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# Introducing the Flap Mass Damper for Controlling Bridge Aeroelastic Instabilities

**K.N. Bakis[1], M. Massaro[2], M.S. Williams[3], D.J.N. Limebeer[3] and J.M.R. Graham[4]**

[1]Department of Civil and Earth Resources Engineering
Kyoto University, Yoshida Honmachi, Sakyo-ku, Kyoto 606-8501, Japan.

[2]Department of Industrial Engineering
University of Padova, Via Venezia 1, 35131 Padova, Italy.

[3]Department of Engineering Science
University of Oxford, Parks Road, Oxford OX1 3PJ, U.K.

[4]Department of Aeronautics
Imperial College London, London SW7 2AZ, U.K.

## Abstract

This study investigates the control of bridge deck flutter and torsional divergence instabilities, using a novel passive approach. The control system design study is based on a sectional flexible bridge model interacting with a constant velocity airstream. The control configuration incorporates the properties of movable flaps, seen as extensions of the bridge deck, and of a tuned mass damper (TMD). This combined mechanical system, referred to as the flap mass damper (FMD), combines favourable aerodynamic properties of the flaps with a driving force provided by the vibrating mass inside the hollow deck. A further advantage is that the deck's motion is transmitted to the flaps passively without requiring complex external linkages. Special attention is given to ensuring that the control configuration attains optimum robustness properties and thus maximizes uncertainty tolerance.

## Introduction

Past research on controlling bridge aeroelastic response has focused on both active and passive methodologies. Perhaps the most effective physical control measure is the introduction of auxiliary flaps either adjacent to the deck or at some distance above or below it. The control of bridge flutter using actively controlled flaps located beneath the deck was first proposed by Ostenfeld and Larsen [10] and further investigation was undertaken by Hansen and Thoft-Christensen [5]. More recently, Li et al. [6] based on a two dimensional active control framework, performed an active control procedure using a pair of rotatable winglets at a distance from the deck. Optimal control strategies of this sort as well as pole placement algorithms provide a computationally viable procedure for deriving stabilizing controllers but neglect robustness properties and the assessment of available stability margins with respect to plant uncertainties. Indeed, for a controllable and observable system the computation of a stabilizing controller is a straight forward design exercise but investigation by the authors has shown that it can lead to very poor robustness margins. To address this issue Bakis et al. [2] introduced a framework for investigating flap efficiency in a bridge multi-modal model using an $H_\infty$ active control strategy, which aims at quantifying structural and aerodynamic uncertainty.

Contrary to active systems, passive flap mechanisms have the advantage of dispensing the need for an external power source, but require an elaborate mechanical network for transforming deck movement into flap rotation. Ideally, a feedback connection between deck rotational movement, rotation being the dominant mode in flutter coupling, and the flap rotations should be established. The main practical implication of this endeavour arises due to lack of access to a ground reference frame. Omenzetter et al. [8] proposed a mechanical system in which the flap rotation is linked to the deck pitch motion by means of additional cables and an auxiliary transverse beam supported by the main cables. Prestressed springs are used to push the flaps since the cables only provide tensile forces. Sectional analysis of this system showed that although higher critical wind speed can be attained, large flaps are required as well as significant stiffness for the supporting beam. A subsequent finite element aeroelastic framework showed that due to kinematic coupling between the flap rotation and the sway motion of the deck and cables, the achievable improvement in the bridge's critical flutter speed is limited [9].

Wilde et al. [15] proposed an alternative mechanism in which the flaps are kinematically constrained to a pendulum inside the deck. More recently Phan and Nguyen [11] proposed a mechanism based on a cross beam, surrounding the deck girder, attached to the hanger cables with additional gears and a belt for driving flap motion. The system results in a pure gain control law. The cross beam is used as a reference for pitch rotation, but hinders the mechanism's applicability on real structures due to its construction complexity and aerodynamic modification of the deck girder. Pure gain controllers like these however forego the advantages that accrue from phase compensation, whereas fixed-phase controllers are difficult to implement. Zhao et al. [16] introduced phase compensation and used an optimization routine for determining the controller's parameters. However the proposed design was only realizable under the premise that it can be connected to an inertial reference frame. The feasibility of passive control of schemes on the erection stage of suspension bridges for flutter and buffeting has been presented recently in [1,3] whereas some efforts have been made to combine the advantages of control surfaces with a Tuned Mass Damper (TMD) [13] although a detailed analysis has not been reported yet.

This work aims to fill the research gap by proposing a passively controlled deck-flap network by means of suspended masses in the box girder. The mechanical layout avoids the use of external components, which interfere with the deck's aerodynamic characteristics, and of additional cables which complicate the system and add to the structure's dead load.

## Aeroelastic Model

The kinematic model of the deck-flap system consists of four degrees of freedom $q = \{h, a, \beta_l, \beta_t\}$, figure 1. These are the deck heave and pitch angle with respect to the elastic centre and the leading-and trailing-edge relative to the deck flap angles.

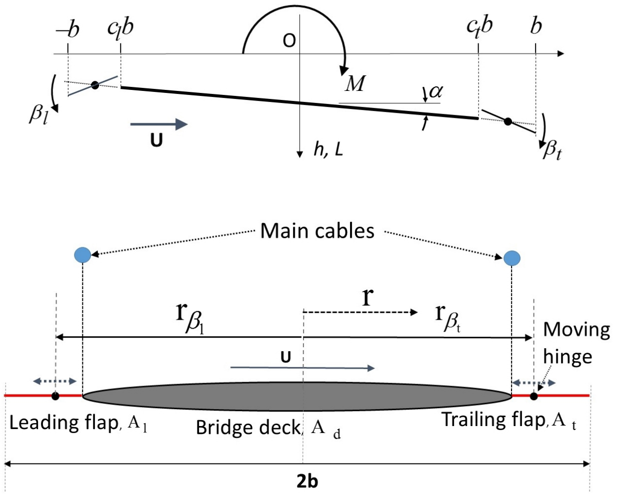


Figure 1. Kinematic model of the deck-flap system and corresponding notation.

In order to provide a driving force for controlling the flaps a combined system of symmetrically suspended masses connected to the flap controllers is proposed, referred to as the FMD, figure 2. Employing Lagrange's approach to derive the equations of motion, for a detailed derivation see [4], we obtain:

$$\begin{aligned} &m_f h + (S_\alpha - m_{dl} d_{tm} + m_{dr} d_{tm})\alpha + (S_{\beta_l} + m_{dl} r_l)\beta_l \\ &+(S_{\beta_t} - m_{dr} r_t)\beta_t + C_h h + K_h h = L, \end{aligned} \tag{1}$$

$$\begin{aligned} &(S_\alpha - m_{dl} d_{tm} + m_{dr} d_{tm})h + I_f \alpha - (I_{\beta_l} - r_{\beta_l} S_{\beta_l} + m_{dl} r_l d_{tm})\beta_l \\ &+(I_{\beta_t} + r_{\beta_t} S_{\beta_t} - m_{dr} r_t d_{tm})\beta_t + C_\alpha \alpha + K_\alpha \alpha = M, \end{aligned} \tag{2}$$

$$\begin{aligned} &(S_{\beta_l} + m_{dl} r_l)h - (I_{\beta_l} - r_{\beta_l} S_{\beta_l} + m_{dl} r_l d_{tm})\alpha + (I_{\beta_l} + m_{dl} r_l^2)\beta_l \\ &+(C_{\beta_l} + r_l^2 C_{dl})\beta_l + (K_{\beta_l} + r_l^2 K_{dl})\beta_l = M^{\beta_l} + M_c^{\beta_l}, \end{aligned} \tag{3}$$

$$\begin{aligned} &(S_{\beta_t} - m_{dr} r_t)h + (I_{\beta_t} + r_{\beta_t} S_{\beta_t} - m_{dr} r_t d_{tm})\alpha + (I_{\beta_t} a + m_{dr} r_t^2)\beta_t \\ &+(C_{\beta_t} + r_t^2 C_{dr})\beta_t + (K_{\beta_t} + r_t^2 K_{dr})\beta_t = M^{\beta_t} + M_c^{\beta_t}, \end{aligned} \tag{4}$$

Where,

$$m_f = m_d + m_{\beta_t} + m_{\beta_l} + m_{dl} + m_{dr}, \; I_f = I_\alpha + m_{dl} d_{tm}^2 + m_{dr} d_{tm}^2$$

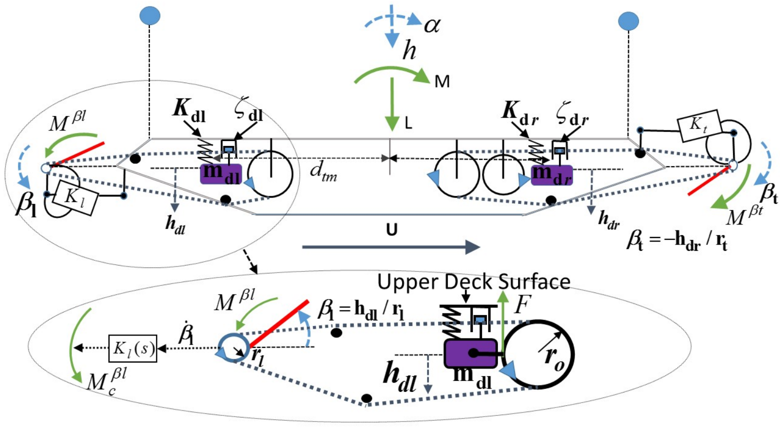


Figure 2. Conceptual mechanical configuration of the flap mass damper (FMD).

The quantities $m_d, m_{\beta_l}, m_{\beta_t}$ are the per unit length masses of the deck, leading and trailing flaps respectively.

$I_\alpha, I_{\beta_l}$ and $I_{\beta_t}$ are the per unit length mass moment of inertia of the deck with respect to the elastic centre and mass moment of inertias for the flaps with respect to the corresponding deck-flaps hinges. The expressions for these quantities as well as for the terms $S_\alpha, S_{\beta_l}$ and $S_{\beta_t}$ are given in [4]. $K_h$ and $K_\alpha$ denote the per unit length vertical and torsional stiffness of the entire assembly; $K_{\beta_l}$, $K_{\beta_t}$ are the per unit length leading-and trailing- edge torsional stiffnesses of the flaps with respect to the hinges. For a bridge with known heave and torsion resonant frequencies $\omega_h$, $\omega_\alpha$ the corresponding structural stiffnesses can be computed as $K_h = m_f \omega_h^2$ and $K_\alpha = m_f \omega_\alpha^2$. The flap frequencies are similarly computed by the expressions $K_{\beta_t} = I_{\beta_t} \omega_{\beta_t}^2$, $K_{\beta_l} = I_{\beta_l} \omega_{\beta_l}^2$. In this case the flaps are considered to be an internal part of the deck and treated as aerodynamically thin. The structural characteristics of the Humber Bridge in the U.K. have been used as an application example.

The kinematic constraint between the tuned mass and the flap angle can be realized in different ways, for example through a rack, pinion and belt mechanism or by a connection using levers and a gear box. A conceptual realization of the proposed mechanism is graphically presented in figure 2. In this example, the TMD movement is transmitted through a rack-pinion-belt linkage. For the leading-edge flap a single pinion of radius $r_o$ is used whereas for the trailing flap two pinions are needed in order to reverse the angle. The sign choice was determined based on preliminary analyses for attaining optimum performance. Assuming the pinion radii at the flap pivots to be $r_l$ and $r_t$ then the vertical mass motions is linked using expressions: $h_{dl} = \beta_l \times r_l$, $h_{dr} = -\beta_t \times r_t$.

The aerodynamic lift force $L$ and torques $M$, $M^{\beta_l}$ and $M^{\beta_t}$ in equations (1)-(4) are derived using a coordinate system transformation [4,8,16] of the Theodorsen - Garrick wing-aileron-tab model [14]. The coupled structural aerodynamic equations are then brought in a state space form as has been shown repeatedly in [1-4,16], wherein a rational function approximation for the circulatory part of the aerodynamic forces is employed. This procedure has the advantage of dispensing with the frequency dependency of the aerodynamic forcing and allows the use of convenient control oriented analytic devices such as the root-locus and Nyquist diagrams.

## Feedback System

The block diagram of the bridge control system in figure 3 demonstrates the interconnection of structural dynamics, fluid dynamics and the control system. The uncontrolled system is described by the plant $P(s)$ that contains the structural dynamics and the non-circulatory part of the fluid mechanics, while the Theodorsen function approximation $C(s)$ generates the circulatory flow. The controllers for the leading- and trailing- edge flaps are denoted by $K_l(s)$ and $K_t(s)$ respectively, their function is to generate the flap torques $M_c^{\beta_l}, M_c^{\beta_t}$ from the flap velocities $\dot{\beta}_l, \dot{\beta}_t$. This control design exercise presumes an asymmetric flap configuration as the prevailing wind direction for an individual bridge is usually known from site measurements.

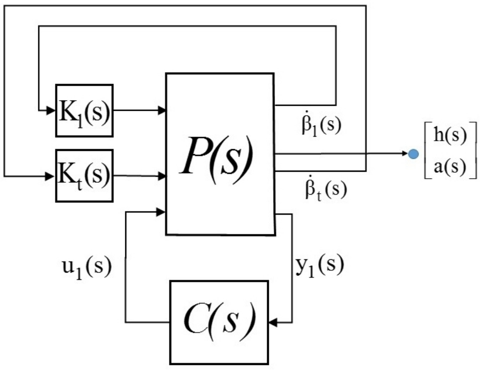


Figure 3. Block diagram of the aeroelastic control system.

The procedure for designing the mechanical system of figure 2 is based on a constraint optimization process. The optimization parameters include the mass, spring and damper elements for the 2 TMD configurations as well as the additional compensator coefficients at the flap pivot connections, $K_l(s), K_t(s)$. It was observed that using a first order compensator configuration results in a significantly simpler network with marginally worse performance when compared to higher compensators. The overall number of optimization parameters for this case is 14. The optimization objective is to maximize the robust stability margins while enforcing passivity for the mechanical networks $K_l(s), K_t(s)$. The higher the stability robustness margin, the larger the uncertainties (e.g. variation in the system parameters) under which the controlled system remains stable. An unstructured uncertainty model is used here [7] and more specifically that of additive perturbations on the normalized left coprime description of the plant. This uncertainty model has the advantage of relating to perturbations of lightly damped natural frequencies which in turn control the system's dynamic behaviour, see [4,7]. The resulting compensator network is depicted in figure 4 and consists of a three element network of 2 dampers and one inerter, [12]. The network geometry is not preselected but attained from the network synthesis process.

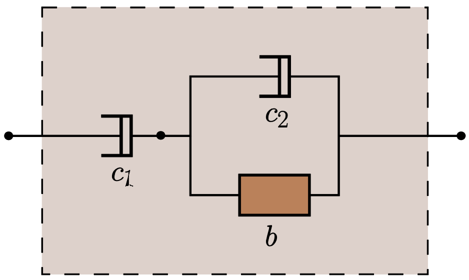


Figure 4. Realization of first order mechanical compensator.

The flap-deck pivot location is another issue of concern, figure 2. This will affect both the structural and aerodynamic quantities in equations (1)-(4) and will ultimately result to different network characteristics. As shown in [4] the best pivot location is that on the outer flap edges primarily because it can effect both the flutter and torsional divergence limits.

## Results

Following the procedure presented in Omenzetter et al. [8,9] and Bakis et al. [1,2,3,4] we assess the system's stability using root loci diagrams which effectively consist of depicting the eigenvalues of the generalized state space model, resulting from control system configuration in figure 3, at different wind speeds. For the uncontrolled systems the flaps are considered to be rigidly attached to the deck. The open loop system of the sectional model of the Humber Bridge exhibits flutter at 65 m/s and torsional divergence instability at 72 m/s. Table 1 compares these values with the cases where the system is controlled by a TMD without a kinematic constraint to the flaps and the FMD system presented here. Figure 5 compares the root-loci diagrams for the TMD and FMD systems.

| Aeroelastic instability limits | | |
|---|---|---|
| **System** | **Flutter limit** | **Divergence limit** |
| Uncontrolled | 65 m/s | 72 m/s |
| TMD | 86 m/s | 78 m/s |
| FMD | 93 m/s | 104 m/s |

Table 1. Aeroelastic stability limits for the uncontrolled and controlled systems of the Humber's sectional model.

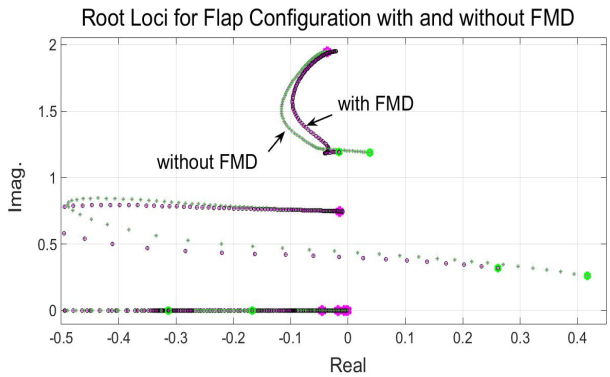


Figure 5. Root loci of the Humber Bridge section model for the TMD and FMD cases. The wind velocity is swept from 0 to 100 m/s.

An important issue relating to TMD performance for aeroelastic control is its sensitivity to tuning frequency, which poses a serious reliability issue. Figure 6 compares the critical flutter and torsional divergence wind speeds with regard to the tuning frequencies of the TMD and FMD systems. It becomes apparent that the FMD's performance is considerably less sensitive to optimal tuning of mechanical components. It also interesting to note that the implemented optimization algorithm estimated the tuning frequency of the leeward mass to be approximately $\omega_{dr} = 12.75\text{rad/s}$. Figure 6 indeed shows that this value is a trade –off between performance and robustness, because of the steep drop in flutter critical wind speed above $\omega_{dr} = 13.75\text{rad/s}$.

Furthermore, figure 7 shows that the vertical mass oscillations are much smaller for the FMD mechanism compared to the TMD system. This is to be expected since the function of the suspended masses in the FMD case is to control the flaps and change the aerodynamic loading on the deck rather than control its vibration through inertia.

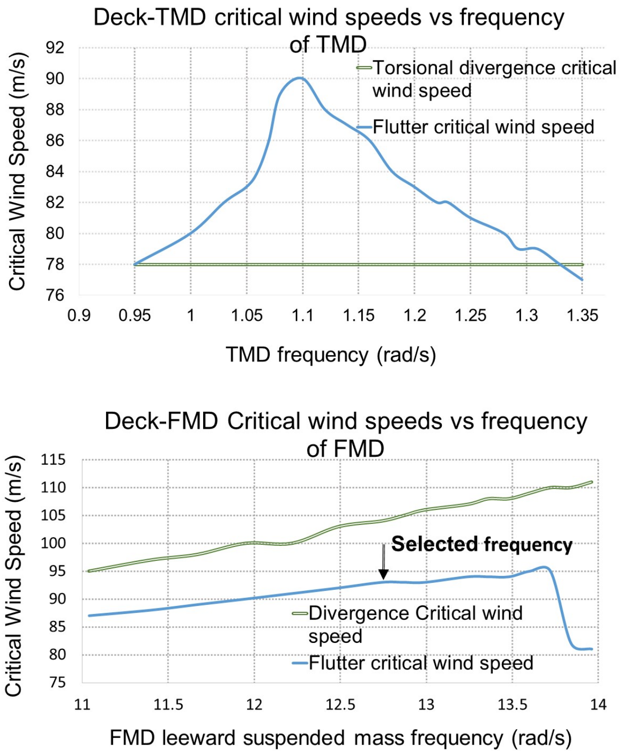


Figure 6. TMD vs FMD effectiveness with respect to tuning frequency.

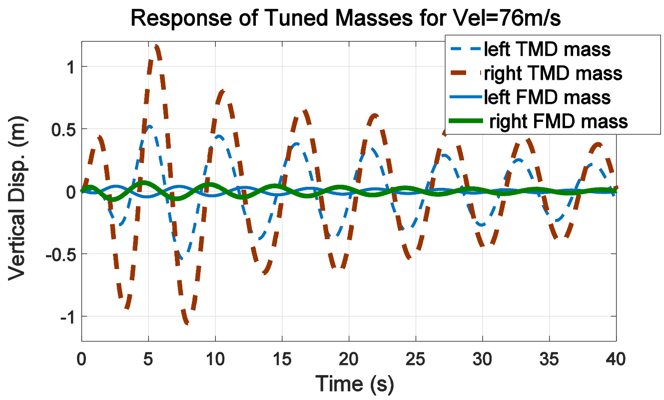


Figure 7. Vertical mass oscillations for the controlled sectional model using a TMD and an FMD mechanism.

## Conclusions

This work focused on combining the benefits of tuned mass dampers systems with that of optimum flap kinematics for aerodynamic bridge control. A transmission system designed out of purely passive components (spring, dampers and inerters) aims at achieving maximum robustness margins to structural uncertainties. The proposed design avoids the inherent shortcomings of TMDs while increases flap effectiveness by providing additional structural damping. An advantage of this procedure is that the network layout is not preselected, but determined through an optimization process. The introduced flap mass damper network avoids external structural linkages which can pose serious technical difficulties as well as alter the deck's aerodynamic characteristics.

## Acknowledgements

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