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Generalized Modal Analysis for Closed Loop Piezoelectric Devices

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Abstract. Stress in piezoelectric material can be controlled by imposing the electrical field. Thanks to a feedback, this electrical field can be a function of some strain related measurement so as to confer to the piezoelectric device a closed loop macroscopic behaviour. In this paper we address the modelling of such system by extending the modal decomposition methods to account for the closed loop. To do so the boundary conditions are modified to include the electrical feedback circuit, hence allowing a closed-loop modal analysis. A case study is used to illustrate the theory and to validate it. The main advantage of the method is that design issue such as coupling factor of the device and closed loop stability are simultaneously captured.

1. Introduction

Piezoelectric devices are intrinsically suited for closed loop structure due to their reversibility: they can be actuators and sensors. It is therefore sensible to modify the dynamic of such a device by feeding back the strain state using the direct piezoelectric effect. Then, applying a voltage depending on this measured strain state, it is therefore possible for instance to confer a modified compliance to the system or increase dissipation in an assigned frequency band. This is the basic idea to many applications where frequency control or damping is required especially in the case of collocated structure. In (Moheimani, Fleming and Behrens 2003) or (Fairbairn and Moheimani 2013) for instance, a feedback model is used to modify the damping of structures with different strategies. As a prerequisite to apply the methods though, the frequency response of the mechanical structure must be identified. In (Preumont, de Marneffe, Deraemaeker and Bossens 2008), Preumont et al. address the problem of controlling the vibrations of large trusses. A quasi-static model of the actuator was considered, because in this case the dynamic of the piezoelectric actuator was much faster than the one of the controlled structure. This assumption cannot hold in some cases e.g MEMS or energy harvesters. Indeed, in this later cases, the overall dynamic must be accounted for since the resonance is the key issue (Dutoit, Wardle and Kim 2005, Lavrik, Sepaniak and Datskos 2004). Moreover, it is also a well known fact that the coupling factor, is inherent to the overall feedback structure and the design of the actuator (Preumont 2005). Therefore, the designer should consider a comprehensive approach in order to take into account the dynamic of the piezoelectric actuator in the case of the closed loop structure. Of course, the model is crucial in this context.

Literature on the subject of piezoelectric actuators modelling is abundant, concentrating on fine quasistatic modeling (Smits and Choi 1991, Goldfarb and Celanovic 1997) or dynamic operation near a specific resonance (Mason 1935, Ballas 2007). When addressing broadband operations, one has to consider several modes (Meirovitch 2003). Therefore, modal decomposition is a natural and widely used tool (Erturk and Inman 2009, Ducarne, Thomas and Deü 2012). The approach consists in finding the modal shapes for a given electrical condition (open circuit or short circuit, the later being the most used) where the electric field or charge can be eliminated. These modal

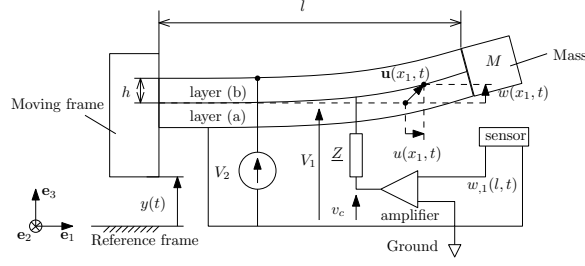


Figure 1. Schematic of the system

shapes are used to form an orthonormal basis on which any response can be decomposed (Meirovitch 2003, Geradin and Rixen 2014). Moreover, in some cases, the modal shapes have analytical expressions that are helpful to deduce design rules (Ducarne et al. 2012). Even for more complex geometry, modal shapes of known simpler structures can be used to model the dynamic (Erturk 2012). However, since the initial analysis is performed in open loop, many modes may be necessary to model the closed loop response since the resonant frequencies may be modified. Thus, the insight can be lost, and in practice the accuracy of the design is dependent on the number of modes that are considered.

In this paper, we show that the modal analysis method can be generalized to take into account the closed loop. The remainder of the paper continues as follows. Section 2 recalls the modeling of a clamped-free beam equipped with a sensor to provide feedback possibilities. Section 3 introduces the extension of the modal analysis in closed loop. We show that the boundary conditions can be modified to incorporate the effect of the feedback as moments dependent on the measurement. By properly dividing the contribution of the feedback induced moment into dissipative or generative (active power) and non-dissipative (reactive power) contributions, we prove that modal projection on a closed loop modal basis is possible. Section 4 presents experimental results to illustrate the approach and validate the prediction of a model based on the theory developed.

2. Modal analysis of a bender

2.1. Assumptions and notations

The schematic of Fig 1 depicts a simplified model of a slender beam used in this paper. The bender is mechanically excited at one end, and a feedback is used to modify its dynamic behaviour. In the following, the overall feedback circuit will be modeled as an amplifier and a series impedance so as to take into account simple filtering circuits. Regarding the mechanical modelling, the problem is supposed to be two dimensional in the sense that the different fields are not dependent on x_2 . The bender is constituted by two piezoelectric layers polarized to operate on a 31 coupling. The end at $x_1 = 0$ is clamped to the moving frame while a proof mass M is clamped at the free end at $x_1 = l$. The moving frame is translated along the direction \mathbf{e}_3 , its position relative to

the reference frame is $y(t)$. The bender is supposed to be mechanically unloaded, apart from the effect of inertia.

The electrodes are connected as shown. In practice, as will be explained later, voltage V_2 is fixed while the voltage V_1 is varied. We consider small displacements and since the electrodes impose equipotentials, it will also be assumed that the electrical field is mainly along the \mathbf{e}_3 direction. In the following, the voltage $V_1(t)$ is generated from the measure of the free tip of the bender $w_{,1}(L, t)$.

Van-Voigt notation for tensors will be used in this paper. \mathbf{x}^\top is the transpose of vector \mathbf{x} . Matrices will be indicated by brackets e.g $[c^E]$ is the matrix of compliances at fixed electrical field. $f_{,ij}$ denotes the partial derivative with respect to space variables x_i, x_j , a repeated index denotes repeated partial derivative with respect to one of the space variable. Finally \dot{f} denote time derivative of the function f .

2.2. Model

2.2.1. Piezoelectric law for the following discussion, it is convenient to consider displacement and voltage to formulate the problem thus the relevant piezoelectric equations used throughout this paper are (Ikeda 1996) :

$$\begin{cases} \mathbf{T} = [c^E] \mathbf{S} - [e]^t \mathbf{E} \\ \mathbf{D} = [e] \mathbf{S} + [\epsilon^S] \mathbf{E} \end{cases} \quad (1)$$

with $[e]$ the piezoelectric coefficient matrix and $[\epsilon^S]$ the permittivity at fixed strain matrix. \mathbf{T} , \mathbf{S} are the stress and strain tensor, \mathbf{E} , \mathbf{D} are the electric and electrical displacement fields. The corresponding thermodynamic potential then writes (Tiersten 1969) :

$$dG_2(\mathbf{S}, \mathbf{E}) = \mathbf{T}d\mathbf{S} - \mathbf{D}d\mathbf{E} \quad (2)$$

2.2.2. Kinematic of the model The bonding of the layers is supposed to be perfect, thus a continuous displacement field is considered. In this study shear strains are not considered the bender being thin and the frequencies being low. Thus a Euler-Bernouilli displacement field is used, given by (W. Weaver, Timoshenko and Young 1990, Geradin and Rixen 2014) :

$$\mathbf{u}(x_1, x_3, t) = \begin{cases} u_1(x_1, x_3, t) = u(x_1, t) - x_3 w_{,1}(x_1, t) \\ u_2(x_1, x_3, t) = 0 \\ u_3(x_1, x_3, t) = w(x_1, t) + y(t) \end{cases} \quad (3)$$

Which results in the strain field :

$$\mathbf{S} = \{u_{,1}(x_1, t) - x_3 w_{,1}(x_1, t), 0, 0, 0, 0, 0\}^\top \quad (4)$$

2.2.3. Electrostatic solution Considering the electrical displacement \mathbf{D} , the condition $\text{div}\mathbf{D} = 0$ must hold. It is assumed that the layers are thin along \mathbf{e}_3 compared to the dimensions in the other directions. Moreover, the potentials are imposed on the electrodes located on the horizontal sides of the bender, hence it is assumed that $E_3 \gg E_1$ and $E_3 \gg E_2$. Let $d \in \{a, b\}$ denotes the considered sub domain relative to the layers, then for each layer the electric field is given by $\mathbf{E} = -\text{grad}(\phi_d)$. Using the (4) and (1), and neglecting the influence of the electric field along \mathbf{e}_1 and \mathbf{e}_2 , this condition reduces to :

$$D_{d3,3} = 0 \Leftrightarrow -e_{31}w_{,1}(x_1, t) - \varepsilon_{33}\phi_{d,33}(x_1, x_3, t) = 0 \quad (5)$$

Integrating two times with respect to x_3 gives the expression of the electrical potential ϕ_d in one layer of the bender which should write (Nadal, Giraud-Audine, Giraud, Amberg and Lemaire-Semail 2014) :

$$\phi_d(x_1, x_3, t) = -\frac{e_{31}}{\varepsilon_{33}}w_{,11}(x_1, t)\frac{x_3^2}{2} + A_d(x_1, t)x_3 + B_d(x_1, t) \quad (6)$$

The functions $A_d(x_1, t), B_d(x_1, t)$, are functions to be determined from the equipotential conditions at the electrodes :

$$\begin{array}{l} \text{layer(a)} \quad \phi_a(-h) = 0 \\ \quad \quad \quad \phi_a(0) = V_1 \end{array} \quad \begin{array}{l} \text{layer(b)} \quad \phi_b(0) = V_1 \\ \quad \quad \quad \phi_b(h) = V_2 \end{array} \quad (7)$$

2.2.4. Dynamic equations The augmented Lagrangian of the piezoelectric energy harvester is given by :

$$\begin{aligned} \mathcal{L} = \frac{1}{2} \iiint \rho \dot{\mathbf{u}}^t(x_1, x_3, t) \dot{\mathbf{u}}(x_1, x_3, t) - G_2(x_1, x_3, t) d\Omega + \\ \frac{1}{2} M \dot{\mathbf{u}}(l, 0, t)^t \dot{\mathbf{u}}(l, 0, t) + V_1 Q_1 + V_2 Q_2 + Fy \end{aligned} \quad (8)$$

where the four last contributions correspond respectively to the kinetic energy of the proof mass M , the energy supplied by the two generators, and the mechanical energy due to the external force applied for the mechanical excitation.

Applying variational calculus to the Lagrangian (8) leads to the equations of the problem (Hammond 1981). The internal mechanical equilibrium involves the following equations for the extension and the flexion displacement field respectively :

$$-\mathcal{M}\ddot{u}(x_1, t) + \mathcal{K}u_{,11}(x_1, t) = 0 \quad (9a)$$

$$-\mathcal{M}(\ddot{w}(x_1, t) + \ddot{y}(x_1, t)) + \mathcal{J}\ddot{w}_{,11}(x_1, t) - \mathcal{D}w_{,1111}(x_1, t) = 0 \quad (9b)$$

where :

- \mathcal{M} is the mass per unit length

- \mathcal{J} is the rotational inertia per unit length
- \mathcal{K} is the compressive rigidity
- \mathcal{D} is equivalent the flexion rigidity

The mechanical boundary conditions at the free end are :

$$\mathcal{K}u_{,1}(l, t) = -\mathcal{N}_{ex}V_2(t) \quad (10a)$$

$$\mathcal{D}w_{,11}(l, t) = \mathcal{N}_{fl} \left(V_1 - \frac{V_2}{2} \right) \quad (10b)$$

$$\mathcal{D}w_{,111}(l, t) - \mathcal{J}\ddot{w}_{,1}(l, t) = M\ddot{w}(l, t) + M\ddot{y}(t) \quad (10c)$$

where the electromechanical conversion coefficient for extension and bending are respectively :

$$\mathcal{N}_{ex} = e_{31}a \quad (11a)$$

$$\mathcal{N}_{fl} = e_{31}ah \quad (11b)$$

At the other end of the bender, the displacement are supposed to be perfectly imposed, hence :

$$u(0, t) = 0 \quad (12a)$$

$$w(0, t) = 0 \quad (12b)$$

$$w_{,1}(0, t) = 0 \quad (12c)$$

The electrical charges can be deduced from :

$$Q_1(t) = \mathcal{C}^S(2V_1 - V_2) - \mathcal{N}_{fl} [w_{,1}(x, t)]_0^l \quad (13a)$$

$$2Q_2(t) + Q_1(t) = \mathcal{C}^S V_2 - \mathcal{N}_{ex} [u(x, t)]_0^l \quad (13b)$$

$$\text{with } \mathcal{C}^S = \frac{\epsilon_{33}^S al}{h} \quad (13c)$$

\mathcal{C}^S is the clamped capacitance.

3. Theoretical study

The study will consider the case when V_2 is constant, which is necessary in practice to apply a compressive prestress in order to avoid excessive tensile stress during bending. Therefore, the displacement field $u(x, t)$ is static since the extension and flexion fields are decoupled as can be deduced from the previous equations. Besides, without loss of generality, the frequency range considered is supposed to be sufficiently low to neglect the rotational inertia effects in (9b) and thus they will not be considered.

The equilibrium (9b) with the boundary conditions (10b), (10c), (12b) and (12c) are used for the modal analysis of the bender. To do so, the system is supposed to be free, hence every independent sources of the system are cancelled. This implies that for the

mechanical side, $y(t)$ must be set to zero.

At a considered resonant angular frequency ω , the solution to (9b) is :

$$w(x_1, t) = \psi(x_1)\eta(t) \quad (14)$$

where $\psi(x_1)$ is the modal shape written as a linear combination of the Duncan functions (see appendix Appendix A) :

$$\psi(x_1) = \left[As_1 \left(\beta \frac{x_1}{l} \right) + Bc_1 \left(\beta \frac{x_1}{l} \right) + Cs_2 \left(\beta \frac{x_1}{l} \right) + Dc_2 \left(\beta \frac{x_1}{l} \right) \right] \quad (15)$$

and $\eta(t)$ is the vibration :

$$\eta(t) = \eta \sin(\omega t + \alpha) \quad (16)$$

Moreover the dispersion condition is :

$$\mathcal{D}\beta^4 - \mathcal{M}\omega^2 l^4 = 0 \quad (17)$$

The problem can be simplified further thanks to (12b) and (12c) :

$$A = B = 0$$

We proceed in the following by modifying the boundary condition (10b) to include the feedback effect.

3.1. Electrical equations

The electrical circuit that filters and amplifies the measurement signal is now introduced. Assuming a linear circuit, a Thévenin equivalent circuit is considered. We restrict the study to case when the voltage source is proportional to the measured signal‡ :

$$v_c(t) = G_c w(l, t) \quad (18)$$

where G_c is the sensor gain possibly followed by an amplification. For the purpose of the study, we substitute $V_1 = \frac{V_2}{2} + v$ in (10b) and (13a). It reflects the fact that the bending moment is actually controlled by disturbing the middle plane voltage from the value it would normally have in a pure extension case. Since in the following harmonic oscillation will be studied, the various variables are expressed using complex vectors. Then, the displacement $w(x_1, t)$ writes § :

$$w(x_1, t) \rightarrow \underline{w}(x_1, t) = \psi(x_1)\underline{\eta}(t) = \psi(x_1)\underline{\eta}e^{j\omega t}$$

with $\underline{\eta} = \eta e^{j\alpha}$. Furthermore, equations will be written in a rotating frame so that the $e^{j\omega t}$ can be dropped, and for shorthand : $\underline{W}_{1\dots} = \psi_{1\dots}(L)\underline{\eta}$.

‡ A more general case involves a frequency dependent measure voltage. However, since there are no fundamental differences in the outline of the demonstration, this simpler case is considered.

§ \underline{x} denote a complex number, and $j = \sqrt{-1}$.

Let $\underline{Z} = \frac{N(j\omega)}{P(j\omega)}$ be the equivalent series impedance of the Thévenin equivalent generator, the equations of the circuit are :

$$\underline{i} = \frac{\underline{v}_c - \underline{v}}{\underline{Z}} \quad (19a)$$

$$j\mathcal{C}^S \omega \underline{v} = \underline{i} + j\omega \mathcal{N}_{fl} \underline{W}_{,1} \quad (19b)$$

The second equation is deduced by time derivation of (13a). According to (13a) and (10b), \underline{v} and \underline{v}_c can be expressed in terms of displacement or derivatives of displacement. Writing that $P(j\omega) = \sum p_k(j\omega)^k$ and $N(j\omega) = \sum n_k(j\omega)^k$ $k \in \mathbb{N}^+$, equations (19a), (19b) and (10b) can be combined, resulting in the following equation :

$$\left[\sum_{k=0}^{deg(P)} \mathcal{C}^S p_k(j\omega)^{k+1} + \sum_{k=0}^{deg(N)} n_k(j\omega)^k \right] \mathcal{D} \underline{W}_{,11} + \kappa_{fl}^2 \sum_{k=0}^{deg(P)} p_k(j\omega)^{k+1} \underline{W}_{,1} - \kappa_{fl} G_c \sum_{k=0}^{deg(N)} n_k(j\omega)^k \underline{W} = 0 \quad (20)$$

For the sake of clarity, the condition $n_0 \neq 0$ is assumed. In this case, the previous result can be reformulated as :

$$\mathcal{D} \underline{W}_{,11} = R(j\omega) \underline{W}_{,11} + S(j\omega) \underline{W}_{,1} + T(j\omega) \underline{W} \quad (21)$$

where $R(j\omega) = -\sum_{k=0}^{deg(P)} \frac{\mathcal{C}^S p_k}{n_0} (j\omega)^{k+1} - \sum_{k=1}^{deg(N)} \frac{n_k}{n_0} (j\omega)^k$, $S(j\omega) = -\frac{\kappa_{fl}^2}{n_0} P(j\omega)$ and $T(j\omega) = \frac{\kappa_{fl}}{n_0} N(j\omega)$ are polynomials. This defines the new boundary condition for the bending moment including the closed loop. For the following discussion, rewrite the previous equation :

$$\mathcal{D} \underline{W}_{,11} = R_d(j\omega) \underline{W}_{,11} + S_d(j\omega) \underline{W}_{,1} + T_d(j\omega) \underline{W} + j\omega (R_q(j\omega) \underline{W}_{,11} + S_q(j\omega) \underline{W}_{,1} + T_q(j\omega) \underline{W}) \quad (22)$$

where $R_d(j\omega)$, $R_q(j\omega)$, $S_d(j\omega)$, $S_q(j\omega)$, $T_d(j\omega)$ and $T_q(j\omega)$ are real even polynomials of ω .

Only the non-dissipative contribution of this condition must be considered for modal analysis. Multiplying both sides of (20) by the the conjugate of the rotation speed at the end of the tip $(j\omega \underline{W}_{,1})^*$, gives the complex power transmitted to the bender by the feedback :

$$-j\omega \mathcal{D} \underline{W}_{,11} \underline{W}_{,1}^* = -j\omega [R(j\omega) \underline{W}_{,11} + S(j\omega) \underline{W}_{,1} + T(j\omega) \underline{W}] \underline{W}_{,1}^*$$

Two different contributions should be distinguished :

- imaginary terms, corresponding to real projection on the real axis of the closed loop bending moment complex vector (therefore leading or lagging in quadrature with the rotation speed), which are conservative ;

- real terms, corresponding to imaginary part of the closed loop bending moment complex vector, which are either dissipative or supplying power.

Since \underline{W} , $\underline{W}_{,1}$ and $\underline{W}_{,11}$ are in phase, it can be concluded that real parts of R , S and T (denoted R_d , S_d and T_d in the sequel) will contribute to the reactive power, while imaginary parts (denoted R_q , S_q , T_q) will be responsible for active power .

3.2. Secular equation

For the modal analysis, only the conservative contribution of the system must be considered. Thus, according to the previous discussion, only the R_d , S_d and T_d are relevant, and thus, after cancelling the $\underline{\eta}$ that appear on both sides of the equality sign, the boundary conditions (10b) and (10c) are rewritten as :

$$\mathcal{D}\psi_{,11}(l) = R_d(\omega)\psi_{,11}(l) + S_d(\omega)\psi_{,1}(l) + T_d(\omega)\psi(l) \quad (23a)$$

$$\mathcal{D}\psi_{,111}(l) = -M\omega^2\psi(l) \quad (23b)$$

Using (17) to rewrite these conditions in β , a general secular equation can be obtained (see Appendix B).

Solving this equation for $\beta = \beta_k$ gives the resonant frequencies ω_k using (17), and once replaced in (14), the mode shape ψ_k can be obtained up to a multiplicative constant . In the usual modal analysis method, the modes are used to decompose the solution to a given excitation. These mode shapes which already partly include the electrical feedback are now compared to the classical mode shapes that would arise from considering short circuit conditions.

3.3. Properties of the closed-loop modal shapes

The closed loop (C.L.) modal shapes can be used to describe the solution to the forced vibrations problem. Indeed, let the $\theta_j(x_1)$ be the normalized open loop (O.L.) modal shapes of the bender, obtained by replacing the feedback circuit by a short circuit which verify :

$$-M\nu_j^2\theta_j^2 + \mathcal{D}\theta_{j,1111} = 0 \quad (24)$$

where ν_k are the corresponding modal angular frequencies, for the boundary conditions :

$$\theta_j(0) = 0 \quad (25)$$

$$\theta_{j,1}(0) = 0 \quad (26)$$

$$\mathcal{D}\theta_{j,11}(l) = 0 \quad (27)$$

$$\mathcal{D}\theta_{j,111}(l) = -M\nu_j^2\theta(l) \quad (28)$$

$$(29)$$

The normalization considered is defined by (Erturk and Inman 2009) :

$$\int_0^l \mathcal{M}\theta_j^2(x_1)dx_1 + M\theta_j^2(l) = 1 \quad (30)$$

Note that the same normalization will be applied to the ψ_k . The O.L. modal shapes verify :

$$\int_0^l \mathcal{M}\theta_j(x_1)\theta_k(x_1)dx_1 + M\theta_k\theta_j = \delta_{jk} \quad (31)$$

where $\delta_{jk} = 1$ if $j = k$ and $\delta_{jk} = 0$ if $j \neq k$. Therefore, the C.L. modal shapes $\psi_k(x_1)$ can be expressed as :

$$\psi_k(x_1) = \sum_{j=1}^{\infty} \alpha_{kj}\theta_j(x_1) \quad \alpha_{kj} \in \mathbb{R} \quad (32)$$

Since the $\psi_k(x_1)$ depend on the wave numbers $\{\beta_k\}$ which form an increasing sequence, it is clear from the expression (15) that :

$$\psi_k(x_1) \neq \psi_j(x_1) \quad \text{if } k \neq j \quad (33)$$

It follows that the ψ_k are independent vectors that can be related to a free basis, and thus can be used also as a basis, that is any response of the bender can be written :

$$w(x_1, t) = \sum_{k=1}^{\infty} \psi_k(x_1)\eta_k(t) \quad (34)$$

The standard procedure to solve the vibration problem involves the projection of the dynamic equilibrium verified by a modal shape ψ_k on a modal shape ψ_j is :

$$\int_0^l [\mathcal{M}\psi_k(x_1)\ddot{\eta}_k(t) + \mathcal{D}\psi_{k,1111}(x_1)\eta_k(t)] \psi_j(t)dx_1 = 0 \quad (35)$$

Using part integration, and the boundary conditions (23a) and (23b), the previous equation for harmonic vibrations (i.e $\ddot{\eta}_k(t) = -\omega_k^2\eta_k(t)$) is rewritten as follows :

$$\begin{aligned} -\omega_k^2 \left(\int_0^l \mathcal{M}\psi_k(x_1)\psi_j(x_1)dx_1 + M\psi_k(l)\psi_j(l) \right) \\ - [R_d(\omega_k)\psi_{k,11}(l) + S_d(\omega_k)\psi_{k,1}(l) + T_d(\omega_k)\psi_k(l)] \psi_{j,1}(l) \\ + \mathcal{D} \int_0^l \psi_{k,11}(x_1)\psi_{j,11}(x_1)dx_1 = 0 \quad (36) \end{aligned}$$

Considering the special case where $j = k$, and using the proposed normalization given by (30), the potential energy for the modal shape, including the feedback, verifies :

$$\begin{aligned} - [R_d(\omega_j)\psi_{j,11}(l) + S_d(\omega_j)\psi_{j,1}(l) + T_d(\omega_j)\psi_j(l)] \psi_{j,1}(l) \\ + \mathcal{D} \int_0^l \psi_{j,11}(x_1)^2 dx_1 = \omega_j^2 \quad (37) \end{aligned}$$

The projection of the dynamic equilibrium of mode ψ_j on mode ψ_k leads to an equation similar to (36) where the k and j indexes are swapped. The difference of these equations would give :

$$\begin{aligned}
& (\omega_j^2 - \omega_k^2) \left(\int_0^l \mathcal{M}\psi_k(x_1)\psi_j(x_1)dx_1 + M\psi_k(l)\psi_j(l) \right) \\
& + [R_d(\omega_j)\psi_{j,11}(l) + S_d(\omega_j)\psi_{j,1}(l) + T_d(\omega_j)\psi_j(l)] \psi_{k,1}(l) \\
& - [R_d(\omega_k)\psi_{k,11}(l) + S_d(\omega_k)\psi_{k,1}(l) + T_d(\omega_k)\psi_k(l)] \psi_{j,1}(l) = 0 \quad (38)
\end{aligned}$$

Therefore, the C.L. modal shapes are generally not orthogonal in closed-loop for the functional (35) ||. Actually, in C.L. the functional should be modified to account for the electrical equilibrium of the bender and the circuit. Fortunately, this tedious task can be avoided to solve the forced response due to the feature discussed below.

3.3.1. Response to the forced vibration Indeed, the problem can still be decomposed. Assuming that the solution can be written as in (34) the projection of the solution on mode ψ_j writes :

$$\int_0^l \left[\sum_{k=1}^{\infty} \mathcal{M}\psi_k(x_1)\ddot{\eta}_k(t) + \mathcal{D}\psi_{k,1111}(x_1)\eta_k(t) + m\ddot{y} \right] \psi_j(x_1)dx_1 = 0 \quad (39)$$

Isolating the contribution of the j^{th} mode and performing part integration gives :

$$\begin{aligned}
& \int_0^l \left[\sum_{k \neq j}^{\infty} \mathcal{M}\psi_k(x_1)\ddot{\eta}_k(t) + \mathcal{D}\psi_{k,1111}(x_1)\eta_k(t) \right] \psi_j(x_1)dx_1 \\
& + \mathcal{M} \int_0^l \psi_j(x_1)^2 dx_1 \ddot{\eta}_j(t) + \mathcal{D} \int_0^l \psi_{j,11}(x_1)^2 dx_1 \eta_j(t) \\
& - [R_d(\omega)\psi_{j,11}(l) + S_d(\omega)\psi_{j,1}(l) + T_d(\omega)\psi_j(l)] \psi_{j,1}(l)\eta_j(t) \\
& + \ddot{y}(t) \int_0^l \mathcal{M}\psi_j(x_1)dx_1 + M\psi_j(l)^2 \ddot{\eta}_j(t) + M\ddot{y}(t)\psi_j(l) = 0 \quad (40)
\end{aligned}$$

First integral in this expression is nil because modal shape verify (35), thus it can be deduced using (30) and (37) that the equation simplifies to :

$$\ddot{\eta}_j(t) + \omega_j^2 \eta_j(t) = -\phi_j(t) \quad (41)$$

with the modal inertia forces :

$$\phi_j(t) = \left(\int_0^l \mathcal{M}\psi_j(x_1)dx_1 + M\psi_j(l) \right) \ddot{y}(t) \quad (42)$$

These equations allow to calculate the modal shapes, the resonant frequencies as well as the coupling factors. However, to be able to predict the vibration amplitude, it is necessary to include the active effects of the electrical circuit.

|| This depends on the feedback law : for instance, it can be easily deduced from (38) if the feedback is proportional to the derivative of the displacement $w_{,1}(l, t)$, orthogonality property is retrieved for $\mathcal{Z} = 1$

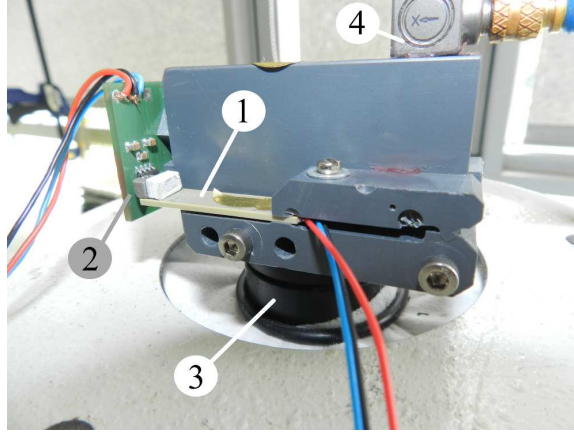


Figure 2. Experimental setup : (1) bender with a magnet and proof mass fixed at the end; (2) Hall sensor; (3) shaker; (4) accelerometer

3.3.2. Active effects of the electrical circuit It remains to take into account the effect of the sources introduced by the feedback. Equations (21) and (22) show that the odd powers of the polynomials $R(j\omega)$, $S(j\omega)$ and $T(j\omega)$ (that is R_q , S_q and T_q) can either dissipate or provide power. The procedure is the same, that is assuming the solution (34), the dynamic equation is projected onto a mode ψ_j . However, the general boundary condition for moment is now considered :

$$\mathcal{D}w_{,11}(l, t) = \mathfrak{R}_\delta w_{,11}(l, t) + \mathfrak{S}_\delta w_{,1}(l, t) + \mathfrak{T}_\delta w(l, t) + \mathfrak{R}_q \dot{w}_{,11}(l, t) + \mathfrak{S}_q \dot{w}_{,1}(l, t) + \mathfrak{T}_q \dot{w}(l, t) \quad (43)$$

where \mathfrak{R}_δ , \mathfrak{S}_δ , \mathfrak{T}_δ , \mathfrak{R}_q , \mathfrak{S}_q , and \mathfrak{T}_q are the linear differential operators with respect to time corresponding to the ω polynomials defined in (22).

The modal equations are then :

$$\ddot{\eta}_j(t) + 2\xi\omega_j\dot{\eta}_j(t) + \omega_j^2\eta_j(t) = -\phi_j(t) - \sum_{k=1}^{\infty} \phi_{jk}(t) \quad (44)$$

In the previous equation the modal damping ξ has been introduced (Geradin and Rixen 2014). A cross coupling between modes appears with the contributions $\phi_{jk}(t)$ given by :

$$\phi_{jk}(t) = [\mathfrak{R}_q \dot{\eta}_k(t)\psi_{k,11}(l) + \mathfrak{S}_q \dot{\eta}_k(t)\psi_{k,1}(l) + \mathfrak{T}_q \dot{\eta}_k(t)\psi_k(l)] \psi_j(l) \quad (45)$$

4. Experimental validation

4.1. Experimental setup

A picture of the experimental set up is reproduced in Fig. 2. The co-fired multilayer bender is a Noliac CMBP05 (NOLIAC 2011). The material is NCE57. The main specifications are summed up in tab. 1, and were used for the model that is discussed

$\frac{\epsilon_{33}^T}{\epsilon_0}$	1800	-	Relative dielectric constant
$\tan\delta$	170.10^{-4}	-	Dielectric loss factor
$-d_{31}$	170.10^{-12}	C/N	Piezoelectric charge constant
k_{31}	0.33	-	Electromechanical coupling factor
ρ	$7.7.10^3$	kg.m ³	density
s_{11}^E	17.10^{-12}	m ² /N	elastic compliances
Q	70	-	Mechanical quality factor

Table 1. Characteristics of NCE 57 piezoelectric material

hereafter. The bender is clamped on one side fixed on a vibrating pot. On the other side, a magnet is fixed (weight 0.8 g) and for some test a tungsten mass can be fixed. The magnet is used to measure the end tip displacement thanks to a Hall sensor. This sensor was calibrated using a laser sensor (Polytec OFV 505), the sensitivity was estimated to 8.3 mV/ μ m. The feedback is realized by amplifying the displacement measure using operational amplifier to select the gain by combining resistors then a power amplifier (NF HSA 4051) is used to post amplify with gains of 20 and 40, resulting in supply voltages varying from 0 to 250 V. An accelerometer is fixed on the moving frame. In order to establish the frequency responses of displacement, a dynamic signal analyser (Stanford Research SR 785) is used. Finally, electrical measurements (voltage, current, acceleration and displacement sensors output) are realised using an oscilloscope (Tektronik TDS 3014).

4.2. Case study : effect of a position feedback and output series resistance on the closed-loop resonance

4.2.1. Model For practical reasons, a series resistance was implemented. Indeed, sensor noise are amplified in the closed loop exciting higher resonances, and can even become destabilising. To prevent this, the resistance was initially introduced to constitute in combination with the capacitance C^S low pass filter.

To address the effects of this resistance, since $\underline{Z} = R_s$, the following polynomials are considered :

$$N = R_s \quad (46a)$$

$$P = 1 \quad (46b)$$

Then the condition (43) is :

$$w_{,11}(l, t) = \frac{\mathcal{N}_{fl}}{D} G_c w(l, t) - \tau \dot{w}_{,11}(l, t) - \frac{\mathcal{N}_{fl}^2}{DR} \dot{w}_{,1}(l, t) \quad (47)$$

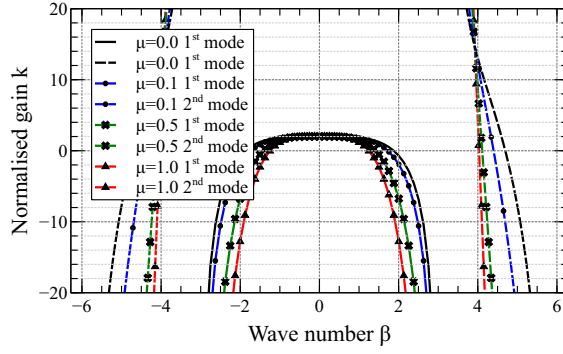


Figure 3. Theoretical effect of the normalised feedback gain k on the wave number β

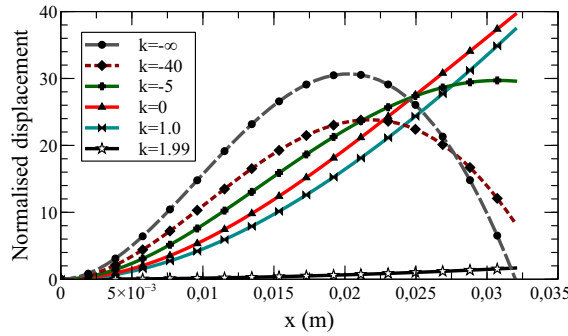


Figure 4. Theoretical effect of the normalised feedback gain k on the modal shapes

with $\tau = R_s C^S$. So, for this simple case :

$$\begin{cases} \mathfrak{R}_\delta w_{,11}(l, t) = 0 \\ \mathfrak{S}_\delta w_{,1}(l, t) = 0 \\ \mathfrak{T}_\delta w(l, t) = N_{fl} G_c w(l, t) \end{cases} \quad \text{and} \quad \begin{cases} \mathfrak{R}_q \dot{w}_{,11}(l, t) = -\tau \mathcal{D} \dot{w}_{,11}(l, t) \\ \mathfrak{S}_q \dot{w}_{,1}(l, t) = -\frac{N_{fl}^2}{R_s} \dot{w}_{,1}(l, t) \\ \mathfrak{T}_q \dot{w}(l, t) = 0 \end{cases}$$

4.2.2. Mechanical resonance As far as the effect of the feedback on resonance is concerned, only $\mathfrak{T}_\delta w(l, t)$ has to be considered. For the sake of generality, the variable change $\zeta = \frac{x}{l}$ is introduced. The plot of β against the normalized gain $k = \frac{N_{fl} G_c l^2}{\mathcal{D}}$ (fig. 3) then depicts the effect of this feedback on the modal shapes (see fig 4 for the case of the first mode), and thus on the resonance frequencies for various mass ratio $\mu = \frac{M}{M}$:

- $k > 0$ reduces β , that is it decreases resonance frequencies, thus it can be assimilated to a softening in the sense that it acts as if the rigidity of the bender was less than in open-loop. The effect is rather drastic as for $k = 1$ the first mode will become unstable due to buckling.
- On the contrary, $k < 0$ increases β , although rather progressively, so a hardening is induced by the closed loop. Asymptotic analysis shows that for the first mode $\lim_{k \rightarrow \infty} \beta = \pi$

The theoretical (solid and dashed lines) and measured (cross and dots) wave number are compared for some values of the normalized gain k on fig. 5. Theoretical

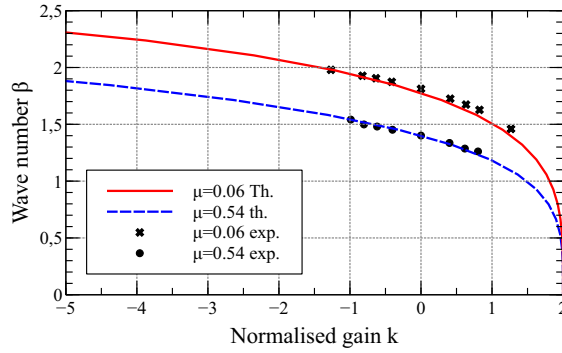


Figure 5. Theoretical (th.) and measured (exp.) wave number β vs normalised gain k

values of β are obtained using the secular equation discussed in Appendix B. The normalized gain are calculated using the theoretical expression of k , the manufacturer coefficients of tab. 1, the values of the feedback gains of the amplifier circuit (set to $G_c = \{-40, -26, -20, -13, 0, 13, 20, 26, 40\}$). Taking into account the gain of the sensor and the flexion rigidity, the reduced gain were $k = \{-1.13, -0.74, -0.57, -0.37, 0, 0.37, 0.57, 0.74, 1.13\}$. The experimental values of β are deduced from the measured resonance frequencies. Two values of the tip mass to bender mass ratio μ were considered ($\mu = \{0.06, 0.54\}$). The experimental results are in good agreement with the theoretical prediction.

The effects of the closed loop on the dissipation are now examined. Due to the variation of β with respect to k , the modal shape ψ_j are modified, and therefore so are the modal forces ϕ_j (42). This is depicted on fig. 6 (top) for the two first modes. Moreover, they affect the modal damping as can be seen in relations (44) and (45). In this case, assuming that the cross coupling of the modes is negligible near the mode resonance since the considered mode dominates, it is possible to express the closed loop damping factor as follows :

$$\xi_j' = \xi_j - \frac{1}{2} (r_q \psi_{j,1}(l) + s_q \psi_{j,1}(l)) \psi_{j,1}(l)$$

where :

$$r_q = -\frac{\tau D}{l^3} \text{ and } s_q = \frac{R \kappa_{fl}^2}{l^2}$$

The curves of the damping versus k for the case $\mu = 0.06$ are presented on fig. 6 and it can be seen that the feedback has a clear impact especially on the first mode. Note that to obtain these curves. The O.L. damping was estimated from the O.L. quality factor on the frequency response. For both modes, the damping vanishes as the gain tend to low values, which indicate a possible instability. The predicted resonance curve of the tip vibration amplitude and phase shift at imposed acceleration are depicted on fig. 7. The model prediction are in good agreement with the experiment as for the resonance amplitudes and frequencies. Still, some discrepancies can be found as the experimental resonance curves are non symmetric, especially for low values of damping. Tests showed that the resonance curves are different for increasing and decreasing frequency sweeping

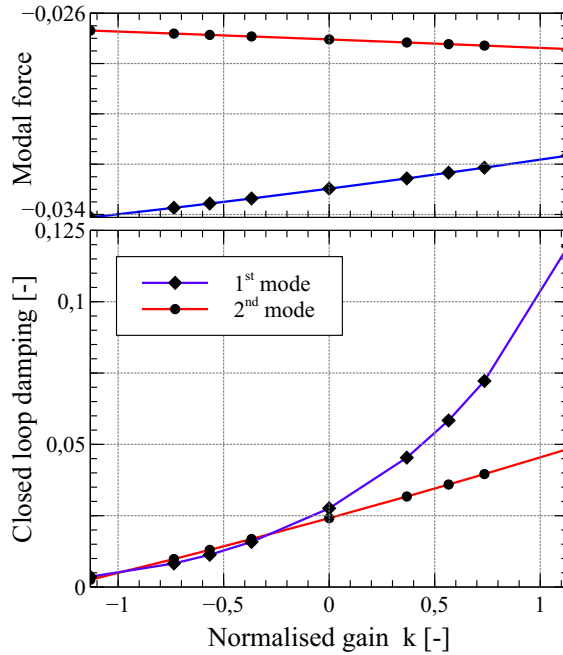


Figure 6. Theoretical dependence of the modal forces (top) and of the closed loop damping coefficients (bottom) on the normalised gain k for different values of μ

and presents amplitude jumps, which is typical of piezoelectric (soft Duffing resonance). This is out of the scope of the model, and the tests presented on fig. 7 are obtained for a decreasing frequency sweep and small acceleration amplitude (0.5 g). Damping is also correctly estimated, in the limit of the model. In fig. 8 prediction of the amplitude are compared to experience (top). Furthermore, predictions of the model taking into account the damping introduced in closed loop by the electrical circuit are compared to prediction including only the mechanical (O.L.) damping (bottom). It clearly illustrates the combination of both modal forces and damping in closed loop. It can be noted that as predicted the system is close to instability as the feedback gain approaches high positive values and the bode diagram cannot be plotted due to noisy measurements.

4.2.3. Current at resonance Finally, fig. 9 compares the measured and predicted currents delivered by the amplifier to the piezoelectric device at the mechanical resonance frequencies. The theoretical current is obtained by replacing the expression of the derivative of the displacement into (13a) and performing a time derivation :

$$i_1(t) = \dot{Q}_1(t) = 2C^S \dot{V}_1 - \underbrace{\kappa_{fl} \dot{w}_1(L, t)}_{\text{motional current}}$$

Again, the results are in good agreement. Additionally, the motional current is represented to illustrate the effect of variation of β due to k that results in an inflexion of the motional current curve when the resonant frequency increases.

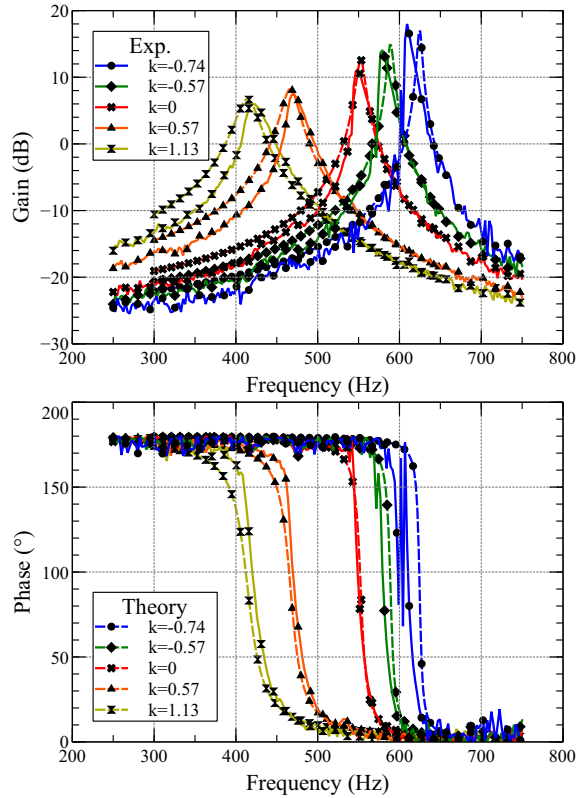


Figure 7. Comparison of the theoretical (plain lines) and measured (dotted line) of the dynamic mechanical gain (top) and phase (bottom) on the normalised gain k ($\mu = 0.06$)

5. Conclusion

This paper proposes an alternative modal decomposition so as to study the effects of feedback on the dynamic of a piezoelectric device. The method is general and consists in the following steps :

- the boundary conditions of the dynamic equation of the system are modified to include directly the feedback as written in (21). Two contributions can be distinguished : reactive (non dissipative) terms depending on even time derivatives of the displacement or spatial derivative of the displacement, active terms depending on odd time derivative of the same functions.
- a modal analysis, consisting in solving the partial derivative equations when the dissipative terms are cancelled. This results in a set of modal shapes (closed loop modal shapes) that presents some properties similar to the classical modal decomposition : the equation of motion can still be projected to form a infinite system of independent equations depending solely on time.
- finally, the dissipative contribution are included as external sources and the solution is then written as a decomposition on the closed loop modal basis.

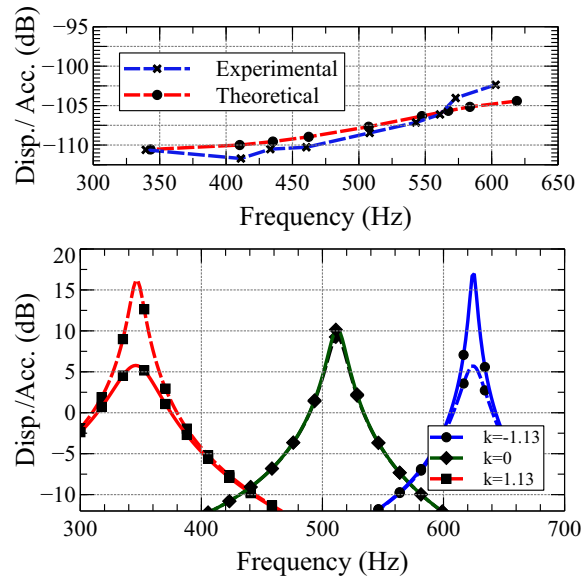


Figure 8. Measured and predicted mechanical gains at mechanical resonance frequencies (top) and comparison of the model predictions when including the closed loop damping introduced by the electrical circuit (dashed) or not (plain) (bottom graph)

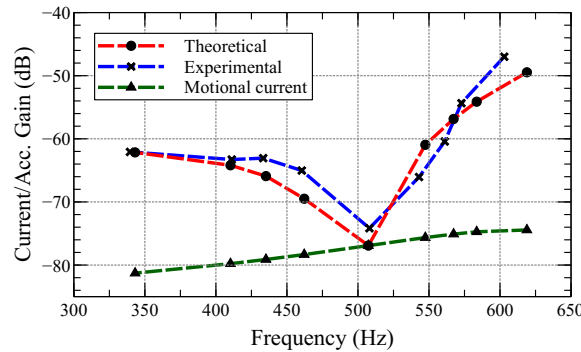


Figure 9. Measured and predicted mechanical gains electrical gains (dots and crosses) and the motional current (triangles) at mechanical resonance frequencies.

The method has been applied to a simple set up that allows to derive the solutions and has been validated against experimental results. The benefit of the approach is to consider the closed loop system as a whole system and represent the electromechanical interaction including the control. The classical modal decomposition, obtained by considering the short circuited resonance, can also be used to obtain this but the truncation of the projected solution is still the key to the accuracy of the model near a resonance of the closed loop system. In contrast, the proposed method can efficiently model the dynamic with a few modes. Moreover, some issues such as stability or energy consumption can be directly addressed. Therefore, the expected benefits of the method are in the field of design or control of devices where the decoupling of the mechanical structure dynamic and the piezoelectric device is not verified e.g energy harvesters such

as the one used here, or atomic force microscope tips.

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Appendix A. Duncan functions

Duncan functions are defined as :

$$s_i(x) = \sinh(x) + (-1)^i \sin(x) \quad (\text{A.1a})$$

$$c_i(x) = \cosh(x) + (-1)^i \cos(x) \quad (\text{A.1b})$$

with $i \in \{1, 2\}$ They verify the property :

$$s_{1,1111} = c_{1,111} = s_{2,11} = c_{2,1} = s_1$$

Appendix B. Expression of the secular equation

Defining :

$$R'_d(\beta) = R_d \left(\frac{\mathcal{D}}{\mathcal{M}} \frac{\beta^4}{l^4} \right), \quad S'_d(\beta) = S_d \left(\frac{\mathcal{D}}{\mathcal{M}} \frac{\beta^4}{l^4} \right),$$

$$T'_d(\beta) = T_d \left(\frac{\mathcal{D}}{\mathcal{M}} \frac{\beta^4}{l^4} \right)$$

the secular equation is obtained after some calculus :

$$\left[(\mathcal{D} - R'_d(\beta)) \frac{\beta^2}{l^2} s_1(\beta) - S'_d(\beta) \frac{\beta}{L} c_2(\beta) - T'_d(\beta) s_2(\beta) \right]$$

$$[s_2(\beta) + \mu\beta c_2(\beta)] - \left[(\mathcal{D} - R'_d(\beta)) \frac{\beta^2}{l^2} c_1(\beta) \right.$$

$$\left. - S'_d(\beta) \frac{\beta}{L} s_1(\beta) - T'_d(\beta) c_2(\beta) \right] [c_1(\beta) + \mu\beta s_2(\beta)] = 0 \quad (\text{B.1})$$

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