}\delta_{jk} - \frac{2}{3}\delta_{ij}\delta_{kl} \right) \dots\dots\dots(4)$$

The term β in the equations 1&3 is the distortion coefficient, which describes the changes of the elastic domain due to plastic deformation. When $\beta = 0$ the distortion effect vanishes and these equations reduce to the classical von Mises' criterion, i.e.

$$\sigma_{eff} = \sqrt{\frac{3}{2} (s - a) : (s - a)} \dots\dots\dots (5)$$

From now on a bold letter represents a second order Cartesian tensor, and a colon (:) between two tensors denotes their inner product. The symbol $\langle \rangle$ denotes the Ma Cauley bracket, (i.e. $\langle x \rangle = 0.5(x + |x|)$) and H stands for the Heaviside step function $H(x) = 1$ if $x \geq 0$ and $H(x) = 0$ if $x < 0$

Under the framework of a kinematic yield function, a hardening rule specifies the translation of the yield surface, or the evolution of the back stress a . In order to specify such evolution, the basic Armstrong and Frederick (AF) [9] nonlinear hardening rule is introduced based on the concept of strain hardening and dynamic recovery. A major modification to that rule was made by Chaboche and his coworkers [10-11] who proposed that the total back stress was decomposed into a number of additives parts (M) and that each part of the back stress follows the AF format:

$$a = \sum_{i=1}^M a_i \dots\dots\dots (6)$$

$$\dot{a}_i = \zeta_i \left(\frac{2}{3} r_i \dot{\epsilon}^p - a_i \dot{p} \right) \dots\dots\dots (7)$$

Where a_i^* is the deviatoric part of back stress component i , denotes the inelastic strain increment, ζ_i and Γ_i ($i = 1, 2, \dots, M$) are material constants.

To overcome the excessive ratchetting predicted by the AF model. Ohno and Wang (OW) [12] modified the AF model by assuming that the dynamic recovery of a_i becomes active when its magnitude $\overline{a_i}$ reaches a critical value r_i . They thus derived the following equation:

$$\dot{a}_i = \zeta_i \left(\frac{2}{3} r_i \dot{\epsilon}^p - H(f_i) \left\langle \dot{\epsilon}^p : \frac{a_i}{\bar{a}_i} \right\rangle a_i \right) \dots\dots\dots (8)$$

where f_i and $\bar{a}_i$ are defined as:
respectively [12].

$$f_i = \bar{a}_i^2 - r_i^2 \quad \text{and} \quad \bar{a}_i = \sqrt{\frac{3}{2} a_i : a_i}$$

Jinag and Selitoglu (JS) [13] suggested one more adaptation for the AF model by introducing a certain power function in the dynamic recovery term of Eq. (7) as

$$\dot{a}_i = \zeta_i \left(\frac{2}{3} r_i \dot{\epsilon}^p - \left(\frac{\bar{a}_i}{r_i} \right)^{m_i} \dot{p} a_i \right)$$

Where m_i ($i = 1, 2, \dots, M$) are constants.

Incidentally, the above three hardening rules will be utilized to specify changes of back stress a through out this work. The specific choice for these hardening rules is ascribed to their different features even for uniaxial change of back stress under tensile loading (Fig. 1). In addition, these hardening rules have different capabilities in simulating uniaxial as well as multiaxial ratchetting experiments [2]. For instance, the AF model overestimates uniaxial ratchetting in contrast with the OW model, which provides zero accumulation of strain in uniaxial ratchetting. Moreover, material constants m_i , in the

JS model are adjustable to fit experiments since larger ratchetting is obtained with smaller values of m ; For more details about these models, see [2, 12, and 13].

The yield surface distortion and kinematic hardening rules introduced above can be applied for both rate-independent plasticity as well as viscoplasticity. In this work, however, viscoplasticity is dealt with. Therefore, the following viscoplastic equation is employed

$$\dot{\varepsilon}^p = \frac{3}{2} \dot{\lambda} (\sigma_{\text{eff}})^{b-1} (s - a)$$

Where A and b are viscous material constants

Finally, for the elastic component of strain, e , Hooke's law with Young's modulus and Poisson's ratio, ν , is assumed i.e.

$$\varepsilon^e = \frac{1 + \nu}{E} \sigma - \frac{\nu}{E} (\text{tr}(\sigma)) \mathbf{I}$$

Where σ , and $\mathbf{I}$ indicate stress tensor and the unit tensor of rank two, respectively, and tr denotes the trace. Since small strain is concerned in this work, total strain, ε , is considered as sum of elastic

strain, e , and inelastic strain, p : $\varepsilon = \varepsilon^e + \varepsilon^p$

3. Experimentsof Portier et al [12]

These experiments were carried out on 316 stainless steel tubular specimens at room temperature under two categories of loading paths as shown in Fig 2(a & b). In the first category, combined effect of steady primary axial stress and a cyclic shear strain γ , were performed with the prescribed values for σ and shown in Fig. 2a. In the second category (Fig. 2b), simultaneous cyclic axial stress (primary loading) and the shear strain (secondary loading), were performed.

In the second loading category, two "bowtie" type paths were considered [4, 5]. Each of the two paths consisted of three sequences at different mean stress (Fig. 2b). PATH-1 started with an increasing of the axial stress (OABCD), conversely, PATH-2 started with a decreasing of the axial stress (OBADC). In PHASE-1, a zero mean stress was imposed for 25 cycles. Next, the shape of the path is kept but a mean stress of 55 MPa was imposed for 100 cycles (PHASE-2). Finally, the shape of the path kept unchanged and the mean stress was suppressed at zero stress for 50 cycles (PHASE-3). It should be noticed that, such second category of loading histories is regarded as a demanding test to the capabilities of various suggested or newly developed hardening rules [5]. Therefore, these particular experiments are utilized in this work as a critical evaluation for the effect of yield surface distortion on multiaxial loading simulations.

4. Material Parameters

As indicated in many works, 316 stainless steel at room temperature exhibits significant viscoplasticity but almost negligible isotropic hardening especially when small strain range is concerned [5,8]. Therefore isotropic hardening effect, which provides changes in yield surface size, is neglected in this work.

Under, monotonic tensile loading, the introduction of yield distortion effect in Eq. (1), has a feature that this model gives the same results as the classical von Misses formulations. Moreover.

Eq.(10) reduces to $\dot{\epsilon}^p = \dot{\lambda} (\sigma - \bar{\alpha})^b$ Therefore, tensile stress under constant strain rate can be expressed approximately as

$$\sigma = \left(\frac{\dot{\epsilon}}{A} \right)^{\frac{1}{b}} + \bar{\alpha}$$

Where represents the change of back stress under monotonic tensile loading .Since, for the OW model, $C_{\{i\}}$ and $r_{\{i\}}$ are illustrated in Fig.3 as [14]

$$\zeta_i = \frac{1}{\varepsilon_{(i)}^p} \quad r_i = \left(\frac{\alpha_{(i)} - \alpha_{(i-1)}}{\varepsilon_{(i)}^p - \varepsilon_{(i-1)}^p} - \frac{\alpha_{(i+1)} - \alpha_{(i)}}{\varepsilon_{(i+1)}^p - \varepsilon_{(i)}^p} \right) \varepsilon_i^p$$

Table1.Material parameters for 316 stainless steel at room temperature.

Elastic

Elastic	$E = 1.96 \times 10^5$ v = 0.33	
Viscoplastic	$A = 1 \times 10^{-21}$, $b = 7.5$	
Kinematic Hardening (M = 10)	$r_1=19, \zeta_1=10000$ $r_2 = 17, \zeta_2 = 5000$ $r_3 = 16, \zeta_3 = 2500$ $r_4 = 18, \zeta_4 = 1000$ $r_5 = 18, \zeta_5 = 400$	$r_6=15, \zeta_6=183.5$ $r_7 = 8, \zeta_7 = 100$ $r_8 = 10, \zeta_8 = 50$ $r_9 = 11, \zeta_9 = 33.3$ $r_{10} = 80, \zeta_{10} = 25$

Stress (Mpa), Strain(mm/mm),time(s)

Where $\varepsilon_{(0)}^p=0$ and $\alpha_{(0)}=0$ Then the values, $\zeta_{\{i\}}$ and $\Gamma_{\{i\}}$ as well as viscous material constants A and b utilized in Eq. (10) can be adjusted to fit the tensile experiments shown in Fig. 4 Following this procedure, the material parameters listed in Table (1) are obtained and used for different models.

5. Application to Experiments and Discussion

Simulations using the three hardening rules utilized to describe changes of back stress are compared with experimental results. The three kinematic hardening rules are the initial AF model represented by Eq. (7); the OW model refers to Ohno-Wang model, Eq. (8); and finally the JS model indicates the model suggested by Jiang Schitoglu, Eq. (9). The results of simulations along with the experimental results are presented in Figs. (5-10). Along these figures. it should be noted that, β represents the distortion parameter which specify changes in yield surface. When $\beta=0$, the change of yield surface vanishes and classical von Mises criterion is obtained.

Let us first discuss experiments of first loading category shown in Fig. 2a. A series of six experiments are considered in which values of primary stress σ and secondary shear strain γ are varied as indicated in Fig. 2a. Figures (5-7) represent comparison between experimental results and simulations obtained with the three models. The simulations are given with respect to different values for distortion parameter β . It is clear that prediction of this loading category is very sensitive to the incorporated hardening rule especially when distortion parameter $\beta=0$.

To begin with. let us discuss predictions from the AF model (Fig. 5) It is clear that, as a normal tendency, the initial AF model ($\beta=0$) overestimates experiments excessively. This observation is found irrespective of different values for loading conditions (σ and γ). It is also clear that the predicted values are significantly changed with values of distortion parameter β hence, larger value for β led to more decrease in the predicted ratchetting. Moreover, experimental results under different loading conditions can be simulated qualitatively and quantitatively, as long as suitable value for β is employed. The value of β . however, depends on loading conditions where larger value of β is suitable to predict lower ratchetting accompanied with small values for primary stress and secondary shear strain. Therefore, we can conclude one of the most important results obtained in this work, i.e.

the excessive multiaxial ratchetting predicted by the AF model can be greatly restrained if yield surface distortion is introduced in the formulation of constitutive equations.

Now let us discuss predictions from the OW model shown in Fig. (6). This model provides complete closure of stress strain hysteresis loops under uniaxial cyclic loading resulting in zero uniaxial ratchetting strain. Even though, the model over-predicts experiments even that of small values for primary stress σ and secondary shear strain γ . The predictions of the model, however, can be modified with including yield surface distortion. Therefore, we can conclude that yield surface distortion has a significant effect not only on the AF model but also on the OW model, which has the capacity to provide the lowest predictions for multiaxial as well as uniaxial ratchetting.

To end with the first loading category, let us consider predictions from the JS model (Fig.7). It is obvious that, results of the initial model ($B=0$) recline between those of the AF and the OW models. Moreover, the predicted values are also affected by distortion parameter β . As a matter of fact the predicted values can be smaller with larger values for adjustable parameters m . However, even for very large values of m , the JS model provides results greater than those of OW model. This is because the accumulated plastic strain increment is introduced in the recovery term of the JS model rather than maximum plastic strain increment that introduced in the OW model. The important point here is that the distortion of the yield surface has an obvious effect on hardening rules either accumulated plastic strain increment or maximum plastic strain increment is introduced in dynamic recover term.

Now let us consider the second loading categories (Fig. 2b). Figures 8-10 show comparison between maximum axial strain over each cycle collected from experiments and predicted by simulations. To highlight the effect of yield surface distortion, simulations for different values of distortion parameter β are also given in the figures.

As illustrated in Figs. 8(a & b). the predictions obtained from the initial AF model ($\beta=0$) are over estimates experiments too much (about 45 times experiments value at the end of phase 2). The important finding in this figure is that for both PATH-1 and PATH-2,

during PHASE-1, the effect of distortion is almost negligible. On the contrary, during PHASE-2 in which mean stress increases, this effect becomes noteworthy since predicted ratchetting decreases and becomes only twice experimental values at the end of this phase for PATH- 1. These results indicate that, yield surface distortion has negligible influence as long as initial mean stress is equal to zero. It is also evident that, the model with the distortion effect can describe qualitatively PHASE-3 of the test, i.e. almost no recovery of axial strain.

With respect to the OW and JS models (Figs.9-10), both of them provide a very good prediction of PHASE-1 for PATH-1 and PATH-2. However, the OW model provides saturated curves during PHASE-2 in contrast to the IS model, which predicts experimental results qualitatively during the same phase. Besides, the two models fail to describe correctly PHASE-3 of the tests since they provide a complete recovery of the axial strain unlike the experimental results. Finally, irrespective of different values for distortion parameter β . it was found that the effect of the yield surface distortion is almost negligible during PHASE-1 and PHASE-3 but its influence becomes notable only during PHASE-2. Especially, the incorporation of the yield surface enables the JS model to simulate experimental results fairly well at the end of this phase.

These results indicate that, only the yield surface distortion incorporated with constitutive equations cannot predict quantitatively such complex multiaxial loading category. However results obtained from the AF model indicate that the accumulated plastic strain increment in recovery term play a pivotal key to simulate such loading category. Moreover when power function is introduced in recovery term (JS model) the model fails to simulate PHASE-3. In

addition, similar tendency between the OW and the JS models in providing complete recovery of axial strain during PHASE-3 indicates that, capture for activation of the dynamic recovery term has an awful influence in simulating PHASH-3 in both PATII -1 and PATH-2. In other words, to simulate PHASE-3 the initial form for AF model should be utilized.

Another plausible line of consideration to simulate such loading category is to include the concept of non-hardening strain region.

Based on the above discussion, accumulated plastic strain increment is strongly recommended rather than maximum plastic strain increment to be included in that concept. Additionally, the initial kinematic hardening rule proposed by Armstrong-Frederick together with distortion of yield surface could provide a very good agreement with experimental results. This issue will be considered in future work.

6. Conclusions

In this work, in order to scrutinize the effect of yield surface distortion on prediction of multiaxial loading conditions, a specific form describes changes that may take place in yield surface shape is successfully employed in constitutive equations scheme. In addition, three different kinematic hardening rules depict evaluation of yield surface translation is utilized. The main results concluded are summarized as:

1. Distortion of great influence on prediction of yield surface has particularly prediction of multiaxial a multiaxial loading conditions ratchetting in which secondary shear strain is cyclic with constant axial stress.
2. Influence of yield surface distortion, irrespective of dissimilar kinematic hardening rules depends mainly on value of initial

mean axial stress in the case of multiaxial loading conditions in which axial stress and shear strain are cyclic simultaneously.

3. Accumulated plastic strain concept in recovery term of kinematic hardening rules rather than maximum plastic strain, associated with yield surface distortion, is superior in simulating multiaxial loading conditions.
4. Merely the incorporation of yield surface distortion into the constitutive equations scheme cannot overcome the whole problems associated with simulation of multiaxial loading conditions. Another plausible line of consideration is to introduce the concept of non-hardening strain region. Based on the current results, accumulated plastic strain increment rather than maximum plastic strain is strongly recommended to be utilized in this concept.

References

1. Ohno, N., "Recent Topics in Constitutive Modeling of Cyclic Plasticity and Viscoplasticity ", Applied Mechanics Reviews, Vol. 43, pp. 283-295, 1990.
2. Ohno, N., "Constitutive Modeling of Cyclic Plasticity with Emphasis on Ratchetting", International Journal of Mechanical Sciences, Vol. 40, pp. 251-261, 1998.
3. Abdei- Karim, M., "Prediction of Inelastic Behavior under Non-Proportional Loading Histories using Various Plastic and Viscoplastic Theories", M. Sc. Thesis, Cairo University, 1992.
4. Corona, E, Hassan, T., and Kyriakids, S., "On the Performance of Kinematic Hardening Rules in Predicting a Class of Biaxial

- Ratchetting Experiments", International Journal of Plasticity, Vol. 12, pp. 117-145, 1996.
5. Portier, L., Calloch, S., Marquis, D., and Geyer, P., "Ratchetting under Tension-Torsion Loadings: Experiments and Modeling", international Journal of Plasticity, Vol. 16, pp. 303-335, 2000.
 6. Bari, S., and Hassan, T., "Anatomy of Coupled Constitutive Models for Ratchetting Simulation" International Journal of Plasticity. Vol. 16, pp. 381-409, 2000.
 7. Baltov. A., and Sawczuk, A., "A rule of Anisotropic Hardening" Acta Mechanica, Vol. 1, pp. 71-81, 1965.
 8. Benallal, A., and Marquis, M., "Constitutive Equations for Non proportional Cyclic Elasto-Viscoplasticity", ASME. Journal of the Engineering Materials and Technology. Vol. 109, pp. 326-339, 1987.
 9. Armstrong, P.J., and Frederick, C.O., "A Mathematical Representation of the Multiaxial Bauschinger Effect", CEGB Report RD/B/N731, Berkeley Nuclear Laboratories, 1966.
 10. Chaboche, J.L., Dang Van, K., and Cordier, G., "Modelization of the Strain Memory Effect on the Cyclic Hardening of 316 Stainless Steel", Transaction of 5th international Conference on Structural Mechanics in Reactor Technology, Vol.L, Paper No. L11/3. North-Holland, Amsterdam, 1979.
 11. Chaboche, J.L., and Rousselier, G., "On the Plastic and Viscoplastic Constitutive Equation, Part I: Rules Developed with Internal Variable Concept", ASME Journal of Pressure Vessel Technology, Vol. 108, pp. 153-158, 1983.
 12. Ohno, N., and Wang, J.-D., "Kinematic Hardening Rules with Critical State of Dynamic Recovery", International Journal of Plasticity, Vol. 9. pp. 375-403, 1993.
 13. Jiang, Y., and Schilloglu, H., "Modeling of Cyclic Ratchetting Plasticity", ASME Journal of Applied Mechanics, Vol.63, pp 720-733, 1996.

14. Jiang, Y., and Kurath, P., "Characteristics of The Armstrong-Frederick Type Plasticity Models", International Journal of Plasticity, Vol. 12, pp. 387-415, 1996.

List of Symbols

A material constant used in viscoplastic equation (Eq. (10)).

a_{iv} deviatoric part of i th component of back stress. magnitude of a , defined as $\overline{a_{\{i\}}} = \sqrt{(3/2) * a_{\{i\}} / a_{\{i\}}}$

b material constant used in viscoplastic equation (Eq. (10)).

H the Heaviside step function, i.e., $H(x) = 1$ if $x \geq 0$ and $H(x) = 0$ if $x < 0$

M number of back stress parts.

ϵ^p accumulated inelastic strain.

$r_{\{i\}}$ material constants in kinematic hardening rules defined as Eq.(15).

$\mathbf{a}$ back stress tensor.

β coefficient of yield surface

distortion.

δ_{ii} kronecker's delta.

$\mathbf{p}$ plastic strain tensor.

γ shear strain tensor.:

$\mathbf{G}$ stress tensor.

material constants in kinematic

hardening rule defined as Eq. (14).

$\langle x \rangle$ the Macauley bracket, i.c., $\langle x \rangle = (x + |x|) / 2$.

Symbols other than those given above are defined where they

appear in the text.

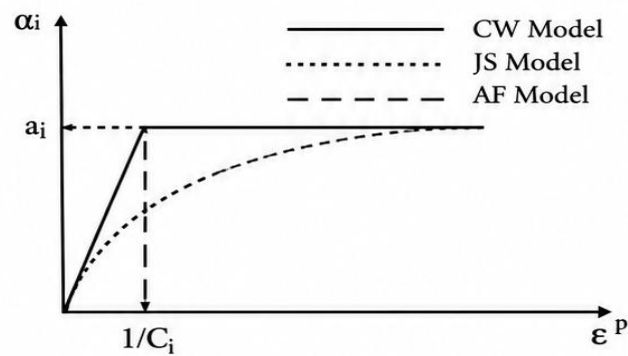


Fig. 1. Variations of the i^{th} part of uniaxial back stress uniaxial tensile loading.

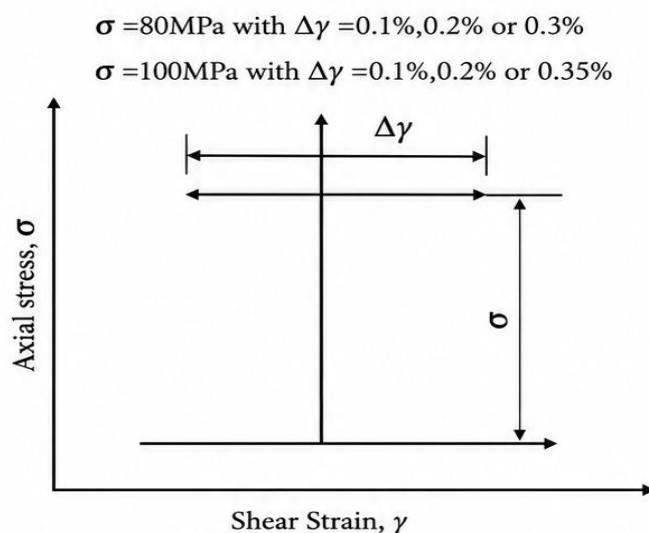


Fig.2-(a) First loading category

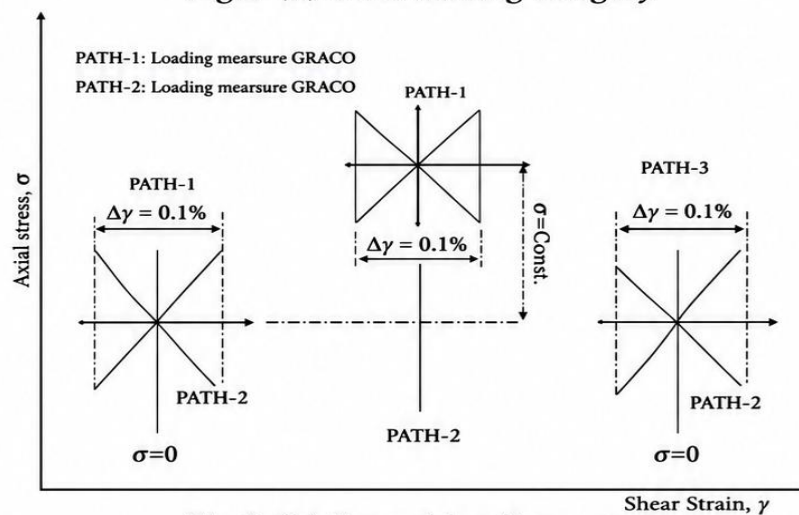


Fig.2-(b) Second loading category
Fig.2. Description of loading histories

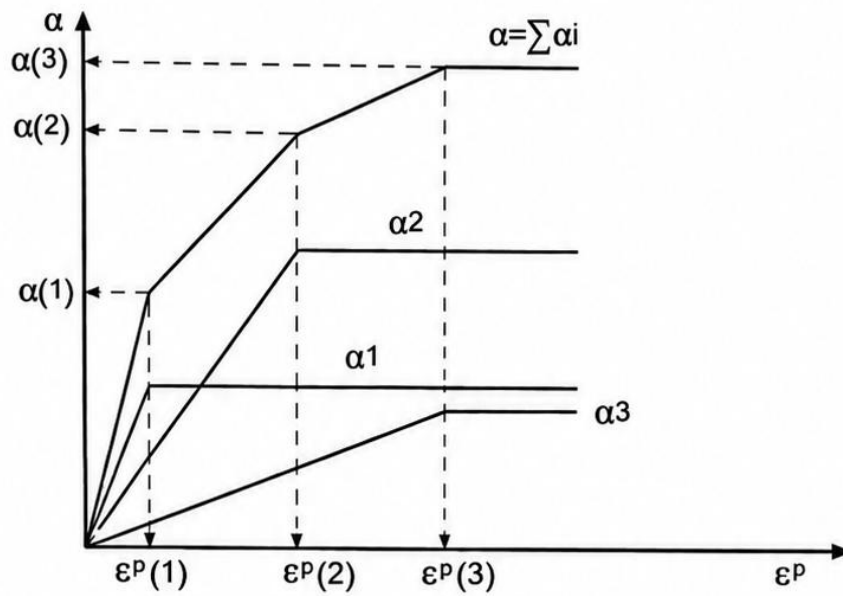


Fig.3. Evolution of back stress α and its components under uniaxial tensile loading.

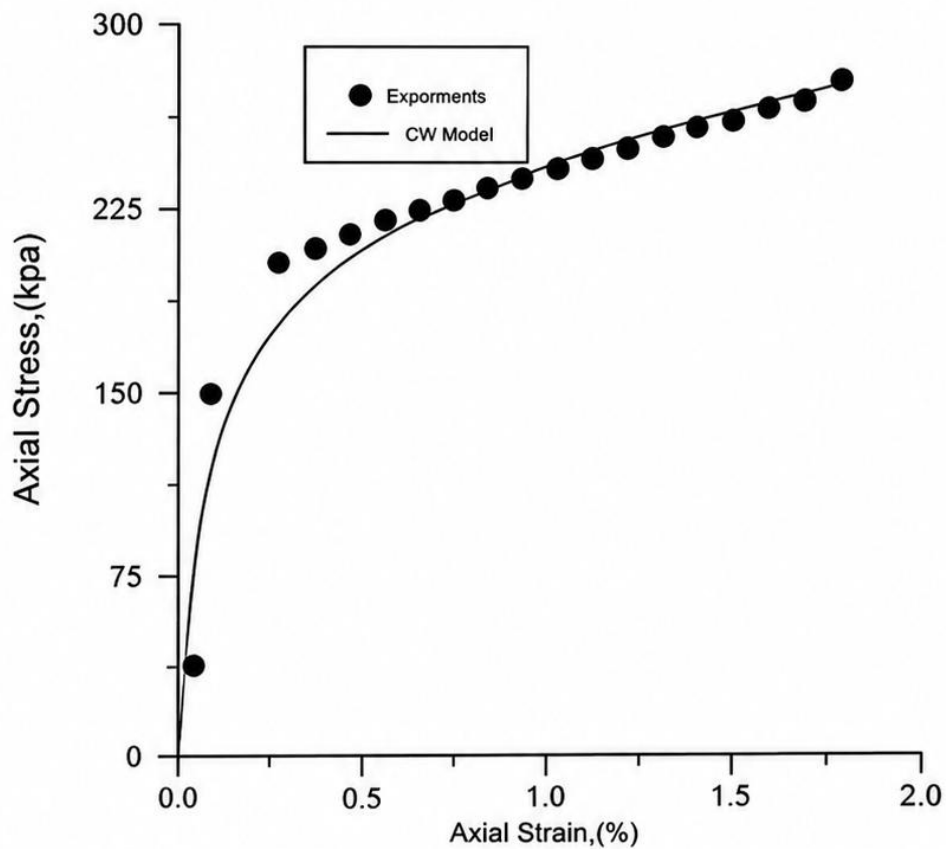


Fig.4. Tensile stress strain curve under constant strain rate of $3E-4 \text{ sec}^{-1}$

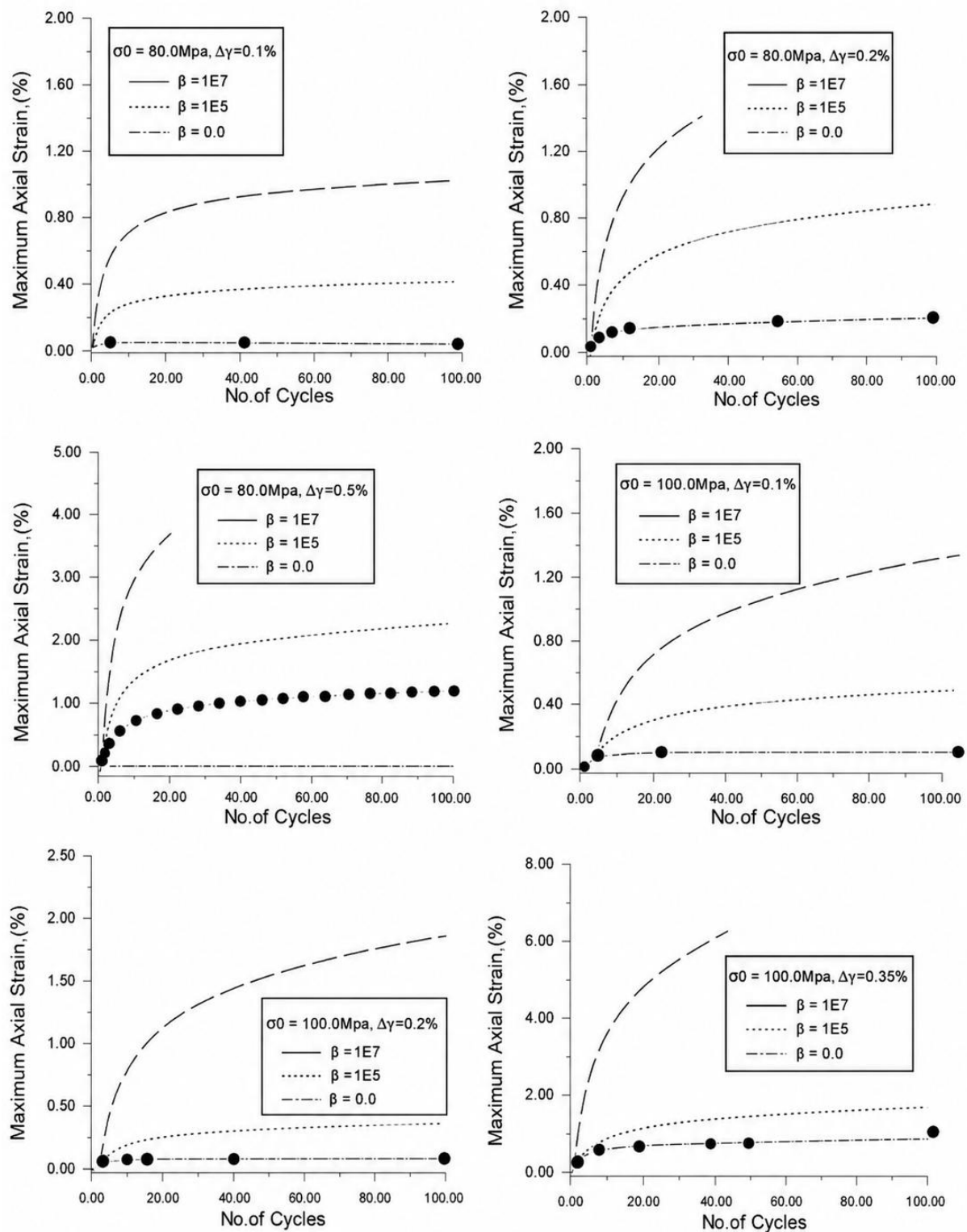


Fig.5. Comparison between experiments and predictions of first loading category from the AF model

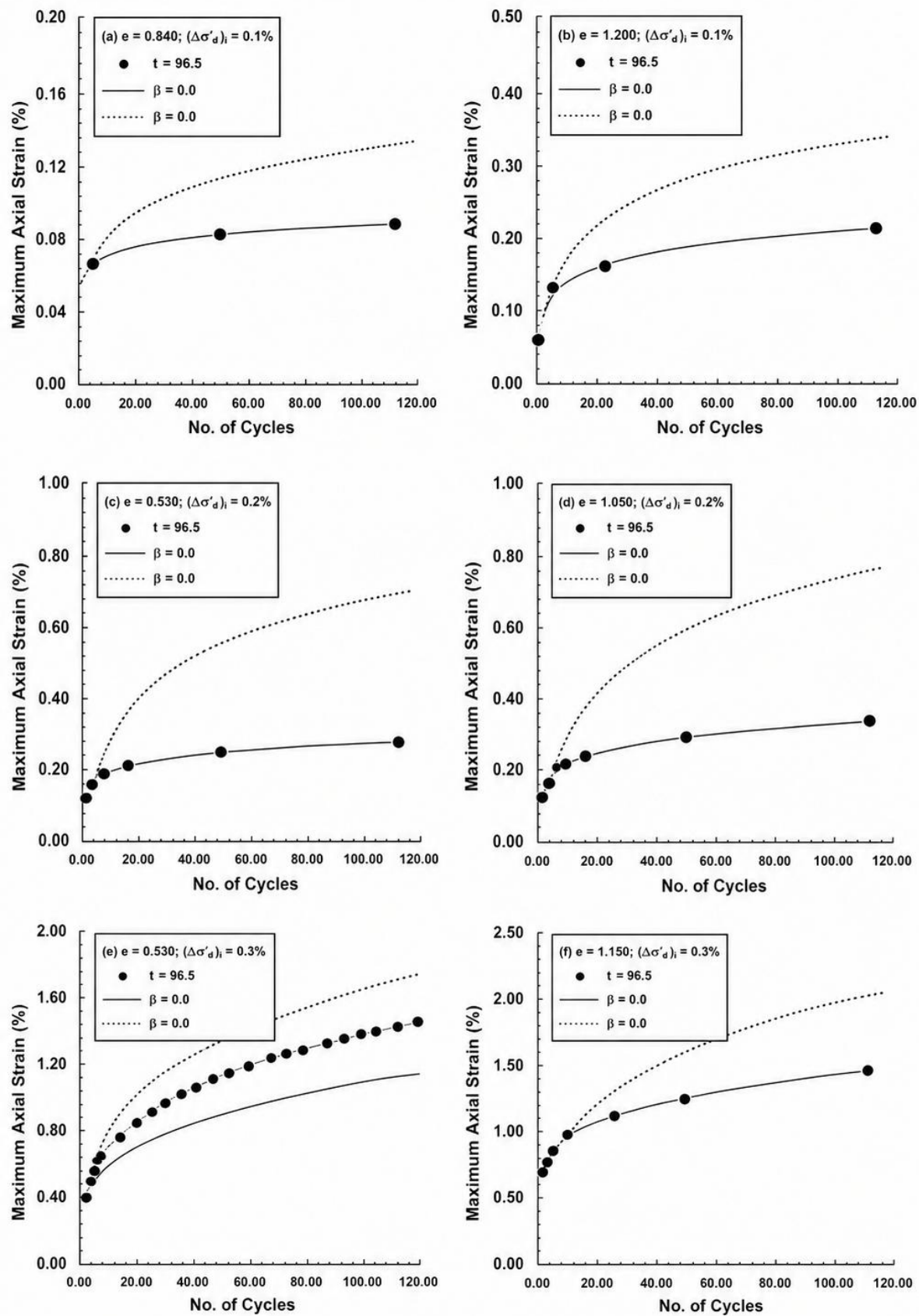


Fig.6. Comparison between experiments and predictions of first loading category from the OW model

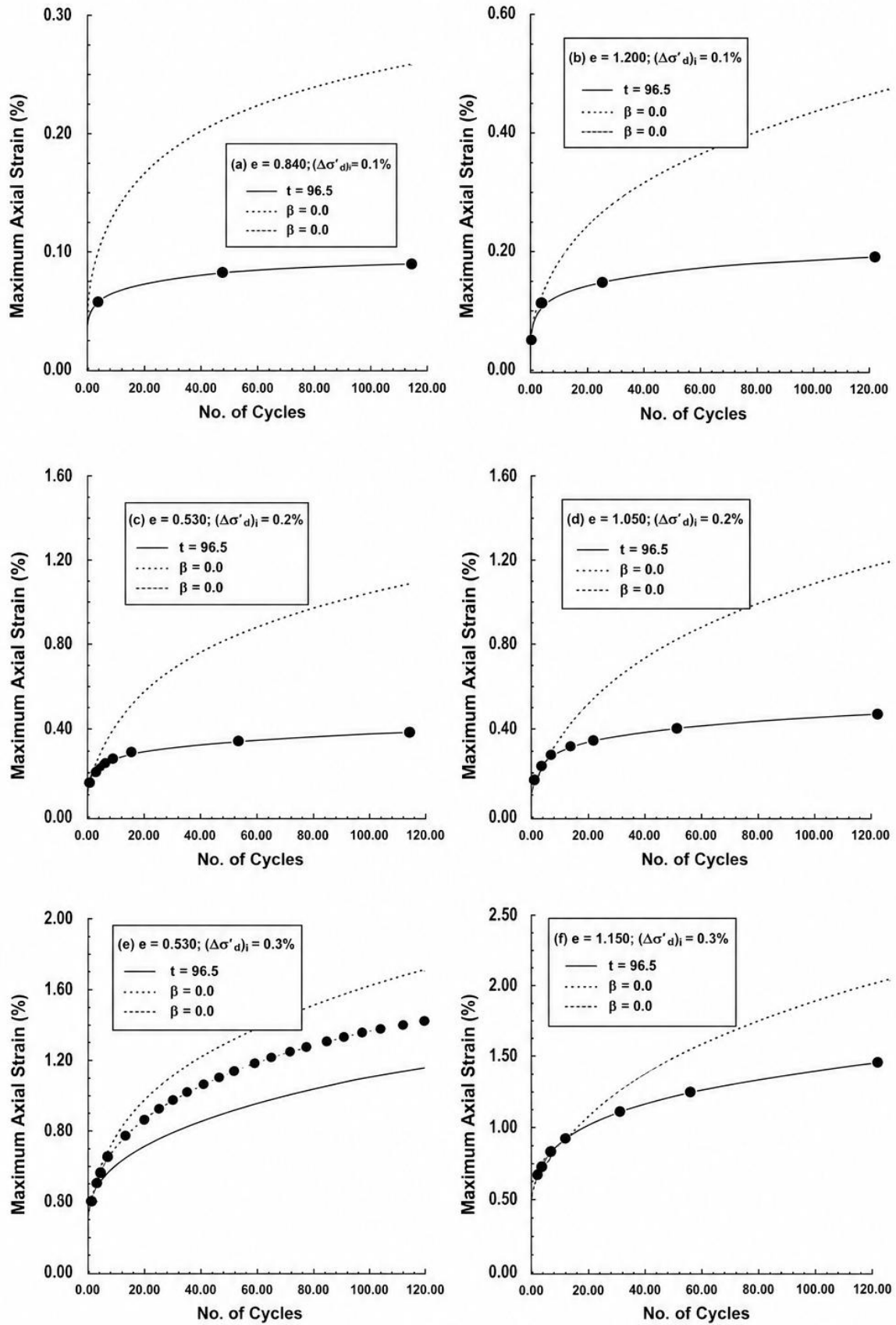
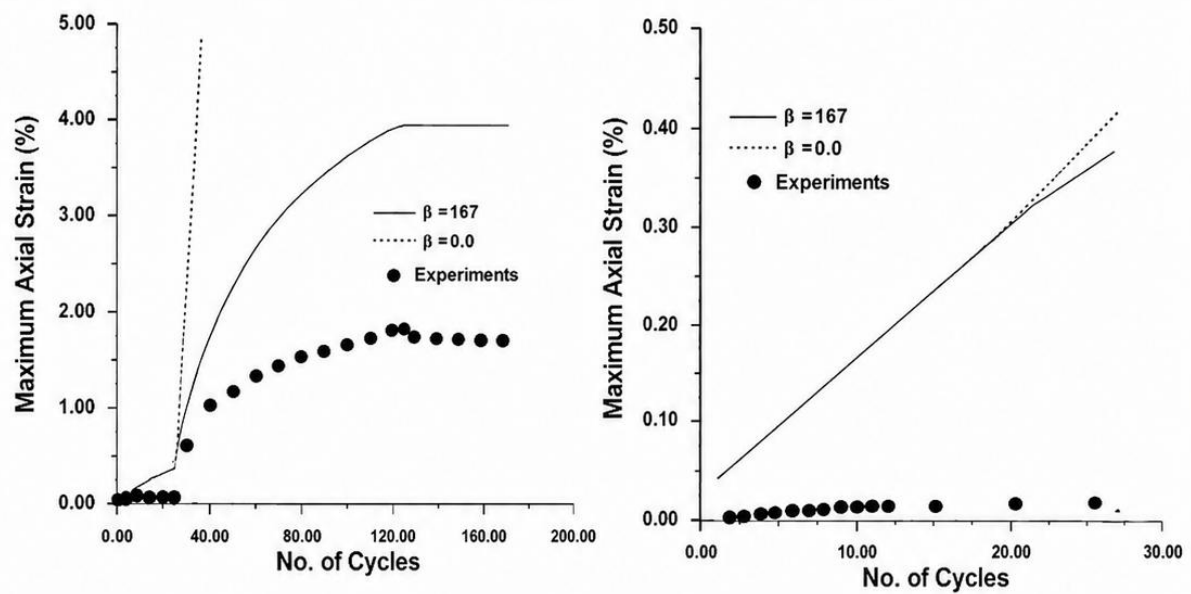
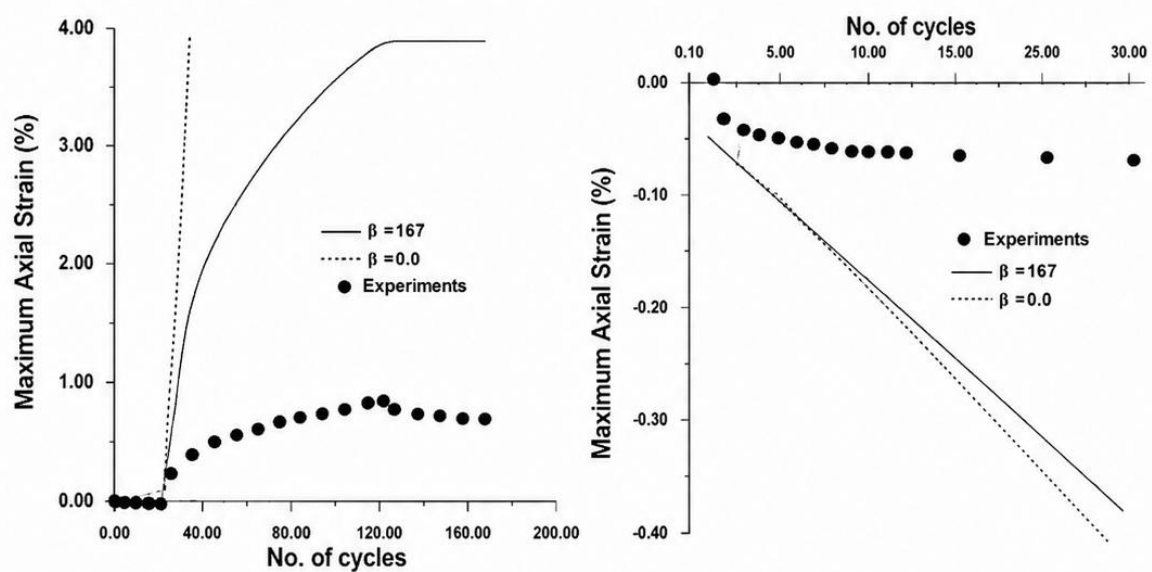


Fig.7. Comparison between experiments and predictions of first loading category from the JS model ($m_f=10$)



(a) PATH-1



(b) PATH-2

Fig.8. Comparison between experiments and predictions of second loading category from the AF model: (a) PATH-1, (b) PATH-2.

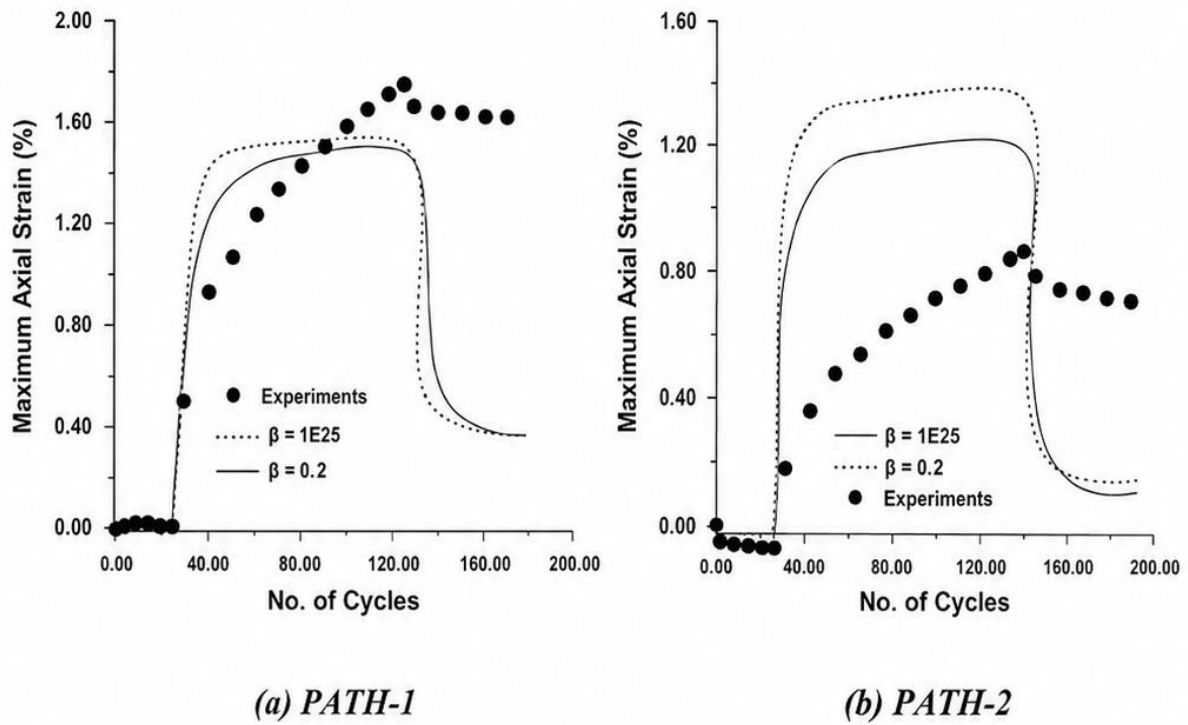


Fig.9. Comparison between experiments and predictions of second loading category from the OW model: (a) PATH-1, (b) PATH-2.

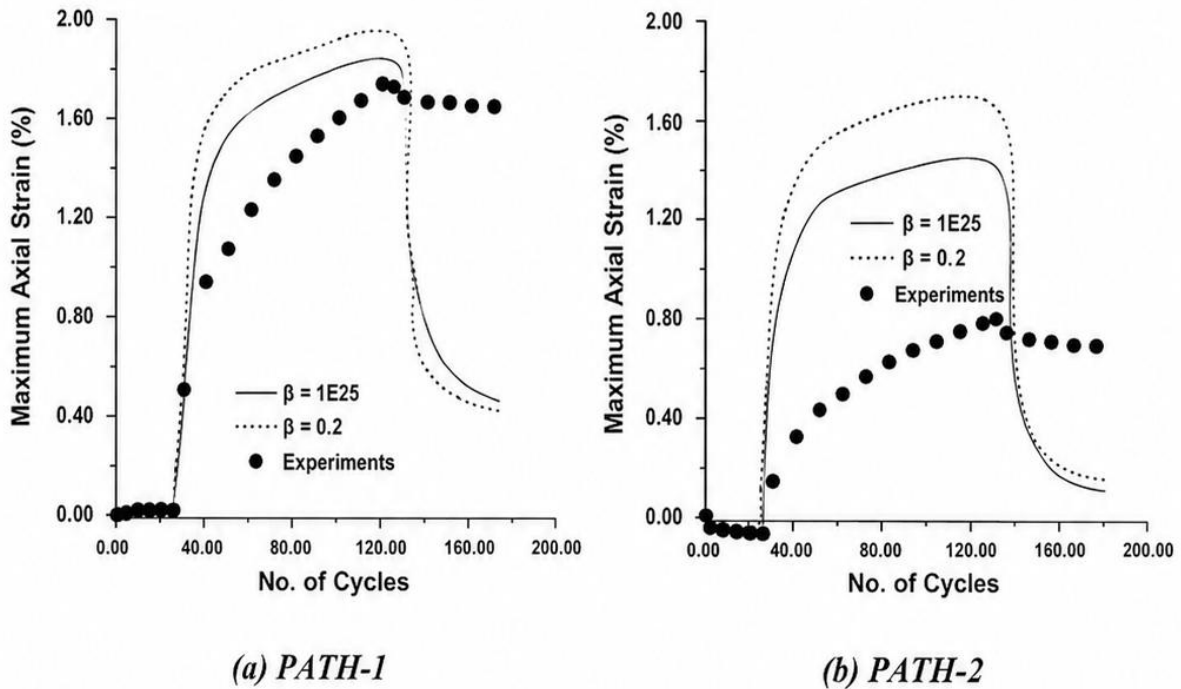


Fig. 10. Comparison between experiments and predictions of second loading category from the JS model ($m_f=15$): (a) PATH-1, (b) PATH-2.

تأثر تشوه سطح حد المرونة على التكهّن بمسارات التحميل ثنائي الاتجاه

أوصت العديد من الدراسات النظرية والمعملية المقترحة بضرورة إدماج التشوه الذي قد يحدث في سطح حد المرونة الى المعادلات التكوينية للحصول على نمذجة افضل المسارات التحميل ثنائي الاتجاه، ومع ذلك فأن تأثير مثل هذا التشوه على التكهّن بالنتائج المعملية لم تتم دراسته حتى الآن، لذلك يهدف هذا البحث الى دراسة تأثير مثل هذا التشوه ومدى فعاليته وتأثيره على التكهّن بالتصرف غير المرن الناتج عن بعض من مسارات التحميل ثنائي الاتجاه الحرجة والمعقدة حيث ظهر ان التشوه في سطح حد المرونة له تأثير فعال على التكهّن ببعض مسارات التحميل ثنائي الاتجاه ويخص بالذكر التكهّن بمعدل الانفعال اللدن المتراكم والناتج عن خليط من حمل ابتدائي ثابت وحمل ثانوي متغير ، كما أوضح البحث ان التشوه في سطح حد المرونة له تأثير شبه مهمل في بعض من مسارات التحميل التي يتغير فيها لحظيا كل من الحمل الابتدائي والحمل الثانوي.